Διδακτορική διατριβή Δημητρίου Ε. Σικουτρή
tίτλος: "Ανάλυση της απόκρισης σύνθετων πολυμερών υλικών υπό συνθήκες φωτιάς. Εφαρμογή σε αεροπορικές κατασκευές"

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Title: “Fire Response of Composite Aerostructures"
This dissertation is dedicated to God for giving me the strength, courage and perseverance all these years to pursue the fulfillment of this hard work. Also, I would like to dedicate it to my beloved parents Evangelos & Georgia Sikoutris and my sister Evgenia for being there every time I needed their support. I would like to thank my Professor Vassilis Kostopoulos for all the academic, research and financial support that he, as director of the Applied Mechanics Laboratory, provided all these years. Without it this work would not have been possible to be completed in time. Many thanks to my best friends and colleagues Dr. Dimitris Mazarakos, Dr. Thanasis Kotzakolios, Dr. Nikos Athanasopoulos, and Dr. Dimitris Vlachos for all their help during these years. I would also like to acknowledge the FP6 VULCAN Project consortium for all the resources and equipment they provided. Many thanks to my good friends Panos, Lydia, Dimos and Michelle, for their encouragement all these years. Finally, I would like to thank everyone that helped me in any way during this hard work.
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ΤΜΗΜΑ ΜΗΧΑΝΟΛΟΓΩΝ & ΑΕΡΟΝΑΥΤΗΓΩΝ ΜΗΧΑΝΙΚΩΝ
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Πάτρα, 8/05/2012

Προς Τμήμα Μηχανολόγων και Αεροναυτηγών Μηχανικών
Πολυτεχνική Σχολή
Πανεπιστήμιο Πατρών

Θέμα: Έγκριση Διδακτορικής Διατριβής του κ. Σικουτρή Δημητρίου.

Η επιταμελής εξεταστική επιτροπή αποτελούμενη από τους κ.κ. Β. Κωστόπουλος, Καθηγητής, Τμήμα Μηχανολόγων και Αεροναυτηγών Μηχανικών Πανεπιστήμιο Πατρών, Γ. Παπανικολάου, Καθηγητής, Τμήμα Μηχανολόγων και Αεροναυτηγών Μηχανικών Πανεπιστήμιο Πατρών, Θ. Πανίδης, Αναπλ. Καθηγητής, Τμήμα Μηχανολόγων και Αεροναυτηγών Μηχανικών Πανεπιστήμιο Πατρών, Δ. Παλύζος, Καθηγητής, Τμήμα Μηχανολόγων και Αεροναυτηγών Μηχανικών, Πανεπιστήμιο Πατρών, Δ. Σαραβάνος, Καθηγητής, Τμήμα Μηχανολόγων και Αεροναυτηγών Μηχανικών Πανεπιστήμιο Πατρών, Δ. Μάργαρης, Καθηγητής, Τμήμα Μηχανολόγων και Αεροναυτηγών Μηχανικών Πανεπιστήμιο Πατρών και Κ. Περράκης, Λέκτορας, Τμήμα Μηχανολόγων και Αεροναυτηγών Μηχανικών Πανεπιστήμιο Πατρών, κατά τη σημερινή συνεδρίαση για την κρίση της διδακτορικής διατριβής του κ. Δ. Σικουτρή με θέμα:

«Ανάλυση της απόκρισης συνθέτων πολυμερών υλικών υπό συνθήκες φωτιάς.
Εφαρμογή σε αεροπορικές κατασκευές»
(«Fire response of composite aerostructures»).

Αποφάσισε ..., ότι το περιεχόμενο της διατριβής είναι πρωτότυπο και αποτελεί ουσιαστική συμβολή στην επιστήμη και βαθμολόγησε τη διδακτορική διατριβή με το βαθμό Χ.Ε.Τ.

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Πρόλογος.

Στην παρούσα διατριβή με τίτλο «Ανάλυση της απόκρισης σύνθετων πολυμερών υλικών υπό συνθήκες φωτιάς. Εφαρμογή σε αεροπορικές κατασκευές» πραγματοποιείται εργασία στην αριθμητική προσμοίωση και πειραματική διερεύνηση της συμπεριφοράς αεροπορικών κατασκευών σε συνθήκες φωτιάς. Στην μέχρι τώρα βιβλιογραφία οι διάφοροι έλεγχοι για πιστοποίηση των αεροπορικών υλικών αλλά και των αεροσκαφών στο σύνολό τους αποτελούνταν από εκτενείς πειραματικές δοκιμές σε μεσαία κλίμακα καθώς και σε πλήρους κλίμακας κατασκευές. Οι προδιαγραφές των ελέγχων ορίζονται από την Ομοσπονδιακή Διεύθυνση Αεροπλοίων των Ηνωμένων Πολιτειών της Αμερικής, Federal Aviation Administration FAA. Όπως γίνεται αντιλήπτο πλήρους κλίμακας δοκιμών είναι χρονοβόρες αλλά και οικονομικά ασύμφορες, για τον λόγο αυτό τα τελευταία χρόνια πραγματοποιούνται προσπάθειες από την FAA για καθερέση Προτύπων ελέγχου μικρής κλίμακας τα οποία σε συνδυασμό με αριθμητικά μοντέλα θα είναι σε θέση να προβλέψουν την συμπεριφορά των αεροπορικών κατασκευών σε συνθήκες φωτιάς από την φάση του σχεδιασμού τους. Θα εξασφαλίζεται έτσι καλύτερη διαχείριση οικονομικών και υλικών πόρων. Στην βιβλιογραφία ο μεγαλύτερος όγκος αριθμητικής μοντελοποίησης έχει πραγματοποιηθεί στους τομείς της ναυπηγικής και των θαλάσσιων κατασκευών καθώς επίσης και τα τελευταία χρόνια στον τομέα της αστικής δόμησης. Αριθμητική δουλεία πάνω στην συμπεριφορά των αεροπορικών κατασκευών είναι υπερβολικά περιορισμένη και εκεί στοχεύει να συμβάλει η παρούσα διατριβή. Οι αεροπορικές κατασκευές εκτός των περιορισμών και προδιαγραφών που θέτουν οι άλλες εφαρμογές απαιτούν την ελαχιστοποίηση του προστιθέμενου βάρους στην κατασκευή.

Διάφοροι τύποι πολυμερών συνθέτων υλικών χρησιμοποιούνται στην βιομηχανία, διακρινόμενα σε θερμοσκληρυνόμενα και θερμοπλαστικά. Αρχικά παρουσιάζονται τα θερμοσκληρυνόμενα ξεκινώντας από τους ευρέως χρησιμοποιούμενους πολυεστέρες και βινυλεστέρες, στις φαινόμενες και εποξικές ρητίνες καταλήγοντας στους υψηλής θερμοκρασίας κυανεστέρες. Εν συνεχεία γίνεται αναφορά στη συνήθη χρησιμοποιούμενα θερμοπλαστικά, πολυπροπυλένιο PP, Poly-ether-ether-ketone PEEK και polyphenylene Sulphide PPS. Φυσικά δεν παραλείπεται να γίνει σύντομη αναφορά και στις τυπικές διεργασίες θερμικής αποσύνθεσης των προαναφερθέντων πολυμερών.

Η συμπεριφορά των σύνθετων πολυμερών υλικών σε συνθήκες φωτιάς περιγράφεται από κάποια χαρακτηριστικά μεγέθη τα οποία χρησιμοποιούνται για την ποιοτική και ποσοτική σύγκριση των διαφόρων υποψηφίων αεροπορικών υλικών. Συγκεκριμένα τα μεγέθη αυτά είναι: Heat Release Rate HRR, Thermal Stability Index TSI, Limited Oxygen Index LOI, Extinction Flammability Index ESI, Time-to-Ignition, Surface Flame Spread, Mass Loss, Smoke Density, Smoke Toxicity. Οι διαδικασίες ελέγχου και τα υπολογιζόμενα μεγέθη γίνονται βάσει διεθνών Προτύπων που κυρίως για τον τομέα της αεροναυπηγικής ορίζονται από την Ομοσπονδιακή Διεύθυνση Αεροπλοίας FAA.

Preface.
Η αριθμητική προσομοίωση προϋποθέτει γνώση της συμπεριφοράς των πολυμερών υλικών σε συνθήκες υψηλής θερμοκρασίας, για τον σκοπό αυτό πραγματοποιήθηκαν πειράματα απόλειας μάζας με χρήση θερμογραμμετρίας TGA κατά τη διάρκεια της οποίας η απόλεια μάζας καθώς και ο ρυθμός αυτής παρακολουθούνται και καταγράφονται σαν συνάρτηση του ρυθμού θέρμανσης. Μέσα από αυτά τα δεδομένα μπορεί να πραγματοποιηθεί εκτίμηση του τρόπου αποσύνθεσης του πολυμερού. Αρχικά πραγματοποιήθηκε η θεώρηση της μονοβάθμιας αντίδρασης (single-stage reaction) που αποτελεί και την πλέον απλοστικευμένη προσέγγιση. Στην θεώρηση αυτή θεωρείται πως η πολυμερής μήτρα περνά από την «παρθένου» κατάσταση στην απανθρακωμένη μέσα σε ένα βήμα. Η περιγραφή της αντίδρασης αυτής γίνεται με μια μονοβάθμια αντίδραση τύπου Arrhenius.

Σε δεύτερο βήμα χρησιμοποιήθηκε κινητική θεωρία πολλαπλών σταδίων (multi-stage kinetics) σύμφωνα με την οποία πραγματοποιήθηκε ακριβείτερη προσέγγιση της αποσύνθεσης της πολυμερούς μήτρας των συνθέτων υλικών με απόκλιση μικρότερη του 5% από τα πειραματικά δεδομένα της θερμογραμμετρίας (thermogravimetry). Και στις δύο προσεγγίσεις της αποσύνθεσης υπολογιζόταν οι κινηματικές παράμετροι: συντελεστής συγχήτης Α (frequency factor), ένεργεια ενεργοποίησης ΕΑ (activation energy), τάξη αντίδρασης η (reaction order) για κάθε στάδιο. Με την ολοκλήρωση αυτού του σταδίου υπήρχε μια αξιόπιστη δυνατότητα αναπαράστασης της διαδικασίας αποσύνθεσης στο πείραμα της θερμογραμμετρίας.

Είναι γνωστό ότι οι διακυμάνσεις της θερμοκρασίας επηρεάζουν τις τιμές των θερμοφυσικών ιδιοτήτων των υλικών. Αναλογιζόμενοι ότι στην διαρκεία της επιβολής της φλόγας στα συνθέτα υλικά όχι μόνο η θερμοκρασία αλλά και η σύσταση μεταβάλλεται συνεχώς λόγω της αποσύνθεσης κρήτης αναγκαία η ανάπτυξη μιας μεθοδολογίας που θα συμπεριλαμβάνει την επίδραση της αποσύνθεσης στην κλίμακα των θερμοφυσικών ιδιοτήτων (θερμική αναγωγικότητα, ειδική θερμοκρασιακότητα και πυκνότητα) της πολυμερούς μήτρας κατά συνέπεια του συνθέτου υλικού. Οι εξεγόνευσές μαθηματικές σχέσεις χρησιμοποιήθηκαν στην αριθμητική προσομοίωση που ακολούθησε.

Με σκοπό την ορθή αριθμητική μοντέλοποιηση κρίνεται αναγκαία η μέτρηση και βαθμονόμηση του θερμικού φορτίου του πειραματικό του δοκιμών. Το μετρούμενο θερμικό φορτίο χρησιμοποιήθηκε εν συνεχεία ως φόρτιση στα αναπτυγμένα μοντέλα. Χρησιμοποιήθηκαν δύο πειραματικές διατάξεις εφαρμογής φλόγας, μία μεσαίας κλίμακας σύμφωνα με τις διατάξεις του FAA Standard, που περιγράφεται στο ISO2685:1998(E) “Aircraft – Environmental test procedure for airborne equipment – Resistance to fire in designated fire zones” και μίας εργαστηριακής κλίμακας. Πραγματοποιήθηκε μέτρηση με θερμομετρία και καλόριμετρο νερού καθώς και αριθμητική μοντέλοποιηση με χρήση CFD για την πρώτη διατάξη. Ενώ για την εργαστηριακής κλίμακας έγινε μέτρηση με θερμομετρία και ενός αισθητήρα θερμικού φορτίου «water-cooled Hukseflux Schmit-Boelter SBG01 sensor».

Εν συνεχεία πραγματοποιήθηκε η κατασκευή των δοκιμών των υποψηφίων υλικών καθώς και των πειραματικών δοκιμών και έλεγχοι τους. Συγκεκριμένα πραγματοποιήθηκε: Θερμομετρία κόνου (cone calorimetry), Θερμογραμμετρία (thermogravimetry), Θερμομετρία Ιντρουρής Ανίχνευσης (Differencial Scanning
Καθώς και αρκετά αεροπορικών μοντέλων των πολυμερών υλικών, η μεταβολή των θερμοφυσικών ιδιοτήτων, η μέτρηση και βαθμονόμηση του επιβαλλόμενου θερμικού φορτίου καθώς και οι πειραματικές δοκιμές έχουν ολοκληρωθεί ακολουθεί η αριθμητική προσομοίωση. Οι συναρκείς συνήθεις θερμικού φορτίου και ψύξης επιλέχθηκαν ως εξής. Ως φόρτιση θεωρήθηκε η κατανομή του θερμικού φορτίου (σε kW/m²) στην εμπρός επιφάνεια του πάνω. Στην ψύξη της πίσω επιφάνειας λήφθηκε υπόψη τόσο η ελεύθερη μεταφορά θερμότητας με επαφή όσο και η ακτινοβολία. Το μοντέλο της συμπεριφοράς του υλικού διαμορφώθηκε κατάλληλα ώστε να γίνει κατανοητό από τις απαιτήσεις ενός εμπορικού κώδικα Πεπερασμένων Στοιχείων επίλυσης θερμικών προβλημάτων και προσομοιώθηκαν οι πειραματικές δοκιμές διείσδυσης φλόγας των δύο πειραματικών διατάξεων, μεσαίας και εργαστηριακής κλίμακας.

Πλέον της αριθμητικής προσομοίωσης της συμπεριφοράς σε φωτιά επιπέδων δοκιμιών αεροπορικών κατασκευών, πραγματοποιήθηκε προσπάθεια απλούστευμενής μοντελοποίησης των συνήθεις φλόγας ενός λιμνάζοντος όγκου καυσίμου αεροσκαφών στο εξωτερικό μιας ατράκτου. Δημιουργήθηκε ένα τρισδιάστατο ρευστομηχανικό μοντέλο πρόβλεψης του θερμικού φορτίου στην επιφάνεια μιας τυπικής ατράκτου σύμφωνα με τις προδιαγραφές γεωμετρίας του Προτύπου “Full-scale test evaluation of Aircraft fuel fire burnthrough resistance improvements” DOT/FAA/AR-98/52,1999. Τα ρευστομηχανικά αποτελέσματα συγκρίθηκαν με δεδομένα βιβλιογραφίας για μεγάλες φλεγόμενες δεξαμενές λιμνάζοντος καυσίμου.

Εκτός από τη μελέτη της απόκρισης των αεροπορικών κατασκευών σε συνήθεις φλόγες σκοπός της παρούσας εργασίας είναι και η παρουσίαση λύσεων οι οποίες θα έχουν την δυνατότητα της βελτίωσης της συμπεριφοράς των υπαρχούσων δομών καθώς και των μελλοντικών σύνθετων δομών. Ενδεικτικά αναφέρεται η δυνατότητα χρήσης νανογκλεισμότων, και βελτιωμένων μονοτικών υλικών, π.χ. aerogels. Οποιοί έχει ήδη αναφερθεί οι αεροπορικές κατασκευές θέτουν τον περιορισμό της ελαχιστοποίησης του προστιθέμενου βάρους, για τον λόγο αυτό η ενίσχυση των συνθέτων υλικών θα πρέπει να πραγματοποιηθεί σε επίπεδο υλικού και σχεδιασμού. Πρέπει δηλαδή η ιδέα τη κατασκευή που είναι ικανή να φέρει τα μηχανικά φορτία να εξασφαλιστεί και την πιστοποίηση της FAA για συνήθεις φωτιές.

Συνοψίζοντας, η παρούσα διατριβή πραγματοποιεί μια καινοτόμο, γρήγορη και αρκετά ακριβή προσέγγιση του σημαντικότατου ζητήματος της συμπεριφοράς των πολυμερικών συνθέτων αεροπορικών δομών σε συνήθεις φωτιές. Η πολυπλοκότητα του όλου φαινομένου επέβαλε την πραγματοποίηση παραδοχών και εποπτεύσεων. Καθώς όμως με την αυξανόμενη χρήση των συνθέτων υλικών στις αεροπορικές κατασκευές, ο τομέας της ασφάλειας σε συνήθεις φλόγες είναι συνεχώς αυξανόμενος και απαιτητικός. Για αυτό οι παραδοχές και θεωρήσεις της παρούσας διατριβής μπορούν να βελτιωθούν με χρήση νέων υπολογιστικών μεθόδων και πειραματικών δεδομένων με στόχο την ακόμα ακριβέστερη πρόβλεψη της συμπεριφοράς των αεροπορικών δομών σε συνήθεις φλόγες.
Preface

The current dissertation, titled “Fire Response of Composite aerostructures” deals with a crucial subject of the aeronautics industry that is the fire response of composite aerostructures, more specifically the issue of interest in this work is the fuselage fire burnthrough from an external liquid jet-fuel pool fire. Other fire issues that “bother” the aeronautics industry are the fire spread inside the cabin, smoke generation and toxicity of the fumes, but these are not handled in the current dissertation.

Aircraft structures are designed to withstand various loading scenarios during their operational life. These loading scenarios are associated to a great extent with normal aircraft operation (flight manoeuvres, take-off and landing). However there are situations where the aircraft structures are required to assure the safety of the passengers and crew. In the case of an emergency crash landing, the threat of an external jet-fuel fire always exists. Considering that the aircraft structure survives the impact, the survivability of the passengers and crew onboard the aircraft depends solely on the fire resistance of the aircraft structure. A measure of the fire resistance of an aircraft structure is the time needed for the flames to penetrate the fuselage and spread inside the cabin, the so-called, burn-through time.

So far, the aircraft fire resistance has been extensively studied by conducting lab, medium and full scale tests. The early lab scale tests were performed by the Federal Aviation Administration (FAA) and involved the Bunsen-burner flammability test of coupons for developing fire safe interior materials. As the application of polymer materials on aircrafts kept increasing, the problem of fire burn-through due to external fire emerged. Marker was one of the first to perform full-scale fuselage burn-through tests to access the insulating performance of materials. Also a statistical analysis was performed by Cherry and Warren that accessed and analyzed data from past accidents and their work resulted in proving the importance of fuselage fire hardening and the passengers’ lives that could be saved using low-cost solutions. These works led the FAA to proposed new fire testing procedures for aircraft materials.

The scope of this dissertation was to assess the performance of various structural materials in a pool-fire scenario. A simplified approach is made, approximating the pool-fire conditions with a flat panel burn-through test in accordance to the ISO2685:1998(E) Standard.

The originality of the present work comes from the fact that it incorporates a multistage approach in order to investigate the behaviour and response of composite aircraft structures in the possibility of a fire event. The current approach goes down on material level in order to investigate and model the deterioration (decomposition) of the polymer composite. Thus, it investigates and proposes a methodology of how the thermophysical properties of the composite are deteriorated due to the fire event. It proceeds into developing a progressive-damage material model (material properties varying with the deterioration degree) and finally implementing this custom material model into a commercial FE package and solving the loading scenarios.

Being more specific the current work begins with a quick review of the literature where incidents and work done on the burnthrough event for the past 20-30 years are summarized. It progresses then to presenting the various types of polymers used in the aircraft industry and their basic decomposition mechanisms, from the unsaturated polyesters to the epoxies and phenolics and in the end reference to the thermoplastics is made. Every organic material, hence, polymers used in aerospace applications, present a set of response characteristics when
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subjected to fire, specifically the heat release rate, thermal stability index, limiting oxygen index, flammability index, time-to-ignition, surface flame spread, mass loss, smoke density and smoke toxicity.

Following is the backbone of this dissertation, the kinetics modelling. Two approaches are made, one simplified using single stage kinetics where the decomposition degree \( a \) is calculated based on the Arrhenius reaction theory and using the kinetic triplets (kinetic parameters) extracted from thermogravimetry, TGA, data using the Friedman multi-curve method. The second approach is more complicated and considers multi-stage decomposition of the polymer composite. Specifically a 3-stage reaction network is considered for every material, the LY-Ref, and the two modified batches, one with ammonium polyphosphate AP423 and the other both with AP423 and multi-wall carbon nanotubes MWCNT. Again the kinetic parameters, activation energy \( E_A \), frequency factor \( A \), and reaction order \( n \), are extracted for every step using the van Krevelen methodology. In the end using the reaction rates equations the reconstruction of the TGA curves is achieved with an error of less than 5% from the test data. Correlations that consider the material deterioration and affect the thermophysical properties of the materials are proposed. Those expressions are being developed for both of the two kinetic approaches, the single and multi-stage.

Another crucial part of this work is the measurement and calibration of the applied fire load. Again two fire load approaches are used, one according to the ISO2685 Standard where a propane burner was manufactured and calibrated according to the Standard for medium scale samples testing and a lab scale butane burner for small samples. The ISO2685 burner was also CFD simulated and the models calibrated against analytical expressions, ISO requirements and real measurements. The CFD simulations were performed so the heat flux or heat transfer coefficient to be extracted and used as input for the later thermal FE burnthrough models. The heat flux distribution of the lab-scale AML burner on the specimen surface was measured via a water cooled Schmit-Boelter SBG01 heat flux sensor manufactured by Hukseflux.

Manufacturing and material details are presented concerning the samples used for every test campaign. Metallic (AL2024-T3) samples, CFRP neat and modified, and hybrid GLARE ones where manufactured. Also the experimental work performed is described. Cone calorimetry testing data are available, results from thermogravimetry tests, differential scanning calorimetry, and finally the burnthrough tests with both the testing apparatuses, the ISO2685 one and the AML lab-scale burner.

The modelling work in this dissertation involved thermal models that were developed into a commercial FE package. It was not part of this work to develop a thermal solver so a commercial one was selected and all the developed methodology was adapted to its requirements and specifications. The boundary conditions on the models are presented both for the ‘hot’ front surface and the rear ‘cooling’ one. For the ‘hot’ one the heat flux distribution is used and for the ‘cooling’ one an equivalent convection is applied that accounts for both convective and radiative cooling. The decomposing material model is implemented into to FE solver via user defined subroutines for the single stage kinetics and the multi-stage approach. Finally the simulations were run and the results and models were compared against the available experimental results.

Since so far the burnthrough response of aerostructures was limited to coupon, samples and medium size flat panels. A more realistic approach was performed by developing a mathematical model of a real size test. The certification tests conducted by the FAA are for full size fuselage sectors under the fire load of a burning jet-fuel pan pool-fire. A burning jet-fuel pool fire is a complex phenomenon on its own, combining it with a decomposing fuselage structure make the modeling approach even more difficult to simulate if not impossible. Required data for the pool-sizes under investigation were not available, so data
for large external hydrocarbon pool fires from literature were used. Also, because the main characteristic of a jet-fuel (kerosene) pool fire is that the flames are not clear, on the contrary, great amount of shoot is produced making combustion modeling and radiative heat transfer to the fuselage even more of a challenge to model, it was decided to try and tackle this full-scale approach by a simplified the modeling approach. Instead of liquid fuel combustion, an equal hot air stream with mass flow, velocity and temperature properties extracted from literature correlation data was performed.

Conclusively, in terms of completeness the benefit analysis performed by Cherry and Warren is presented in brief. The objective of their analysis was to assess the potential benefits, in terms of reduction of fatalities and injuries, resulting from improvements in fuselage burnthrough resistance to ground pool fires. Fire hardening of fuselages will provide benefits in terms of enhanced occupant survival and may be found to be cost beneficial if low-cost solutions can be found. The maximum number of lives saved per year in worldwide transport aircraft accidents, over the period covered by the data, if hardening measures were applied, was assessed to be 12.5 for the aircraft in its actual configuration (when the accidents occurred) and 10.5 for the aircraft configured to later airworthiness requirements.

These figures are completely significant and give an extra confirmation that this work on investigating the fire response of composite aerostructures is on the right track. As the work of Cherry and Warren concluded, the fire hardening measures in order to be applicable need to be cost efficient. The concept under which this whole dissertation stepped on was to investigate the fire response of composite aerostructures and the possibility of hardening the structure itself without the use of extra protective layers that add cost and weight to the overall aircraft and its maintenance. In the end it was concluded that there is the possibility of hardening the fuselage structure by design and by material. Incorporating composites into the structure it is possible to prolong the burnthrough time at least for 4-5 minutes before auto ignition occurs on the inner side of the fuselage. Auto ignition of the inner side fuselage cabin materials is mentioned since in NONE of the burnthrough tests of the CFRP composites and the GLARE samples flame penetration was observed.
CHAPTER 1  Introduction.

Fire contributes to aircraft accidents and many fatalities. The growing use of polymer composite materials in aircraft has the potential to increase the fire hazard due to the flammable nature of the organic matrix. The polymer composite most often used in the external structures of aircraft is carbon/epoxy, which is a flammable material that easily ignites and burns when exposed to fire. A large percentage of the cabin interior of wide-bodied passenger aircraft is made using composite materials, mostly glass/phenolic. Phenolic composites have fairly good fire performance, but newer materials are being developed that offer the promise of increasing the fire safety of aircraft cabins. In fact, a large number of new composite materials are being developed for cabins and external structures that have the potential to increase the fire safety of aircraft, but a detailed analysis of the fire performance of these materials against conventional materials now used in aircraft has not been performed. Such an evaluation will provide a clear indication of the potential improvements in fire safety by using new fire resistant composites in aircraft [1.1].
1.1 Literature Review

1.1.1 Composite applications in aircraft

The amount of polymer composite material used in aircraft and helicopters has risen dramatically since the 1970s. The rapid growth in the use of composites in large civil aircraft, military aircraft and rotorcraft is shown in Figure 1.1. In all three classes of aircraft the use of composites has increased many-fold over the past thirty years, and this trend is set to continue as these materials continue to replace aluminium and other aerospace alloys in primary structures and control surfaces.

![Figure 1.1 Composite aircraft structure by weight [1.1],[1.2].](image)

The value of aircraft components made using composite materials in 2004 is estimated at over $130 billion. The market value is expected to grow in coming years provided, among other things, composites do not pose an increased fire safety hazard to aircraft. Boeing and Airbus – the two largest aerospace companies – expect the amount of composites used in their aircraft to increase in the next 10 to 15 years. The percentage of the structure of large passenger aircraft made using composites is currently 5 to 10%, although this is projected to increase to over 20% and possibly higher. Both the new A380 and B787 aircraft make extensive use of composite materials. The use of composites in cabins is expected to increase with their growing use in passenger electronics and telecommunications equipment.
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The growth in the usage of composites is due to several factors, most notably:

- light weight
- high specific stiffness and specific strength
- fatigue endurance
- design flexibility
- corrosion resistance.

There are many varieties of composite materials used in aircraft, although the two most common types are:

**Glass reinforced phenolic composites** that are used extensively in aircraft cabins. Phenolic composites are used as either a single skin laminate or as a sandwich material that consists of thin glass/phenolic face skins over a Nomex honeycomb core. Phenolic composites account for 80%-90% of the interior furnishings of modern passenger aircraft. These composites are used in ceiling panels, interior wall panels, partitions, galley structures, large cabinet walls, structural flooring and overhead stowage bins. An important reason for the extensive use of phenolic composites inside cabins is their low flammability and good fire resistance.

**Carbon reinforced epoxy composites** are used in aircraft structures including fuselage, wing and tail fin components, control surfaces and doors. As with phenolic composites, epoxy composites are used as either carbon/epoxy laminates or sandwich materials containing carbon/epoxy skins and a Nomex or aluminium honeycomb core. Most types of carbon/epoxy laminates used in aircraft structures are flammable and readily decompose when exposed to heat and fire.

Flame retardant epoxies and other fire-resistant polymers are being used increasingly in carbon fibre composite aircraft structures; however these materials are often much more expensive and may not have the same mechanical performance as conventional aerospace-grade epoxies.

1.1.2 Fire hazard to aircraft

Aircraft fire safety is a high priority with aviation safety authorities and, of course, the flying public. A major hazard with the use of many types of polymer composite materials in aircraft cabins and structures is their flammability. When composites are
exposed to high temperatures (typically above 300-400°C) the organic matrix decomposes with the release of heat, smoke, soot and toxic volatiles. Organic fibres used to reinforce composites, such as aramid and polyethylene, also decompose and contribute to the generation of heat, smoke and fumes. Composites also soften, creep and distort when heated to moderate temperature (>100-200°C), and damage caused by the heat and flame can cause distortion, buckling and failure of load-bearing structures. The heat, smoke and gases released by a burning composite and the degradation in structural integrity can quickly jeopardise the safety of an aircraft. The susceptibility of many conventional composites to fire has been the key issue in curtailing their use in many aircraft applications.

Aircraft fires are extremely hazardous because there is little time to combat and extinguish the fire before the crew and passengers are in danger. The hazard is extremely severe in the event of cabin flashover, which may occur within several minutes without the use of appropriate fire resistant materials. As a guide, when fire occurs in the cargo-hold, the pilots have about two minutes to extinguish the flames. If it takes longer than this, the fire will often grow too large to extinguish using on-board fire-suppression systems. If the aircraft has an extinguishable fire, the pilots have about 14 minutes to land/ditch and evacuate before the risk of incapacitation from smoke and fumes.

The pie-chart in Figure 1.2 shows the causes of fatal wide-body passenger aircraft accidents over a ten-year period (from 1987 to 1996). Over this time there were 180 accidents, but only six (or 3.5%) of these were caused by fire. During this period, in-flight fire was the tenth most common cause of aircraft accidents. Fire is a rare event because of the strict fire safety regulations and effective flame suppression systems in aircraft.

Despite the relatively small number of aircraft accidents caused by fire, another telling statistic is that fire is the fourth highest cause of fatalities (excluding accidents due to unknown causes). Figure 1.3 gives a breakdown of the causes of aircraft fatalities between 1992 and 2001 when 339 people were killed (4.9% of all fatalities) by in-flight fire. These statistics highlight the danger that in-flight fire poses to aircraft safety, and the tragically high death toll it can cause.
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Figure 1.2 Causes of fatal wide-body passenger aircraft accidents between 1987 and 1996; the number for the different causes is shown in brackets (source FAA) [1.1].

Figure 1.3 Fatalities resulting from passenger aircraft accidents between 1992 and 2001; the number of fatalities for the different causes of accidents is shown in brackets (source FAA) [1.1].

It is common practice by fire scientists to quantify the intensity of a fire by the radiant heat flux rather than flame temperature. Figure 1.4 shows the relationship between heat flux and temperature at the hot surface of a polymeric material. There is an approximate relationship between fire type and heat flux, and examples are:

- small smouldering fire: 2-10 kW/m²
- trash can fire: 10-50 kW/m²
- cabin fire: 50-100 kW/m²
- post-flashover cabin fire: >100 kW/m²
- jet fuel fire: 150-200 kW/m².


1.1.3 FAA fire safety regulations

The FAA determines the fire safety regulations on the materials used in US designed and manufactured civil aircraft. These regulations are generally applied across the global aviation sector, including within Australia. Aircraft fires fall into three categories: ramp, in-flight and post-crash. Ramp fires occur when an aircraft is parked at the terminal ramp, and the incidence of fire in this state is very low. The fire hazard is much more common during flight, such as occurred to Swissair 111 on 2 September 1985, or, in particular, post-crash. For this reason, the FAA regulations consider the fire, smoke and toxicity properties of cabin materials for a post-crash fire scenario. The scenario dictates that passengers must be able to escape a large, wide-body aircraft within five minutes of a crash landing without being incapacitated, injured or hindered by heat, toxic fumes or smoke released from combustion of the cabin materials.

All non-metallic materials used inside the pressure vessel of commercial aircraft are subject to the FAA flammability regulations. There are several fire tests mandated by the FAA to assess the flammability and fire performance of materials, and these are specified in FAR 25.853. The key fire properties considered by FAR 25.853 are total heat release, heat release rate and smoke emission. The FAA sets performance limits for heat and smoke on cabin materials to delay cabin flashover and thereby increase the escape time for passengers. Cabin flashover is a fire event characterised
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by ignition of the hot smoky layer below the cabin ceiling that contains incomplete combustion products released by the burning and smouldering cabin materials. When flashover occurs the cabin temperature rises rapidly, the flames spread rapidly, and the chances of survival for passengers and crew are virtually non-existent. The FAA also specifies other fire regulations for ignition resistance and flame propagation using the traditional Bunsen burner test.

The FAA mandates that the heat release properties of non-metallic materials must be measured using the Ohio State University calorimeter test operated at a heat flux of 35 kW/m², as described in ASTM E906. As part of the safety regulations, the test material is required to have a total heat release of less than or equal to 65 kW/m² over two minutes and a peak heat release rate of less than or equal to 65 kW/m² over the five minute duration of the test. These specifications are used to ensure a cabin material does not contribute to the growth and spread of a fire during the first five minutes following a crash landing.

The FAA requires that the smoke properties of non-metallic materials be measured using the NBS Smoke Chamber according to ASTM E662. The material is required to have a smoke specific optical density ($D_s$) value of less than or equal to 200 at four minutes. Further information on the fire safety regulations can be found in FAR 25.853, and McLean et al. [1.3] provide a review of the different test methods.

1.2 Problem Statement.

Aircraft structures are designed to withstand various loading scenarios during their operational life. These loading scenarios are associated to a great extent with normal aircraft operation (flight manoeuvres, take-off and landing). However there are situations where the aircraft structures are required to assure the safety of the passengers and crew. In the case of an emergency crash landing, the threat of an external jet-fuel fire always exists. Considering that the aircraft structure has survived the impact, the survivability of the passengers and crew onboard the aircraft depends on the fire resistance of the aircraft structure. A measure of the fire resistance of an aircraft structure is the time needed for the flames to penetrate the fuselage and spread inside the cabin, the so-called, burn-through time.
The majority of previous full-scale fire tests conducted by FAA prior to 1994 utilized a fuselage with a fire patch at the cabin floor level adjacent to an opening in the hull. This configuration was representative of a severe but survivable fire condition in which interior materials’ flammability could be compared. Exposing the interior of the fuselage to the direct intense thermal radiation of the fuel fire allowed the evaluation of the combustibility of combinations of materials in a realistic scenario.

This type of full-scale testing resulted in new FAA Standards for low heat release interior panels and seat cushion fire-blocking layers. Another crash scenario is one where an intact fuselage is exposed to an external ground level fuel fire. In this case the fire penetrates into the cabin by means of a fuselage hull burthrough. Four examples of this type of accident are those at Los Angeles, 1978; Malaga, 1982; Calgary, 1984; and Manchester, 1985. The aircraft resistance to burnthrough in each accident and the resultant survivability varied as follows [1.4]:

1.2.1 Los Angeles, 1978
A Douglas DC-10 was exposed to a large pool fire for 2 ½ minutes before the fire was extinguished by crash fire rescue teams. The fuel fire did not penetrate and ignite the cabin materials; although, there was some evidence of damage at the panel seams and along the seat back cushions. In this case, the fuselage exhibited significant burnthrough resistance.

1.2.2 Malaga, 1982
A Douglas DC-10 aborted takeoff and overran the runway striking the right wing on an obstruction. The wing was severed from the aircraft rupturing the fuel tanks and exposing the intact fuselage to a large exterior fuel fire. The fuselage resisted burnthrough for a relatively long period of time allowing the 344 of the 394 people on board to escape.

1.2.3 Calgary, 1984; and Manchester, 1985
Both accidents were very similar in configuration. In each case, the aircraft was a Boeing 737, the accident was caused by an engine failure, and the aircraft was completely gutted by fire. The similarity ends however when survivability is considered. In Calgary, all of the occupants escaped the aircraft whereas in
Introduction.

Manchester fifty-five people lost their lives due to the fire. At Manchester, it was reported that the fuel fire penetrated the cabin in less than 60 seconds.

Fire can penetrate into an intact fuselage and into the passenger cabin in a number of different ways. Likely areas of penetration include the sidewall (above the floor), windows, cheek areas (below the floor), cabin floor, and baseboard air return grills [1.4]. There is no direct evidence from past accidents or test data from previous experiments to indicate which area is most vulnerable to fire penetration or which provides the most likely path for flame travel once a penetration occurs. Previous tests examined the burnthrough resistance of individual fuselage elements such as the aluminum skin, windows, sidewall panels, thermal, acoustical insulation, and cargo compartment liners. That work was primarily materials testing aimed at improving burnthrough resistance of specific assemblies. There was no record of full-scale tests in the past to examine the resistance of the complete fuselage with the goal of identifying fire penetration paths as well as burnthrough times. That was the reason Webster [1.4] started a project with purpose to study the burnthrough characteristics of commercial passenger-carrying transport aircraft when subjected to a large external fuel fire. Specifically, areas of likely flame penetration and resultant flame paths within the fuselage were identified as well as a time frame for each event.

So far, the aircraft fuselage fire resistance has been extensively studied by conducting lab, medium and full scale tests. The early lab scale tests were performed by the Federal Aviation Administration (FAA) and involved the Bunsen-burner flammability test of coupons [1.5]. Later on, Lyon [1.6] performed a research in order to develop fire safe materials for commercial aircrafts, but these materials were mainly interior materials and not structural ones. As the application of polymer materials on aircrafts was increasing, the problem of fire burn-through due to external fire emerged. Marker [1.7] was one of the first to perform full-scale fuselage burn-through tests to access the insulating performance of materials. Also a statistical analysis was performed by Cherry and Warren [1.8] that accessed and analyzed data from past accidents and their work resulted in proving the importance of fuselage fire hardening and the passengers’ lives that could be saved using low-cost solutions. Based on all these, FAA proposed new fire testing procedures for aircraft materials [1.9].
The scope of this dissertation is to assess the performance of various structural materials in a pool-fire scenario. A simplified approach is made, approximating the pool-fire conditions with a flat panel burn-through test in accordance to the ISO2685:1998(E) Standard [1.10].

As mentioned earlier, the main part of the available literature is based on the experimental characterization of interior materials. The study of the response of composite structures under fire loading was first performed by Gibson [1.11] and is a pioneer modelling work. The fire burnthrough analytical/numerical approach of CFRP aircraft structures is a missing issue and the present dissertation intends to be a first approach to tackle this problem. The originality of the present work comes from the fact that it incorporates a multistage approach in order to investigate the behaviour and response of composite aircraft structures in the possibility of a fire event. The current approach goes down on material level in order to investigate and model the deterioration (decomposition) of the composite material. Thus, it investigates and proposes a methodology of how the thermophysical properties of the composite are deteriorated due to the fire event. It proceeds into developing a progressive-damage material model (material properties varying with the deterioration degree) and finally implementing this custom material model into a commercial FE package and solving the loading scenarios.

1.3 References


Introduction.


CHAPTER 2  Polymers and Their Decomposition Processes.

Materials are sensitive to temperature changes. Despite the nature of the materials, metallic, non-metallic, organic, inorganic temperature gradients affect their composition. Typically greater effect poses temperature elevation, since the majority of the chemical reactions are temperature sensitive if not temperature driven. Such reactions are crystallization, oxidation, polymerization, pyrolysis, charring, etc. In this work organic polymers are investigated, hence temperature elevation affects the structure of the polymer chain. If significant energy (heat) is given to the polymer, under the appropriate environmental conditions, then chain break-up could occur. Absence of oxygen or other oxidative agents would result in polymer pyrolysis and the formation of porous carbonaceous char, consisting mainly of amorphous carbon.

In this section several typical polymer systems used as matrix material in composites are described, along with a brief summary of what is known of their thermal decomposition mechanisms [2.1]. Both thermosets (polyesters, vinyl esters,
epoxies and phenolics) and thermoplastics (polypropylene PP, poly-ether-ether-ketone PEEK and polyphenylene sulphide PPS) are included.

2.1 Thermoset Resins.

Thermosets are the type of polymer resins that after curing/hardening occurs, either at room or oven temperature, the formed crosslinks and chains structure can no be altered. Trying to heat up the polymer would cause it to melt, chain scission and in the end decompose. Cross links can not be rearranged.

2.1.1 Unsaturated Polyester.

Polyesters are used in many composite products because of their moderate cost, good mechanical properties, environmental durability, and low-temperature curing. Polyesters can be used in all the conventional manufacturing processes (hand lay-up, spray lay-up, etc). They are the most widespread example of “solvent monomer” resin systems, that are cured by free radical polymerization. A schematic of the “solvent monomer” systems polymerization principle is shown in Figure 2.1.

![Figure 2.1 Principle of cross-linking “solvent monomer” systems such as thermosetting polyester.](image)

The two components of a solvent monomer system are (i) a pre-polymer having carbon-carbon double bonds (or unsaturation) in the back-bone chain and (ii) an unsaturated monomer, such as styrene, in which the pre-polymer is dissolved. Typical examples of solvent monomers are presented in Figure 2.2.
Cure is triggered by adding an initiator (a source of free radicals). The free radical cure reaction involves not only the addition polymerisation of the monomer species but also the double bonds in the pre-polymer resulting in a polymeric network where the pre-polymer chains are cross-linked by chains of polymerised monomer. In polyester resins the pre-polymer is an unsaturated polyester and the solvent monomer is usually styrene. The final cross-linked product can be considered to have polyester chains with polystyrene cross-links. A typical polyester resin can contain up to 35% by weight of styrene.

Styrene is widely used as solvent monomer mainly for cost reasons. Although, if improved optical properties or lower smoke production in fire are required, methyl methacrylate can partially or fully replace styrene. Diallyl phthalate is used in polyester systems that are required to be solid at room temperature and in “alkyd” moulding compounds. Other solvent monomer systems following the general principle presented in Figure 2.1 are vinyl esters and modified acrylics.

Polyesters are the result of condensation polymerisation between a di-carboxylic acid (or di-acid) and a di-ol (or glycol), Figure 2.3. The final structure is determined mainly by the nature of the X and Y components. Polyester-based polymers are quite stable at ambient temperature and environmentally resistant, but the possibility for hydrolysis exists in the presence of water, especially at higher temperatures or if acids or alkalis are present. This is attributed to the nature of the polyester formation. The condensation reaction in Figure 2.3 results in the evolution of water as a by-product, even though it is carried out at elevated temperature. The length of the polyester
chains is affected not only by the time and temperature in the process, but also by the extent of water-removal. With this in mind the reverse reaction (hydrolysis) can occur when polyester is in the presence of water.

![Diagram of condensation reaction]

Figure 2.3 Condensation of a di-carboxylic acid with a di-ol to form polyester resin.

The unsaturated polyesters that are used in thermosets are co-polyesters containing a mixture of unsaturated and saturated acids. The “reactivity” of the polyester is determined by the unsaturated - saturated acids ratio. This determines the concentration of double bonds in the chain, a factor that controls the crosslink density in the final product. Polyesters with a high reactivity feature high $T_g$ and low water and other diffusing liquids permeability, so they have increased hydrolysis and chemical resistance. The opposite is also true: low reactivity leads to polymers with lower $T_g$. These tend to be less resistant to hydrolysis.

After condensation polymerization, chips of the solid unsaturated polyester are dissolved in the styrene solvent monomer and shipped as polyester resin. Cross-linking during the composite part manufacturing is started by adding a free radical initiator, universally BUT incorrectly referred to as the ‘catalyst’, at a 0.5-1.5% level usually. These initiators are organic peroxide derivatives, and the most commonly used are methyl-ethyl-ketone-peroxide (MEKP) and tertiary-butyl-perbenzoate (TBP). These compounds are unstable and decompose giving free radicals. For the occasion of room temperature curing initiator decomposition is catalysed by a small amount of “accelerator”, previously added to the resin. As accelerator it is often used cobalt naphthenate and it is a true catalyst for the initiator decomposition reaction.
The cure rate of the resin is affected by the quantity and type of initiator added and of course temperature.

Regardless the curing method, full cure is rare if ever achieved in the fabrication process itself and this is because the cure reaction rate slows down to low values as cure evolves and after the system has gelled the reacting species become less and less mobile. This problem is usually tackled by prolonged high temperature oven “post-cure”. Postcuring is desirable, when feasible, because it improves chemical resistance, mechanical properties and the glass transition temperature of the resin.

One common method to improve the short-term fire reaction performance of polyester resins is by either using halogenated versions of the polyester constituents or by using halogenated additives. The most common method of adding halogen is the use of chlorendic acid (also known as HET acid), as presented in Figure 2.4.

![Chlorendic acid](image.png)

**Figure 2.4** Chlorendic acid used in low flammability resins.

Halogens addition is very effective in reducing flammability and is still the main method by which general-purpose low flammability resins are produced. Halogenated resins are known to feature slightly poorer mechanical properties compared to conventional polyesters. Resin suppliers are striving to develop non-halogen methods to modify the polyesters flammability. For example, there is interest in low toxicity polyesters where part or all of the styrene has been replaced by methyl methacrylate, and the resin is highly filled with alumina trihydrate (ATH) [2.2]. These polyester systems feature similar to modified acrylics fire retarded [2.3].

Researchers, including Bansal et al. [2.4] and Gibson and Hume [2.5], have performed work on polyesters decomposition and polyester composites fire behaviour. The thermal decomposition process of all unsaturated polyesters is
governed during the initial stages by scission of highly strained portions of the polystyrene cross-links, with the formation of free radicals that then go on to promote further decomposition, including some accompanying scission of the polyester backbone. This produces a variety of low molecular weight volatiles, including CO, CO₂, methane, ethylene, propylene, butadiene, naphthalene, benzene and toluene [2.6]. In Figure 2.5 TGA curves for various polyester-type resins are presented. It can be seen that 90-95% of the original mass is decomposed into volatiles, rather than char. This is the main reason for the high flammability and heat release of polyester composites.

![TGA curves for solvent monomer resins](image)

Figure 2.5 Thermogravimetric analysis traces for solvent monomer resins at 25°C/min in nitrogen. 1. orthophthalic polyester; 2. isophthalic polyester; 3. halogenated (HET acid) polyester; 4. vinyl ester and 5. Bisphenol-A polyester [2.1].

Despite the fact that polyester decomposition is a two-stage process, single-stage Arrhenius kinetics is often sufficiently accurate to model the process [2.7]. The TGA comparison in Figure 2.5 shows that the qualitative differences in thermal stability of the different polyester resins are not large.

Additionally to flammability and heat release, another disadvantage of styrene-based solvent monomer systems during fire is that styrene component itself tends to produce smoke. Moreover, these resins during decomposition tend, to pass through a low viscosity liquid stage that could result in flaming droplets formation. Char promoting additives like the phosphorus based ones are beneficial [2.8].
2.1.2 Epoxy Vinyl-Ester Resins.

Often referred as vinyl-ester resins. Similar to polyesters, they are solvent monomer systems, as schematically presented in Figure 2.1, with typically up to 44% of styrene monomer. The most common vinyl ester pre-polymer has the structure presented in Figure 2.6. Vinyl-esters are often said to combine the ease of processing of polyester-type resins with some of the improved mechanical properties and chemical resistance of epoxy resins. Generally they feature a higher $T_g$ than polyesters, often in the range of 110$^0$-125$^0$C.

![Figure 2.6 Structure of epoxy vinyl ester pre-polymer.](image)

As with the polyesters, the thermal decomposition of vinyl-esters is governed by styrene component decomposition. The similarity to the polyesters can be observed in the TGA curves in Figure 2.5. The overall fire behaviour is very similar to polyesters, although, the time-to-ignition, heat release rate and smoke generation may often be slightly higher, due to the slightly higher styrene content.

Regnier and Mortaigne [2.9] studied the thermal decomposition of glass/vinyl-ester composites in air or nitrogen using TGA. Pyrolysis commences with the elimination of small molecules at the chain ends, and this is followed by cleavage reactions involving the side chains and random chain scission of the main polymer chain. These reactions produce a large mass fraction of flammable volatiles including styrene, toluene, etc. Vinyl-esters also produce a variety of other combustion gases, including CO and CO$_2$. Likewise to the unsaturated polyesters, most of the polymer is being decomposed into volatiles and only 5-10% of the original mass is converted into char. Since vinyl esters are derivatives of epoxy resin, the main chain can be modified by the addition of other polymers, such as novolacs. Epoxy novolac vinyl-esters provide a means of further increasing the $T_g$, often up to about 150$^0$C. Thermal stability and glass transition temperature of vinyl esters is ultimately governed and limited by the styrene content needed to facilitate processing by the free radical route.
2.1.3 Modified Acrylic Resins (MODAR).

Modified Acrylic Resins, MODAR, are also based on the solvent monomer principle of Figure 2.1. The pre-polymer is a polyurethane and the solvent monomer is methyl methacrylate. The distinguishing features of MODAR resins are their low viscosity and the very fast nature of the cure reaction. Rapid cure limits the applicability mainly to “closed mould” processes, such as resin transfer moulding and pultrusion.

The low resin viscosity enables relatively high levels of fillers to be incorporated, usually alumina trihydrate (ATH), while retaining processability. Methyl methacrylate monomer has a lower tendency to produce smoke than styrene. ATH modified MODAR has been claimed to show an excellent combination of fire reaction properties. These properties are achieved mainly through the low polymer content and through the relatively benign decomposition products of methyl methacrylate, compared to styrene. It should be stressed, however, that increased levels of ATH can limit the structural performance of this type of resin.

2.1.4 Epoxy Resins.

This class of resins is widely used in high performance composites manufacturing as well as in surface coatings and adhesives. It also contains a very large number of chemical types. All these resins rely on the reactivity of the three-membered epoxy group at either end of the molecule. The commonly used epoxy resin is diglycidyl ether of Bisphenol-A (DGEBA), which is prepared by reacting epichlorohydrin with Bisphenol-A under alkaline conditions, Figure 2.7. The main cure process involves reactions with the epoxy end group, but further cross-linking reactions may take place involving the pendant hydroxyl groups attached to the main chain. Typically, epoxy resins are cured by reacting them with amines or anhydrides also called as “hardeners”.

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Figure 2.7 Manufacture of epoxy resin (diglycidyl ether of Bisphenol-A).

Processing of epoxy resins is less straightforward than unsaturated polyesters and vinyl esters because of the fact that cure reactions are slower than with free radical cure. Also, many epoxy resins and hardeners are viscous or even solid at room temperature and may require preheating to lower their viscosity prior to processing into composites manufacturing. Further heating, sometimes to temperatures up to 150-180°C, is often needed to promote resin gelation and curing. Nevertheless, epoxies are used in all the traditional composite processes, including wet lay-up, resin transfer moulding, resin infusion and filament winding. Some of the highest performance epoxy composites are processed via the prepreg method.

A typical TGA curve for a DGEBA-based epoxy tested under nitrogen atmosphere is presented in Figure 2.8. Decomposition of most epoxies occurs via random chain scission reactions over the temperature range of about 380 to 450°C. In amine- or amide-cured epoxies, the nitrogen linkages have lower bond dissociation energies than the ether or ester linkages, and therefore chain scission occurs at the C-N bonds. The hydroxyl groups are also vulnerable to degradation at elevated temperature. The scission reactions decompose 80-90% of the original polymer weight into almost 100 different volatile compounds, which are mainly various types of substituted alkylated phenols, aromatic ether derivations and other combustible organic species [2.10]. These compounds when burnt provide heat for the decomposition reaction to continue until the epoxy is completely degraded. Between 10% to 20% of the original polymer weight is transformed into a highly porous char, and in the presence of air this will
start to oxidize above 550°C. Similar to polyester composites, the combustible volatiles produced during the decomposition reaction is the main reason for the poor fire performance of epoxy matrix composites.

![TGA curve for an epoxy in nitrogen atmosphere](image)

**Figure 2.8** TGA curve for an epoxy in nitrogen atmosphere [2.1].

### 2.1.5 Phenolic Resins

Phenolics are perhaps the oldest form of fully synthetic polymer, and nevertheless are still widely used, often in heat or fire-related applications. The overall polymerization reaction involves condensation of phenol and formaldehyde, occurring with the elimination of water. Phenol has a functionality of 3 (locations where cross-linking can occur) and formaldehyde has 2. Phenolic resins are not manufactured by direct combination of the two precursors because of the difficulty of dealing with the large quantity of water and heat evolved. Instead, pre-polymers of two different types - resoles and novolacs - are used. Resole pre-polymers contain a small excess of formaldehyde, above the stoichiometric requirement, and are polymerised with an alkaline catalyst, Figure 2.9. This results in a pre-polymer in which methylol groups are present in addition to methylene cross-links, the excess formaldehyde is stored in the methylol groups.
The majority of structural phenolic composites manufactured by hand lay-up, filament winding and pultrusion method involve liquid resoles as the pre-polymer. These resins contain 7-15% of water as a result of the initial polymerization. Further water is evolved during cross-linking and cure. Although several steps may be taken to promote the release of water from the laminate during cure, this leads to a high porosity of the final product affecting both mechanical properties and subsequent water absorption. The cure process for resoles usually requires strong Lewis acid catalysts, like the sulphonic acid, but can result in corrosion problems when metal dies or tooling are used.

Novolacs, on the other hand, contain a small excess of phenol and employ an acid catalyst to prepare the pre-polymer, Figure 2.10. Novolac phenolics are used in hot cure processes, where hexamethylene tetramine (HMT) is used to provide the additional methylene cross-links. Curing occurs in mildly alkaline conditions.

The phenolic resins thermal decomposition behavior has been studied extensively because of the usage of phenolic matrix composites in high temperature applications (eg. rocket nozzles, ablative heat shields) and fire resistant components [2.11]-[2.16].
A typical TGA curve for a glass/resole phenolic composite measured in nitrogen atmosphere is presented in Figure 2.11. Contrary to polyester, vinyl ester, epoxy and several other polymer systems, the retained mass of resole phenolic composite decreases with increasing temperature in several stages. That indicates a multi-stage decomposition process. The first stage occurs between 100 and 300°C, and involves a small mass loss due to water vaporization but also volatilisation of unreacted monomers, phenols, formaldehyde and free species from the catalyst. During this stage the polymer network remains largely intact. Above ~300°C the second stage kicks in, mainly involving scission reactions between the dihydroxydiphenylmethane units along the chain, with elimination of some volatile by-products. These reactions are partially oxidative in nature, and the resin itself can act as an oxygen donor. The reaction rate reaches a maximum in the second stage, and a variety of volatile gases are produced including CO, methane, phenol, cresols and xylenols. In contrast to other thermoset systems, much of the higher molecular weight aromatic material that remains after the scission reactions condenses to form a solid material.

The final stage involves the further fusion of aromatic rings into forming a carbonaceous char, with some further evolution of volatiles. The char formation temperature increases with the heating rate, as presented in Figure 2.12. What this graph suggests is that when a phenolic composite is exposed to fire then the char formation temperature will decrease with distance below the hot surface. Because of
the high aromatic content, 40-60% of the original mass of phenolic resins is transformed into carbonaceous char, resulting in a much lower production of combustible volatiles compared to other polymers. and hence the low flammability and superior fire performance of phenolic composites.

![Figure 2.12 Effect of heating rate on the formation temperature of char in a glass/phenolic composite](image)

**2.2 Thermoplastic Resins for Composites.**

Thermoplastic matrix composites for the moment are a small fraction of the total composites but this fraction is increasing, driven by the need for faster cycle times, cleaner processing technologies and recyclability. Various types of thermoplastics are used including polypropylene (PP), polyethylene-terephthalate (PET), polyamide (PA), poly-ether ether-ketone (PEEK) and polyphenylene-sulphide (PPS), Figure 2.13.

**2.2.1 Poly-ether ether-ketone (PEEK).**

PEEK has good thermal stability and a high melting point for a thermoplastic, 380°C, and for these reasons it is used in elevated temperature applications in aircraft, space and other demanding performance applications[2.17]. The monomer unit to PEEK is characterized by three aromatic units in the main chain, with both the ether and ketone linkages having a high level of thermal stability. Day et al. [2.18] and Moulinie et al. [2.19] concluded that the decomposition of PEEK begins at about 500°C by a primary random chain-scission reaction of the ether bonds and then, under
more severe pyrolysis conditions, random scission of the ketone linkages. Despite the fact that decomposition occurs by two scission reactions, PEEK appears to degrade, under nitrogen atmosphere, in a single-stage process between 500 and 640°C. The major volatile decomposition product is phenol, with smaller fractions of other organic gases like diphenoxylbenzene, dibenzofuran, benzene, naphthalene and bis(phenoxyl)benzophenone [2.1]. An interesting and important feature of the decomposition reaction is the high char production (~60% of the original mass) due to the high concentration of aromatic rings in the polymer chain. The chain fragments containing aromatic rings fuse via a condensation reaction at high temperature to form a stable carbonaceous char. The increased char formation of PEEK, with the consequent reduction in combustible volatiles, results in flammability resistance superior to most other thermoplastics as well as the styrenic or solvent monomer-based thermosets. This behavior makes PEEK a very good ablating material for use in charring spacecraft ablators.

![Thermoplastic resins used in composite materials.](image)

**2.2.2 Polypropylene (PP).**

Polypropylene (PP) is an olefin widely used for cost-effective composite parts, it is being used in preference to polyethylene (PE) because low viscosity grades are readily available promoting good reinforcement impregnation. Another advantage is
its very low polarity, which can be easily modified by the addition of coupling agents to promote strong bonding with inorganic fibres. PP is generally less thermally stable than PE because of the “tertiary” carbon atom that constitutes a weak point in the backbone chain.

The first step in the PP thermal decomposition is main chain scission at the tertiary carbon atom or in general for the olefin polymers as it is called “homolytic dissociation”. This reaction forms two free radicals which go through a chain reaction mechanism to attack the chain at further random points resulting in the formation of a range of olefinic fragments. All olefin polymers tend to decompose completely into volatile products, leaving no char. In the presence of oxygen, free radical formation is accelerated, feature that reduces the decomposition temperature. Oxygen also modifies the composition of the decomposition products to give aldehydes in addition to olefinic products. The poor thermal stability of PP can be altered by adding free radical absorbing stabilizers. These also improve resistance to degradation during high temperature processing and to UV attack at ambient temperature, the latter being a problem with all olefin polymers.

2.2.3 Polyphenylene Sulphide (PPS).

PPS is also used in high performance thermoplastic matrix composites because, unlike PEEK, it forms a molten phase with relatively lower viscosity assisting impregnation and processing. PPS decomposes in a single-stage reaction over the temperature range 380 to 500°C [2.20]. Decomposition occurs by random scission of the C-S bonds along the PPS chain followed by depolymerisation and cyclisation reactions. The major volatile pyrolysis products are benzenethiol, linear trimers and dimers, cyclic tetramer and benzene. The char production of PSS is approximately 60%, due to the high density of aromatic ring compounds in the molecular chain. Both PEEK and PPS feature char formation fractions comparable to phenolic resins [2.21].

2.3 References


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thermogravimetry-fourier transform infrared spectroscopy (TGFTIR)”.


CHAPTER 3 Composite Materials Fire Response Characteristics.

When composite materials are subjected to fire conditions their response can be described by a number of characteristics. These are, heat release rate HRR, thermal stability index, limiting oxygen index, extinction flammability index, time-to-ignition, surface spread of flames, mass loss, smoke density and smoke toxicity. The fire burnthrough resistance is not mentioned in this chapter but instead it is extensively discussed in the rest of this dissertation. A variety of polymer laminates and sandwich composites are referred in this chapter. Special attention is given to polyester-, vinyl ester-, epoxy- and phenolic-matrix laminates because of their widespread use in aerospace, civil, marine and offshore industries. Furthermore in terms of completeness advanced thermoset and thermoplastic composite materials are mentioned.

3.1 Heat Release Rate, HRR.

Heat Release Rate (HRR) is the single most important fire reaction property because the heat released by a burning material can provide the additional thermal
energy required for the growth and spread of fire [3.1],[3.3]. No other reaction property has such a dominant influence on the fire behavior of composites. Furthermore, several other reaction properties, such as surface spread of flame, smoke generation and CO emission, are dependant on or related to the HRR [3.4],[3.5].

Heat release is the thermal energy produced, per unit area of surface, when flammable decomposition products ignite and burn in the vicinity of a material in fire, or subjected to heat flux. The peak HRR occurs over a very short period of time and often shortly after ignition, and is usually a good indication of the maximum flammability of a material. The average HRR is the total heat released averaged over the combustion period, and is considered the most reliable measure of the heat contribution to a sustained fire.

Various instruments have been developed to measure the HRR properties of composite materials, with the most popular techniques being the cone calorimeter and Ohio State University heat release calorimeter. These instruments are limited to testing small specimens of a composite material. The HRR properties of components, assemblies and structures made of composite material can be measured using large-scale fire tests like the single-burning item test, intermediate-scale cone calorimeter and room fire calorimeter test.

The HRR properties have been determined for a diverse variety of composites, including polyester, vinyl ester and epoxy matrix laminates that are used in aerospace, marine and construction applications [3.6]-[3.16]. An example of the HRR response over time for a typical flammable composite material (glass/vinyl ester) that is exposed to a constant heat flux is shown in Figure 3.1. The HRR was measured using a cone calorimeter and is defined as the heat released per unit time by the composite sample divided by the exposed surface area of the sample. The HRR profile fluctuates considerably over time due to various chemical and thermal events that occur to the composite when exposed to fire, and these events are designated as A to D in Figure 3.1. This figure shows an initial induction period (event-A) during which the composite does not release any heat. During this period the exposure time to the external heat flux is insufficient to heat the composite to the decomposition temperature. This delay is followed by a rapid rise in the HRR spectrum (event-B)
due to the sudden, short-term release of heat from the ignition of flammable volatiles released from the resin-rich surface film on the composite. The curve continues to rise to a peak HRR, after which the Heat Release Rate decreases progressively with time due to char formation and growth at the hot surface (event-C). Char reduces the HRR in two ways: (i) it acts as a thermal insulator which retards heat transfer to the underlying virgin material and therefore slows the decomposition reaction rate, and (ii) it limits the supply of combustible gases to the flame front. In some materials, the HRR curve may rise again, and at this stage the specimen is nearly completely degraded by the fire. Only a small portion of virgin composite remains near the unexposed surface of the sample, and behaves in the manner of a thermally thin material. This causes the rapid decomposition of the last remaining amount of resin matrix, resulting in an increase to the HRR. Eventually the HRR becomes negligible when the polymer matrix has completely degraded (event D). The profile shown in Figure 3.1 for the vinyl ester matrix composite is typical of the HRR response of composites that yield low amounts of char, and similar behavior is found for polyester and epoxy laminates.

A somewhat different HRR response is observed for more fire resistant composites, like the phenolic laminates [3.9],[3.10],[3.12],[3.16]. The HRR profile for a glass/phenolic composite is shown in Figure 3.2, and the rate that heat is released by the phenolic composite is substantially lower because of the higher thermal stability and lower release rate of flammable volatiles from the polymer matrix. The start of the release of heat is delayed for a longer time with the phenolic laminate and the amount of heat released is much lower than for the vinyl ester laminate shown in Figure 3.2. This behavior is often observed for advanced thermoset and high temperature thermoplastic composites that contain thermally stable resins that yield a high amount of char and generate low levels of combustible volatiles.
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Figure 3.1  Typical heat release rate profile for a glass/vinyl ester composite exposed to a heat flux of 50 kW/m$^2$.

The heat flux of a fire has a significant influence on the rate of heat release from a composite material [3.9],[3.10],[3.12],[3.17],[3.18],[3.19],[3.20]. Figure 3.3 shows the effect of increasing heat flux on the peak HRR values for a variety of glass/thermoset laminates. It is seen that the heat released by a highly flammable composite material (ie. glass/epoxy) rises rapidly with heat flux due to increases in the decomposition reaction rate and production of combustibles. In comparison, the amount of heat released by high-performance thermoset composites (eg. phenolic, cyanate ester, phthalonitrile) is much lower at all heat flux levels. In many cases the heat released by these composites is just a small fraction of that released by the epoxy-based laminate. Furthermore, the peak and average HRRs of the high-performance thermoset laminates are less sensitive to increases in the external heat flux.

Figure 3.2  Typical heat release rate profile for a glass/phenolic composite exposed to a heat flux of 50 kW/m$^2$.

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The superior fire performance of these composites is attributed to the highly aromatic composition of the polymer matrix that confers high thermal stability and an ability to produce a high amount of char during burning. High-temperature thermoplastic composites also possess much better fire performance than styrene-based polymer materials [3.14]. In Figure 3.4 the peak and average HRR values for a standard carbon/epoxy laminate is compared against values for a variety of carbon fiber reinforced composites with high-temperature thermoplastic matrices. As with high-performance thermoset laminates, the excellent fire performance of these thermoplastic composites is due to the thermal stability, low release rate of flammable volatiles and high char formation of the polymer matrix.
The heat released by composite materials can be affected by the fiber reinforcement in several ways. Le Bras et al. [3.15] and Mouritz and Kootsookos [3.21] have shown that increasing the fiber content causes a reduction in the amount of heat released by a burning composite. For example, Figure 3.5 shows a rapid decline in the peak HRR of a glass/polyester composite with increasing fiber content. This behavior occurs because less polymer matrix material is available to generate heat during thermal decomposition. Numerous studies have found that the type of fabric used to reinforce a composite can also affect the heat release properties [3.7], [3.10], [3.12], [3.22], [3.23]. In particular, significant differences can occur in the heat release response of composites reinforced with woven roving or chopped mat fabrics. The HRR profile for the chopped fiber composite is characterized by a broad peak, and this is indicative of relatively steady-state decomposition throughout most of the material. The profile for a woven composite, on the other hand, can fluctuate with a series of peaks and valleys over time that is indicative of erratic burning, as shown in Figure 3.6. These differences in the HRR responses are attributed to the different distribution of glass fiber and resin in the composites. Chopped fiber laminates have a reasonably even distribution of resin between the glass layers, and this facilitates the stable formation and combustion of degradation products when exposed to fire. In contrast, woven fabric composites have a distinct layered construction, with resin-rich regions between the plies. When exposed to fire the burning rate through the laminate varies considerably, with higher amounts of volatiles released during decomposition of the resin-rich layers that results in the peaks to the HRR spectrum. As combustion progresses through each glass layer, where less resin is present, the heat release rate is reduced that causes the valleys in the HRR curve.
The HRR properties are dependent on thickness with numerous fire studies reporting large reductions to the peak and average HRRs with increasing thickness [3.10][3.16][3.22][3.24]. Figure 3.7 shows the effect of increasing thickness on the heat released from four types of fiberglass laminates. In this figure the HRR values are expressed as the total heat release THR per unit volume of composite, and all the materials have been fire tested at a constant incident heat flux of 60 kW/m². It can be seen that the HRR decreases rapidly with increasing thickness to about 8 mm, but above this value the HRR is insensitive to further increases in thickness. A similar relationship between peak HRR and thickness was measured by Scudamore [3.22] for glass/phenolic laminates when fire tested at different heat fluxes, and again the HRR was only dependent on thickness at values under 8 mm. When composites are very thin the heat penetrates rapidly through the material, causing the complete decomposition of the resin matrix within a short period that accelerates the heat release rate. Composites behave increasingly as a thermally thin material as their thickness is reduced below 8 mm, and this is the cause of the progressive increase in HRR.
Figure 3.6 Comparison of the HRR profiles for a composite reinforced with chopped glass mat and woven glass fabric [3.1].

Fire tests conducted by Ohlemiller and Shields [3.25] reveal that the dependence of HRR on thickness can be quite complex, particularly when the composite is thermally thin. Most fire reaction studies on composite materials are conducted as single-sided fire tests, and only rarely are double-sided tests performed. In the only reported study into the double-sided fire properties, Ohlemiller and Shields [3.25] compared the heat release behavior of polymer composites when subjected to one-sided or two-sided burning. It was found that when a thermally thick composite is exposed to fire load of equal intensity (ie. identical heat flux) on both sides then the HRR simply doubles because twice as much surface area is involved. Contrary to this the response of a thermally thin laminate to two-sided burning is more complex than this, and usually the HRR is more than doubled. For example, Figure 3.8 compares the peak HRR of a thermally thin glass/epoxy laminate when subjected to one-sided and two-sided burning. In this case the HRR is over two times higher for two-sided burning. Ohlemiller and Shields [3.25] report that the thermal process responsible for this effect is not well understood, although they suggest that when a thermally thin composite experiences two-sided burning the thermal waves from the two surfaces merge at a point, resulting in an acceleration in the decomposition reaction rate that is much greater than two.
Composite Materials Fire Response Characteristics.

Figure 3.7 Effect of thickness on the THR of various fiberglass composites. The THR is expressed as the total heat released per unit volume of composite material [3.10].

Figure 3.8 Effect of one- and two-sided burning on a thermally thin glass/epoxy laminate (1.6 mm thick) on the peak HRR [3.25]

### 3.2 Thermal Stability Index, TSI.

Another fire reaction property that is used to describe flammability is the Thermal Stability (or sensitivity) Index (TSI). The TSI provides a value by which the fire performance of materials may be ranked and compared over a range of external heat fluxes, simulating different fire environments [3.6]. It can be calculated from the plot of the average HRR against the incident heat flux, Figure 3.9. The intercept of such a plot in principle indicates whether flaming combustion of a material is self-sustaining.
in the absence of an external heat flux. On the other hand, the slope of the line expresses the thermal stability index. A low TSI value indicates that a material will burn near its maximum rate even at low heat fluxes. The higher the TSI the greater must be the external heat flux for the material to burn at its maximum rate. Again, high temperature thermoset and thermoplastic composites have relatively high TSI values compared against flammable materials.

![Plots of incident heat flux against heat release rate showing the determination of ESI and TSI for glass/polyester and glass/phenolic laminates](image)

**Figure 3.9** Plots of incident heat flux against heat release rate showing the determination of ESI and TSI for glass/polyester and glass/phenolic laminates [3.9].

### 3.3 Limiting Oxygen Index, LOI.

The Limiting Oxygen Index (LOI) is often used to quantify the flammability of organic polymers and composite materials. The LOI is defined as the minimum percentage of oxygen needed to sustain flaming combustion, and thus may be considered as a measure of the ease of self-extinguishment of a burning material [3.26]. The LOI is experimentally determined using the oxygen index test. In brief, the test involves subjecting a sample material to an ignition flame under atmospheres having different oxygen levels, and from this determining the minimum oxygen content that allows the sample to burn with a candle-like flame. The method does not test the sample in a realistic fire environment, and therefore the LOI index cannot be used to accurately quantify the fire behavior of a material. However, the oxygen index test is often used to rank the relative flammability of polymer composite materials [3.15],[3.19],[3.26]-[3.32].
Composite Materials Fire Response Characteristics.

The LOI values for a range of thermoset and thermoplastic composites are presented in Figure 3.10. It can be seen that the LOI values for highly flammable composites, such as polyester-, vinyl ester- and epoxy-based materials, are below 30. Composites with highly stable or aromatic polymers have much higher index values. It is generally recognised that the LOI values for polymers and polymer composites increase with their ability to yield char in a fire [3.31],[3.33]-[3.35]. This is because the formation of char occurs at the expense of combustible volatiles, which in turn increases the oxygen level required to sustain flaming combustion. In addition to the type of polymer matrix, the LOI value can be affected by other factors, most notably the degree of resin cure, fiber content, and the flammability of the fiber reinforcement [3.15],[3.26],[3.29],[3.36],[3.37].

The LOI index values shown in Figure 3.10 were determined at room temperature. However, a composite material will reach a much higher temperatures in a fire. LOI studies have shown the index values of composites are dependent on the test temperature [3.26],[3.30],[3.32]. The values can change dramatically with temperature, usually decreasing with increasing temperature, and often changing the relative ranking of some materials. It is therefore questionable to use LOI values measured at room temperature to assess the flammability of composite materials. Figure 3.11 shows the effect of test temperature from 25°C to 300°C on the LOI of two fiberglass composites. The index values increase with temperature up to 100°C, but at higher temperatures there is a steady reduction in the values because less heat is needed to sustain decomposition and burning.

While the LOI is often used to characterize the fire performance of composites, there is no clear correlation between the index value and other fire response properties [3.26]. Therefore, it is not valid to use the index value as a quantitative measure of fire resistance, although it can be used to rank the relative flammability of different composite materials.
Figure 3.10  LOI values for various thermoset and thermoplastic composite materials at room temperature [3.8],[3.29].

Figure 3.11  Effect of increasing temperature on the LOI values for two fiberglass composites [3.26].

### 3.4 Extinction Flammability Index, ESI.

The Extinction Flammability Index (ESI) is a useful quantitative measure of the flammability of a composite material, although it is a property that is rarely determined because of the greater attention given to heat release rate [3.6],[3.7],[3.12]. The ESI is determined by plotting the average HRR against the incident heat flux, as presented in Figure 3.9. The HRR value at which the line intercepts the y-axis (ie. heat flux = 0) is considered as the ESI. A negative ESI implies that a material will self-extinguish soon after the heat flux is removed, and an example of this behavior for a phenolic laminate is shown in Figure 3.9. Other examples of composites with negative ESI values include advanced thermoset resins...
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(eg. polyimide, phthalonitrile) and high temperature thermoplastics (eg. PEEK, PPS, PES). When a composite has a positive ESI value this indicates that combustion will continue after the external fire has been extinguished, and the glass/polyester laminate shown in Figure 3.9 is such a material. In addition to polyester laminates, other composites with positive ESI values are epoxy- and vinyl ester-based materials. Generally, composite materials containing a resin matrix that has low thermal stability and a high production of flammable volatiles will have a positive ESI value.

3.5 Time-to-Ignition.

Ignition is an important fire reaction property of flammable materials because it defines the start of flaming combustion. The organic resins commonly used in composites (eg. polyesters, vinyl esters and epoxies) can ignite within a very short time of being exposed to a hot fire. Following ignition, composites often burn with large, high-temperature flames that contribute to the rapid spread of fire. For this reason, ignition is an important property in describing the fire hazard of composite materials.

Ignition usually occurs when the surface of a composite exposed to fire is heated to about the endothermic decomposition temperature of the polymer matrix. The thermal decomposition reaction of the matrix produces flammable volatile gases that flow from the composite into the fire. When the amount of volatiles at the composite/fire interface reaches a critical concentration then ignition and flaming combustion will occur. Most of the volatiles are generated by the endothermic decomposition of the polymer matrix, and depending on the type of resin may include a mixture of flammable components such as carbon monoxide, styrene vapour, aromatic compounds and other low molecular weight hydrocarbons. Smaller quantities of volatiles can be produced by the decomposition of organic sizing and binding agents that coat the fiber reinforcement. Volatiles can also be released by organic (combustible) fibers such as aramid and UHMW polyethylene.

The ease of ignition is generally characterized by the time-to-ignition, which is the minimum time required to promote ignition and continuous flaming of a combustible material when exposed to an external heat flux. The ignition time depends on a variety
of factors such as oxygen availability, temperature and the chemical and thermophysical properties of the polymer matrix and fiber reinforcement. In this section the effects of resin composition, resin content, fiber composition, fiber sizing agents, specimen thickness, heat flux and the fire atmosphere on the ignition response of composite laminates are described. The ignition properties of sandwich composite materials will be also discussed.

The time-to-ignition of composites is determined by experimental fire testing using techniques such as cone calorimetry and the ISO ignitibility test. Ignition times can be measured in two conditions called spontaneous and piloted ignition. In spontaneous ignition, flaming occurs spontaneously within the flammable vapour/air mixture at the hot composite surface. Piloted ignition is initiated in a flammable vapour/air mixture by an external pilot or ignition source, such as an electrical spark or flame. In this work the piloted ignition is discussed.

Greater interest in ignition is focused on composite laminates used in aerospace, marine and offshore applications. For this reason, the ignition times of laminates with polyester, vinyl ester, epoxy and phenolic resin matrices have been determined for a wide variety of fire conditions [3.6]-[3.13],[3.17],[3.22],[3.23],[3.26],[3.38]-[3.46]. Figure 3.12 shows the typical effect of external heat flux on the ignition times for fiberglass laminates with different resin matrices. The composites do not ignite below a threshold heat flux, even after exposure to fire for a very long time. The threshold heat flux below which piloted ignition will not occur for polyester, vinyl ester and epoxy composites is ~13 kW/m² whilst for phenolic laminate is ~25 kW/m². Ignition cannot occur because the heat flux is too low to heat the composite to the decomposition temperature of the resin matrix. Above the threshold value, the ignition times decrease rapidly with increasing heat flux. A log-log relationship between decreasing ignition time and increasing heat flux is generally observed for most types of polymer composites. The rapid reduction in the ignition times with increasing heat flux is attributed to the large increase in the pyrolysis rate. An increase in heat flux (or temperature) increases rapidly the production and flow rates of volatiles to the composite/fire interface, thereby lowering the time-to-ignition [3.6].
Figure 3.12 Log-log plot of ignition time against incident heat flux for fiberglass laminates with a polyester, vinyl ester, epoxy and phenolic matrix. The m values define the slope of the curves [3.10].

Figure 3.12 also shows that the ignition times for the phenolic laminate is considerably longer than the other materials. Delayed ignition is one of the outstanding fire response properties of phenolic composites, and is due to the high decomposition temperature, high charring tendency on decomposition, and low production of combustible volatiles from the phenolic resin matrix [3.8],[3.9],[3.12],[3.14],[3.22],[3.23],[3.26],[3.38],[3.42],[3.43],[3.46],[3.47].

One of the main reasons for the common usage of phenolic composites in high fire-risk applications is their excellent ignition resistance. However, there are other high temperature thermoset resins that provide composites with long ignition times, most notably bismaleimides, polyimides, cyanate esters and phthalonitriles [3.8],[3.11],[3.14],[3.17],[3.18],[3.40],[3.48]. In addition, Sorathia and colleagues have shown that many thermoplastic composites have excellent ignition times, even at high fluxes, including polyphenylene sulfide (PPS), polyether ether ketone (PEEK) and polyether ketone ketone (PEKK) laminates [3.8],[3.14],[3.26]. For example, Figure 3.13 compares the ignition time of a standard glass/vinyl ester laminate against various high-temperature thermoset and thermoplastic composites when fire tested at a heat flux of 75 kW/m². Compared to the glass/vinyl ester the other composites have much longer ignition times, particularly the phthalonitrile- and cyanate ester-based materials. The excellent ignition resistance of these materials is due to several factors,
most importantly high thermal stability and low release of flammable volatiles because the resins yield a high amount of char during decomposition.

![Graph showing ignition times for various fiberglass composites](image)

Figure 3.13 Ignition times for various fiberglass composites with advanced thermoset and thermoplastic matrices at the heat flux of 75 kW/m² [3.8]

While the ignition properties of many types of polymer laminates have been determined, there is little published information on the ignition times of sandwich composite materials [3.47],[3.49],[3.50]. Most interest has focused on the ignition behavior of sandwich composites used in boat and ship construction, and limited information is available for non-marine sandwich materials. Ignition of sandwich composites can be a complex process because of the influence of the core material. Figure 3.14 is a log-log plot showing the effect of incident heat flux on the ignition times of two types of marine sandwich composites. It is seen that the GRP skin-balsa core composite shows a linear relationship between ignition time and heat flux when plotted on a log-log scale, and this behavior is identical to single-skin laminate (eg. Figure 3.12). The GRP skin-PVC foam core composite, on the other hand, does not show a linear relationship at high heat fluxes. Furthermore, the ignition times for this material are considerably shorter at heat flux values under~50 kW/m², and this is due to the influence of the PVC core on the ignition process. When exposed to fire the PVC core melts and decomposes, and this causes an air-gap to form between the skin and core. This air-gap reduces the rate of heat conduction through the skin, causing it to heat-up and ignite more rapidly. In the case of the balsa sandwich, on the other hand, heat transfer is not interrupted because the core remains in contact with the skin, resulting in longer ignition times. Core materials with a low thermal conductivity,
such as polymer foams and honeycombs, can also lower the ignition time. These types of core materials are relatively inefficient in conducting heat away from the face skin exposed to the fire. This leads to the rapid heating of the face skin causing early ignition. This is illustrated in Figure 3.14 where the thermal conductivity of the PVC core is substantially lower than the balsa core, and this may be partly responsible for the shorter ignition times of the GRP skin-PVC foam core sandwich composite.

![Figure 3.14 Effect of heat flux on the ignition times of two sandwich composites. The difference in the two curves is due to the influence of the core material on the ignition process [3.49]](image)

The fiber reinforcement can also influence the ignition time. Glass and carbon fibers are inert to fire when the heat flux is below 100-125 kW/m², although the reinforcement can still affect the ignition process. Le Bras et al. [3.15] and Kootsookos and Mouritz [3.21] found that the amount of reinforcement changes the ignition resistance of fiberglass composites. Figure 3.15 shows the effect of fiber content on the ignition time of a glass/polyester composite. It is seen that raising the fiber volume content from 0% (i.e. neat resin) to nearly 70% causes a substantial increase in the ignition time. Increasing the fiber content obviously reduces the amount of resin in a composite, and therefore less combustible material is available to produce flammable volatiles needed for ignition.

The fibers can contribute in various other ways to the ignition process. While glass and carbon fibers remain inert in fires with temperatures below ~1000-1200°C, the sizes, emulsion binders and other organic agents applied to fibers during manufacture to promote adhesion, binding, anti-static properties and abrasion resistance will
thermally decompose. The fibers are coated with a thin film of organic agents such as film formers, antistatic agents and lubricants to a weight content of ~1-2%. In a fire this coating decomposes and releases flammable volatiles that can reduce the ignition time. The film of organic agents coating the fibers is usually very thin (~0.1 μm), and therefore produces a much smaller amount of volatiles than the resin matrix. However, certain types of reinforcement – most notably chopped strand mats – contain a significant amount of organic emulsion binding agent to bind the fiber strands, and in a fire this can produce volatiles that lower the ignition time. For example, Figure 3.16 compares the ignition times of woven glass/polyester and chopped glass/polyester composites at different heat fluxes [3.12]. The glass fibers in both composites are coated with a similar sizing agent, although the chopped glass fibers are also coated with an organic emulsion binder to hold the short fiber strands together as a mat. It can be seen that the chopped glass composite has substantially lower ignition times, particularly at low to intermediate heat fluxes (<50 kW/m²). The generation of flammable volatiles in the decomposition of the emulsion binder is believed responsible for the inferior ignition resistance of the chopped fiber composite [3.12],[3.22].

![Figure 3.15](image_url)  
Figure 3.15  Effect of glass fiber content on the ignition time of a glass/polyester composite [3.15].

The ignition resistance can also be reduced by combustion of the fiber reinforcement itself. Combustible organic fibers such as aramid and Spectra (UHMW polyethylene) can thermally decompose along with the organic matrix in a fire, and thereby reduce the ignition time. The ignition behavior of composite materials
containing combustible reinforcing fibers in addition to a combustible organic matrix has not been thoroughly investigated, and is a topic that requires further investigation.

In one of few reported studies, Brown et al. [3.41] examined the ignition properties of composites reinforced with aramid or extended-chain polyethylene fibers. It was discovered that the ignition times of these composites are shorter than composites reinforced with non-combustible fibers such as glass. Figure 3.17 compares the ignitions times for vinyl ester-based composites containing woven glass, aramid or polyethylene fibers at two heat flux levels. It is seen that ignition occurs more readily when organic fibers are present, particularly when polyethylene reinforcement is present. The polyethylene and, to a lesser extent, aramid fibers thermally degrade with the polymer matrix in a fire, resulting in increased volatile release that causes the ignition time to be reduced.

The time-to-ignition is also dependent on thickness [3.7],[3.22],[3.24]. Figure 3.18 shows the effect of specimen thickness on the ignition time of a glass/phenolic laminate tested at different heat fluxes. It is seen that the thickness-sensitivity is most marked at low heat fluxes. At 35 kW/m$^2$ the ignition time increases rapidly and continuously with thickness. At higher heat fluxes, however, the ignition time increases gradually with thickness up to ~3 mm, and above this value the time is almost independent of thickness. Hume [3.7] suggests that the increase in ignition
time with thickness is due to an increase in the thermal capacity of the material, prolonging the time taken to heat the laminate to the resin decomposition temperature.

![Figure 3.17 Effect of heat flux on the ignition times of vinyl ester-based laminates containing glass, aramid or extended chain polyethylene fibers [3.12],[3.41].](image)

The fire environment also has a large influence on the ignition properties of polymer composites. Most studies into the ignition behavior are performed under standard atmospheric conditions (e.g. 21%O₂/78%N₂). However the atmosphere to some fires can be substantially different. For example, when a fire occurs within an enclosed space without ventilation, the oxygen content drops with time and can reach as low as ~10-12% before the flames are extinguished from the lack of oxygen. Space agencies such as NASA, on the other hand, are concerned about ignition of combustible materials within the oxygen-rich atmosphere of manned spacecraft. Studies into the ignition properties under non-standard atmospheric conditions are limited, although the few studies that have been performed show that the oxygen content of the atmosphere can affect the ignition time.
Hshieh and Beeson [3.13] investigated the effect of oxygen concentration between 18% and 30% in the fire atmosphere on the ignition time of epoxy and phenolic composites. This investigation was performed using an atmosphere-controlled cone calorimeter that allows the fire properties to be determined under different atmospheric conditions. Figure 3.19a shows the effect of oxygen content on the time-to-ignition values for carbon, glass and aramid reinforced epoxy laminates. It can be seen that the ignition times decrease slowly with increasing oxygen content, and this is because more oxygen is available to react in the flame with the flammable volatiles released during the decomposition of the epoxy matrix and aramid fibers. Figure 3.19b shows the influence of oxygen content on the ignition times for several phenolic composites, and in this case different relationships are observed. The carbon/phenolic shows a rapid reduction in the time-to-ignition with increasing oxygen content, the glass/phenolic shows a small increase in ignition time with oxygen concentration, while the aramid/phenolic shows no clear correlation between the oxygen level and ignition time.

Ignition times are generally determined experimentally, because of the difficulty in modelling the ignition mechanisms of composites. Many theories have been proposed for calculating the ignition times of combustible materials, although in almost all cases the models have only been proven accurate for materials such as woods and plastics and have not been validated for polymer composites. It is difficult to theoretically model the ignition (and other fire reaction properties) of composites due to the complex decomposition reactions of the resin matrix and combustible fibers, the
anisotropic heat flow behavior, the mass flow of volatiles, and other events that occur in a fire. When composites are exposed to fire they can selectively burn, chemically degrade, char, and release volatile gases, smoke, fumes and heat as well as crack and delaminate. The complexity of these processes does not permit easy analytical prediction. Nevertheless, several ignition models have been found to give good estimates of the time-to-ignition in certain cases.

Figure 3.19 Effect of oxygen content of the ignition times for (a) epoxy-based and (b) phenolic-based composites [3.13].

Lyon et al. [3.51] report that ignition will occur when there is sufficient thermal energy at a polymer surface to convert it from a solid to gaseous fuel. The heat of gasification per unit mass of solid polymer \( h_g \) can be calculated using:

\[
    h_g = c(T_{ign} - T_o) + (1 - \mu)h_v
\]

where \( T_{ign} \) is the ignition temperature, \( T_o \) is the ambient temperature, \( h_v \) is the heat of vaporization of the decomposition products, and \( \mu \) is the mass fraction of non-combustible material in the polymer, which can include char, filler particles and/or fiber reinforcement. The temperature dependence of the heat capacity is \( c = c_0 \frac{T}{T_0} \), where \( c_0 \) is the heat capacity at ambient temperature. Based on this analysis, the heat of gasification is related to the ignition temperature of a polymer via the expression:
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\[ h_g = \int_{T_0}^{T_i} c(T)\,dT + (1 - \mu)h_i = \frac{c_o T_{\text{ign}}^2}{2T_0} + \left[ (1 - \mu)h_i - \frac{c_o T_0}{2} \right] = \frac{c_o T_{\text{ign}}^2}{2T_0} \]

or

\[ T_{\text{ign}} = \left[ \frac{T_0 h_g}{c_o} \right]^{1/2} \quad (3.2) \]

Knowing the values of \( h_g \) and \( c_o \) for a polymer, it is possible to estimate the ignition temperature. Figure 3.20 compares the calculated and measured ignition temperatures for several polymer systems, and good agreement is observed.

![Figure 3.20](image)

As already mentioned, one of the critical factors influencing the ignition of combustible materials, including polymer composites, is their thermal thickness (as shown in Figure 3.18). A material is classified as ‘thermally thin’ when the heat from the fire is absorbed so rapidly that there is no significant temperature gradient through it. In a ‘thermally thick’ material, by contrast, a significant temperature gradient exists through the material. Mikkola and Wichman [3.52] propose that the thermal thickness of a material can be defined by its characteristic thermal conduction length (\( \Delta \)) which is calculated using:

\[ \Delta = \sqrt{\alpha t_i} = \frac{k t_i}{\rho c} \quad (3.3) \]
where \( \alpha \) is thermal diffusivity, \( t_i \) is ignition time, \( k \) is thermal conductivity, \( \rho \) is material density and \( c \) is specific heat. This equation is only valid for ‘simple’ radiative thermal ignition, and does not consider heat losses such as the emissivity of the material. When the characteristic thermal conduction length is greater than the sample thickness, \( L_o \), then it is considered to be thermally thin. In practice, this means that most composite materials are thermally thin when less than \( \sim 1-2 \) mm thick.

Taking this into consideration, then time-to-ignition of a homogenous material can be determined by [3.52],[3.53]:

\[
t_i = \rho L_o \left( \frac{T_i - T_o}{q_{net}} \right)
\]

(3.4)

where \( T_i \) is the surface temperature at ignition, \( T_o \) is ambient temperature, and \( q_{net} \) is net heat flux to the surface (including heat losses). Equation (3.4) shows the ignition time of a thermally thin material decreases linearly with increasing net heat flux.

For the case of the thermally thick composites, Carslaw and Jaeger [3.54] report that the ignition time of a thermally thick material is proportional to the inverse square of net heat flux to the surface according to the model:

\[
t_i = \frac{\rho c k}{4} \frac{\pi}{q_{net}} \left( T_i - T_o \right)
\]

(3.5)

In this expression it is assumed that heat losses are negligible and the substrate is an inert, thermally thick and opaque solid. This equation has proven accurate in the theoretical determination of the ignition times for thermally thick specimens of wood and plastics, but not polymer composites. Equation (3.5) suggests that a log-log plot of ignition time vs. heat flux should give a straight line with a slope of \(-2\) for an inert, thermally thick material. The plots in Figure 3.14 for the fiberglass composites show values for the slope \((m)\) of about \(-2\), confirming this mathematical expression. However, calculating the ignition time using Eqn.(3.5) is difficult because the values of \( \rho, c \) and \( k \) change with increasing temperature, and these need to be empirically determined at the ignition temperature.
There is also the intermediate case between thermally thin and thermally thick. Here, the ignition time can be approximated by:

\[ t_\text{i} \approx \frac{\rho c}{k_L} \left( \frac{T_\text{i} - T_\text{a}}{q_{\text{in}} - q_{\text{out}}} \right)^{1/2} \]

(3.6)

3.6 Surface Flame Spread.

The speed at which flames spread over the surface of combustible materials is a critical factor in the growth and spread of fire [3.55]. Due to the high flammability of many composites, there is a serious safety concern that flames will quickly spread and thereby increase the difficulty in containing and extinguishing a fire. This is one of the key concerns of fire safety authorities with the use of composites in high fire-risk applications, and for this reason there has been a considerable effort over many years to characterize the flame spread properties of FRP materials.

The flame spread of composite materials can be determined using several experimental techniques. The most common technique is the radiant panel flame spread test, which basically involves exposing a flat composite panel inclined at an angle of 45° to a radiant heater operated at a constant heat flux. The heater is placed at the higher end of the panel to force the composite to ignite at the upper edge. The speed at which the flame front travels down the inclined specimen is measured during a test. In this respect, the radiant panel flame spread test is unrealistic because the flame front is required to travel downwards, whereas in actual fires it is the faster upward movement of flames that is responsible for the spread of fire. Despite this, it is a standard test for determining the flame spread properties of composites and other combustible materials.

Most investigations into the flame spread rates of composites have been performed on polyester-, epoxy- and phenolic-based laminates due to their use in aerospace, marine, offshore and railway applications [3.7],[3.10],[3.13],[3.16],[3.22],[3.40],[3.42],[3.56]. Typical downward flame spread speeds of these composites are shown in Figure 3.21. This figure shows the time taken for the flame front to travel down the composite from the ignition point on the specimen panel in the flame spread test. It is
seen that the flame propagates readily down the surface of glass/polyester and glass/epoxy laminates, and this is due to the high flammability of these materials. However, the flame is unable to spread down the glass/phenolic laminate, and this material can be regarded as self-extinguishing. Several studies have reported that phenolic laminates have excellent flame spread resistance, and this is another outstanding fire reaction property of these materials that makes them suited for many high fire risk applications [3.7],[3.10],[3.13],[3.22],[3.40],[3.42],[3.56]. In addition to phenolic resins, Sorathia et al. [3.40] have shown that composites with a bismaleimide, polyimide or high-temperature thermoplastic (e.g. PEEK, PPS) matrix display excellent flame spread resistance. The combustion behavior of the fiber reinforcement can also have a major impact on flame spread.

The greatest influence on the downward flame spread rate of composite materials is their heat release rate. Sorathia et al. [3.4] have shown the flame spread rate of both thermoset and thermoplastic matrix composites is highly dependent on their peak heat release rate, as shown in Figure 3.22. In this figure the peak heat release rates of the composites have been normalised to their ignition times. The flame spread index is a parameter often used to quantify the downward flame spread rate, and the higher the index value the faster is the average flame spread speed. The figure shows a clear correlation between the flame spread index and the peak heat release rate parameter.

Figure 3.21 Surface spread of flame verses time [3.10].
Several models have been proposed for calculating the upward flame spread speed in composite laminates, including models by Cleary and Quintiere [3.57], and Brehob and Kulkarni [3.58],[3.59]. The accuracy of these models in the prediction of the upward flame spread rate for thermoset and thermoplastic composites has not been rigorously assessed [3.16].

### 3.7 Mass Loss.

Mass loss is another important fire property because it gives a quantitative measure of the amount of materials that will decompose in fire. The amount and rate of decomposition of the organic constituents of a composite material can be determined over the event of a fire by measuring the weight change of a composite sample using test instruments such as the thermogravimetric analyzer, TGA or the cone calorimeter. The mass loss of a wide variety of composite materials has been determined under different fire conditions [3.6],[3.12],[3.60], and two examples of the mass loss behavior of composites are given in Figure 3.23. A typical weight change over time for glass/vinyl ester and glass/phenolic laminates exposed to a constant heat flux of 50 kW/m² is presented. The mass loss curves show four distinct regions that are identified as stages I to IV, and each stage represents a different event in the fire response of the material. Stage I represents the short delay period when the composite shows no change in weight when first exposed to fire. The delay occurs because the composite has not reached the decomposition temperature of the polymer matrix.
Stage II is characterized by a rapid loss in mass with increasing time due to endothermic decomposition of the matrix. During stage III a change in the mass loss rate of the composite occurs because most of the polymer matrix has degraded and only a small region of virgin material remains unaffected by the fire. In this stage the composite behaves as a thermally thin material, and this usually accelerates the mass loss rate. In stage IV the mass loss curve reaches a constant minimum value because the polymer matrix has been consumed, and this represents the final mass of the degraded laminate (ie. mass of the fiber reinforcement and residual char).

The total mass loss of a composite is determined largely by the char yield of the organic constituents. Composites that yield only a small amount of char (eg. polyesters, epoxies) experience a greater mass loss than materials with a high char yield (eg. phenolics, phthalonitriles, thermoplastics). The mass loss is also higher when combustible fibers (eg. aramid) are used in the composite because the reinforcement decomposes along with the resin matrix. It has been found that even the organic sizing and binding agents on the fibers can affect the mass loss, with chopped fiber composites that contain a high amount of fiber binding agent experiencing a greater mass loss than composites that do not contain a binding agent [3.12].

![Figure 3.23 Typical mass loss curves for glass/vinyl ester and glass/phenolic laminates.](image)

Mass loss is one of the few fire reaction properties that can be calculated by thermochemical modelling. Gibson et al. [3.61] and Sikoutris et al. [3.62] recently showed that the mass loss of laminates with a polyester, vinyl ester or epoxy matrix
can be modelled. For example, Figure 3.24 compares the measured mass loss curve for a fiberglass/polyester composite against the theoretical curve determined using a thermal model [3.63]. The mass loss behavior of composites containing combustible fibers cannot be accurately predicted, although research into this problem is in progress.

![Figure 3.24 Measured and calculated mass loss curves for a glass/polyester laminate [3.1].](image)

Figure 3.23 shows that the mass loss rate of a burning composite is not constant. This is because the mass loss is controlled by several thermal processes that change with time, including ignition, combustion, char formation, and the transition from thermally thick to thermally thin behavior as burn-through occurs. Therefore, it is important to characterize the change in mass loss rate of composites with increasing exposure time to fire. The change in mass loss rate of glass/vinyl ester and glass/phenolic composites with time is shown in Figure 3.25. It is seen the mass loss rate varies considerably as the thermal response of these materials change with time, particularly the more volatile vinyl ester-based material. The curve for the glass/vinyl ester laminate shows an initial spike in mass loss rate due to the rapid decomposition of the resin-rich surface layer. Following this initial peak the mass loss rate decreases steadily with time, as the decomposition reaction rate slows due to the increasing thermal insulation provided by the growth of a char surface layer. After this, the mass loss rate increases again with time, and this rise is due to an increase in the decomposition reaction rate as the composite becomes thermally thin. Finally, the mass loss rate decreases with time as the last of the polymer matrix is degraded. These
events also occur with the glass/phenolic shown in Figure 3.25, although they are not as obvious due to the slower mass loss rate of this material.

![Mass loss rate curves for glass/vinyl ester and glass/phenolic laminates](image)

Figure 3.25 Mass loss rate curves for glass/vinyl ester and glass/phenolic laminates [3.1].

### 3.8 Smoke Density.

One of the main safety concerns with polymer composites is the generation of dense smoke in a fire. The smoke produced by a burning composite is a mix of small fragments of fiber and ultra-fine carbon (soot) particles. The short-term exposure of people to smoke released from a burning composite is usually not considered a serious health hazard. However, the smoke can be extremely dense and thereby reduce visibility, cause disorientation and make it difficult to fight the fire. For these safety reasons, the smoke properties of many composite materials have been characterized.

Many fire studies report that the smoke produced by highly flammable polymer composites (e.g., polyesters, vinyl esters, epoxies) is much more dense than smoke from phenolic laminates [3.7]-[3.10],[3.12],[3.13],[3.28],[3.29],[3.40],[3.64]. Figure 3.26 compares the Specific Extinction Area (SEA) for glass/vinyl ester and glass/phenolic composites when exposed to an identical fire. The SEA is a measure of how effectively a given mass of flammable volatiles released by a combustible material is converted into smoke, and is often used to quantitatively define the smoke density. The SEA of the vinyl ester laminate increases rapidly at the onset of combustion, and then remains high until the polymer matrix is completely consumed.
after about 500 seconds. The phenolic composite produces much less smoke, which is one of the main reasons for their common use in high fire-risk applications.

In addition to phenolic resins, other highly aromatic thermoset polymers and many types of high temperature thermoplastics have low smoke emission. Examples include aromatic thermosets such as polyimides, cyanate esters and phthalonitriles as well as thermoplastics such as PPS, PES and PEEK \cite{3.8,3.17,3.20,3.28,3.29,3.40,3.65}. Figure 3.27 shows the maximum smoke density values for various carbon fiber composites, and the smoke released by the thermoplastic materials is generally very low. Price et al. \cite{3.66} report that polymers with aliphatic backbones, or those that are largely aliphatic and oxygenated, have a tendency to yield low levels of smoke, while polyenic polymers and those with pendant aromatic groups generally produce more smoke. Polymers with high thermal stability or which form small amounts of flammable pyrolyzates generally produce little visible smoke. Increasing char formation is one way of minimising the yield of pyrolyzates and hence smoke production. For this reason, composites containing resins that yield a high amount of char, such as phenolics, PEEK and PES, generate less smoke.

![Figure 3.26 Smoke generation (specific extinction area, SEA) verses time for glass/vinyl ester and glass/phenolic composites tested at the heat flux of 50 kW/m² \cite{3.1}](image)

The production of char can also reduce the smoke density by impeding the release of ultra-small fragments of fiber into the smoke. The continuous char structure formed in high char yield composites is effective in eliminating the environmentally hazardous release of fibers into a smoke plume \cite{3.67,3.68}. For example, Gilwee \cite{3.67} compared the amount of fibers released from a carbon/polystyrylpyridine
composite - that yields a high amount of char (~68%) – against a conventional carbon/epoxy laminate – that has a low char yield (~15%). It was found that under impact loading, the charred carbon/polystyrylpyridine composite released less than 0.2% of its fibers whereas the charred carbon/epoxy lost between 1.2-1.4%. The strong, continuous char in the polystyrylpyridine composite restrained any loose fragments of carbon fibers, whereas the open, discontinuous char of the epoxy laminate allowed fiber fragments to escape more easily.

The fiber reinforcement can also influence the amount of smoke released from a burning composite. Increasing the fiber content lowers the maximum and total amounts of smoke because less organic material is available to produce smoke [3.15], [3.21], [3.44]. Combustible fibers, such as aramid and polyethylene, and the organic sizing and binder agents used on glass and carbon fibers will however increase the smoke density [3.12], [3.22], [3.42].

![Figure 3.27](image)

Figure 3.27 Comparison of the maximum smoke density produced by various thermoset and thermoplastic carbon fiber composites [3.8].

The density of smoke released from a burning composite is also controlled by the intensity of the fire. The smoke density usually increases slightly with the external heat flux due to an increase in the release rate of soot particles [3.10], [3.12], [3.22], [3.28]. For example, Figure 3.28 shows that the smoke density of fiberglass laminates increase with the incident heat flux. However, in some highly aromatic polymer composites the smoke density can decrease with increasing heat flux in most cases. This behavior is attributed to the high char yield of these composites, which
inhibits the transport of volatiles and soot particles to the surface and thereby slows
the smoke production rate [3.9].

It is evident from the information presented above that the smoke released from a
burning composite is dependent on a variety of factors, including the amount and type
of resin and fiber reinforcement together with the heat flux of the fire. It also appears
that smoke production is related to the heat release rate properties of the composite
material [3.5]. Figure 3.29 shows the correlation between average heat release rate
and smoke extinction area (SEA) for a variety of thermoset and thermoplastic
composites. The smoke data was determined for composites tested at various heat flux
levels between 25 and 100 kW/m². It can be seen that a strong correlation exists
between the SEA and average heat release rate. It is assumed that this correlation
exists because the endothermic decomposition reaction rate of the organic matrix
determines both the heat release rate and smoke density. An increase in the reaction
rate results in an increase to both the heat release rate and smoke density, and for this
reason a strong correlation exists between these two fire reaction properties.

Figure 3.28 Effect of incident heat flux on the smoke yield of various fiberglass laminates [3.10].
3.9 *Smoke Toxicity.*

While the most important fire reaction property is heat release rate, it is often the toxic gases released during combustion that pose the greater health hazard. It is well recognised that the main cause of death in fires is the toxicity of combustion products, and the gas that generally has the greatest individual hazard is carbon monoxide. The amount of CO produced by a burning composite depends on the composition of the organic constituents, the temperature of the fire, and oxygen availability, but even very low levels of CO can cause incapacitation or death [3.56]. Death in humans will occur within one hour when the CO concentration in air reaches about 1500 ppm. In comparison, the CO₂ content must exceed 100000 ppm for death to occur within the same time. In combination with CO, a variety of other gases can be produced during combustion of composite materials [3.7],[3.8],[3.17],[3.18],[3.26]. The type of gases is determined by the composition of the organic constituents. For example, polyester laminates release CO, CO₂, low molecular weight organic volatiles such as propylene, benzene, toluene and styrene, and higher molecular weight ring compounds including aromatic C-H and aromatic C-H-O [3.69]. As another example, phenolic laminates produce CO, CO₂, toluene, methane, acetone, propanol, propane, benzene, benzaldehyde and volatile aromatic compounds [3.26],[3.33]. Corrosive and toxic gases can also be released, including HCl, HCN and aromatic halogenated species [3.8],[3.26].
Table 3.1 lists the concentration of combustion gases measured by Sorathia et al. [3.40] and Sastri et al. [3.17] for various thermoset and thermoplastic laminates. The gases were measured by analysing a sample of combustion products in a Drager calorimeter tube. It is seen the CO level is substantially higher for the thermoset laminates, and even aromatic resins such as phenolic and polyimide that have excellent flammability resistance still produce a high amount of CO. Certain types of advanced thermoset resins, such as phthalonitrile, cyanate ester and phenolic/siloxane, and thermoplastics yield small amounts of CO and CO₂ in a fire due to their exceptionally high char yield that retains most of the carbon within the composite [3.17],[3.18]. The concentration of gases is also dependent on the mode of combustion. Hunter and Forsdyke [3.64] found that a phenolic composite that was smouldering when exposed to fire released about 50 ppm CO and 300 ppm CO₂. However, when the same material burnt in a flaming mode the gas concentrations increased to 100 ppm CO and 5000 ppm CO₂. The quantity of gas also tends to vary over the course of a fire, with the CO production usually increasing in the later stages of the combustion process when the polymer matrix is extensively carbonised.

Organic fibers also generate toxic gases in a fire [3.41],[3.42]. For example, Sorathia et al. [3.42] measured the gases released by various types of phenolic composites reinforced with organic or non-combustible fibers. The composites reinforced with aramid or Spectra fibers released a higher concentration of CO (700 ppm) compared to materials containing glass (190-330 ppm) or carbon (500 ppm) fibers. A similar amount of CO₂ was released by the different composites, and therefore it appears that combustible fibers only have a significant influence on the production of CO. It is worth noting that fragments of damaged fibers can also be

<table>
<thead>
<tr>
<th>COMPOSITE</th>
<th>CO (ppm)</th>
<th>CO₂ (vol%)</th>
<th>HCN (ppm)</th>
<th>HCl (ppm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Glass/vinyl ester</td>
<td>230</td>
<td>0.3</td>
<td>not detected</td>
<td>not detected</td>
</tr>
<tr>
<td>Glass/epoxy</td>
<td>283</td>
<td>1.5</td>
<td>5</td>
<td>not detected</td>
</tr>
<tr>
<td>Glass/BMI</td>
<td>300</td>
<td>0.1</td>
<td>7</td>
<td>trace</td>
</tr>
<tr>
<td>Glass/phenolic</td>
<td>300</td>
<td>1.0</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td>Glass/polyimide</td>
<td>200</td>
<td>1.0</td>
<td>Trace</td>
<td>2</td>
</tr>
<tr>
<td>Glass/PPS</td>
<td>70</td>
<td>0.5</td>
<td>2</td>
<td>0.5</td>
</tr>
<tr>
<td>Glass/phthalonitrile</td>
<td>40</td>
<td>---</td>
<td>---</td>
<td>---</td>
</tr>
<tr>
<td>Carbon/PEEK</td>
<td>trace</td>
<td>trace</td>
<td>not detected</td>
<td>not detected</td>
</tr>
</tbody>
</table>

Table 3.1  Combustion gases released by burning composite materials [3.17],[3.40].
released by burning composites, and while these are not toxic at low concentrations they can cause irritation to the respiratory tract and affect breathing [3.70],[3.71].

The amount of CO produced by a composite is influenced by the heat release rate properties [3.5]. Figure 3.30 shows a plot of average production of CO against average heat release rate for various types of glass and carbon fiber laminates. A linear correlation exists between the CO level and heat release rate, which suggests that the toxic hazard caused by the release of CO can be minimized by using composite materials that have low heat release rate properties. It also shows the effect of average heat release rate on CO₂ production, although in this case there is no clear correlation.

![Graph showing the correlation between CO and heat release rate](image)

Figure 3.30 Effect of average heat release rate on the yields of (a) carbon monoxide and (b) carbon dioxide from burning composite materials [3.5].

### 3.10 References


Composite Materials Fire Response Characteristics.


Composite Materials Fire Response Characteristics.


Polymer materials when exposed to high temperatures tend to decompose. This behaviour is also observed in polymer composites, where fiber reinforcement is present inside the polymer matrix. Decomposition of the composite material is a complex phenomenon sensitive on many parameters heating rate, atmosphere temperature, etc. During the heating of the composite material in the presence of oxygen (air), the polymer matrix decomposes and char is formed, further exposure to the heat source will force the fiber reinforcement to react and either decompose or oxidize. Furthermore, the formed char may also start to react (oxidize) complicating the phenomena. If the environment is inert then the polymer would decompose through pyrolysis. The macromolecules will break up into smaller chains and further heating would lead to complete pyrolysis and the formation of carbonaceous char residue.
4.1 Single-stage Kinetics.

The simplest way for a polymer to decompose is by employing a single-stage reaction, that would require single-stage kinetics. Resin decomposition can be described by the Arrhenius law and the theory of chemical reaction rate \[4.1\],[4.2],[4.3],[4.4],[4.5],[4.6],[4.7],[4.8]. Assuming the decomposition process as a one-stage reaction, the decomposition rate \( \alpha \) can be expressed as a function of the temperature \( T \), and the quantity of the reactants, using the following expression \[4.8],[4.9\].

\[
\frac{d\alpha}{dt} = K(T) \cdot F(\alpha)
\] (4.1)

where \( \alpha \) is the decomposition degree, expressed as \( \alpha = (M_i - M)/(M_i - M_f) \), \( M_i \) is the initial mass, \( M_f \) is the final mass after decomposition and \( M \) is the mass at every time instance, \( d\alpha/dt \) is the mass-loss rate (e.g. decomposition rate), \( K(T) \) and \( F(\alpha) \) describe the effect of temperature and reactant quantities respectively on the reaction rate. The \( F(\alpha) \) function can be expressed as according to \[4.8],[4.9\] as:

\[
F(\alpha) = (1 - \alpha)^n
\] (4.2)

while the \( K(T) \) function is obtained by the Arrhenius equation \[4.8],[4.9\]:

\[
K(T) = A \cdot EXP\left(\frac{-E_A}{R \cdot T}\right)
\] (4.3)

where \( n \) is the reaction order, \( A \) is the Arrhenius pre-exponential factor (frequency factor), \( E_A \) is the activation energy and \( R \) is the universal gas constant (\( R=8.314\)J/molK). The three parameters \( A, E_A, n \) are called reaction kinetic parameters. These resin decomposition kinetic parameters can be extracted using TGA curves. Several methodologies have been proposed for processing TGA-curves, the Friedman method \[4.1\], the Kissinger method \[4.2\] the Ozawa method \[4.4\] and the modified Coats-Redfern method \[4.6],[4.7\]. The first three (3) methods are so-
called multi-curve whilst the later one is a single curve method, depending on the required TGA-curves.

4.1.1 Friedman method.

Since this methodology [4.1], and the others described further on, were developed for processing TGA data, it has to be mentioned here that during all the TGA tests a constant heating rate should be applied.

\[
\frac{da}{dT} = \beta \tag{4.4}
\]

Combing Equations (4.1-4.4) it is extracted that:

\[
\frac{da}{dT} = \frac{A}{\beta} \exp\left(\frac{-E_A}{RT}\right)(1-a)^n \tag{4.5}
\]

By taking the logarithm of each side of Eq.(4.5), the following relationship can be found:

\[
\ln\left(\beta \frac{da}{dT}\right) = \ln(A) + n \cdot \ln(1-a) - \frac{E_A}{RT} = k_1 + k_2T^{-1} \tag{4.6}
\]

For a specific value of \(a\), the first two terms are constant, and if \(A\), \(E_A\) and \(n\) are thought to be independent of the heating rate \(\beta\), the plot of the left side versus \(T^{-1}\) gives a straight line, as shown in Figure 4.1. \(E_A\) can be obtained from the slope of this straight line. Additionally, \(n\) and \(A\) can be calculated by plotting \(E_A/RT_0\) against \(\ln(1-a)\), where \(T_0\) is the temperature where \(\ln\left(\beta \frac{da}{dT}\right) = 0\).
4.1.2 Kissinger method.

When the maximum reaction rate \(4.2\) occurs at temperature \(T_m\), \(d^2a/dT^2=0\), the derivative of Eq.(4.5) gives:

\[
\frac{E_A\beta}{RT_m^2} = An(1-a)^{n-1} \exp \left( \frac{-E_A}{RT_m} \right) \tag{4.7}
\]

Equation (4.8) is obtained by taking the logarithm of Eq.(4.7) and then deriving with respect to \(1/T_m\).

\[
\frac{d \ln \left( \frac{\beta}{T_m^2} \right)}{d \left( \frac{1}{T_m} \right)} = -\frac{E_A}{R} \tag{4.8}
\]
As a result a plot of $\ln\left(\frac{\beta}{T_m^2}\right)$ versus $1/T_m$ results in a slope of $E_A/R$.

The reaction order, $n$, can be determined from Eq.(4.9) assuming that $n \neq 1$.

$$-n(1-a_m)^{n-1} = 1 + (n-1)\frac{RT_m}{E_A}$$  \hspace{1cm} (4.9)

Where $a_m$ is the decomposition degree at temperature $T_m$. The frequency factor $A$ can be determined by substituting $n$ and $E_A$ into Eq.(4.7).

### 4.1.3 Ozawa method.

Integrating Eq.(4.5) gives [4.4]:

$$g(a) = \int_0^a \frac{da}{(1-a)^n} = \frac{AE_A}{R\beta} \cdot p(x)$$  \hspace{1cm} (4.10)

Where $p(x) = -\int_x^\infty e^{-x} dx$ and $x = E_A/RT$. By taking the logarithm of Eq.(4.10), it is obtained:

$$\log g(a) = \log \left(\frac{AE_A}{R}\right) - \log \beta + \log p(x)$$  \hspace{1cm} (4.11)
While \( \log p(x) \) can be approximated by Eq.(4.12):

\[
\log p(x) \approx -2.315 - 0.4567x, \quad \text{for } 20 < x < 60
\]  

(4.12)

Equation (4.13) is then expressed as:

\[
\log g(a) = \log \left( \frac{AE_A}{R} \right) - \log \beta - 2.315 - 0.4567 \frac{E_A}{RT}
\]  

(4.13)

Deriving Eq.(4.13) with respect to \( 1/T \) at fixed decomposition degrees, Eq.(4.14) is extracted:

\[
E_A = -R \frac{d(\log \beta)}{0.4567 d(1/T)}
\]  

(4.14)

\( E_A \) can be calculated from the slopes of the straight lines by plotting \( \log \beta \) versus \( 1/T \).

![Typical \( E_A \) calculation for the Ozawa method [4.4],[4.9].](image)

The main value of the frequency factor \( A \) at each heating rate can be calculated by Eq.(4.15):

\[
\log A = \log \beta + \log E_A + 0.434 \frac{E_A}{RT} - \log R - 2 \log T
\]  

(4.15)
Decomposition Kinetics.

After obtaining the values of $A$ and $E_A$, $n$ can be determined by substituting Eq.(4.16) into Eq.(4.17):

$$g(a) \approx \frac{1 - (1 - a)^{1-n}}{1 - n}, \quad n \neq 1$$  \hspace{1cm} (4.16)

$$\log g(a) = \log \left( \frac{AE_a}{R} \right) - \log \beta^* - 2.315$$  \hspace{1cm} (4.17)

Where, $\log \beta^*$ is the y-intercept, the value of $\log \beta$ when $E_a/RT$ is taken as zero in Eq.(4.13), of the lines in Figure 4.4.

4.1.4 Modified Coats-Redfern method.

The “multi-curves” methods presented so far require TGA curves of different heating rates, to tackle this, Coats and Redfern [4.6],[4.7] proposed a method to determine $E_A$ in order to obtain kinetic parameters from only one curve. As introduced in the Coats-Redfern method [4.6],[4.7], the right side of Eq.(4.10) can be expressed as:

$$\left( \frac{ART^2}{\beta E_A} \right) \cdot \left( 1 - \frac{2RT}{E_A} \right) \cdot \exp \left( -\frac{E_A}{RT} \right)$$  \hspace{1cm} (4.18)

Whereas the left hand side can be expanded to:

$$a + n \frac{a^2}{2} + n(n+1)\frac{a^3}{6} + \ldots$$  \hspace{1cm} (4.19)

For the case of low values of $a$, terms $a^2$ and higher can be neglected giving:

$$a \approx \left( \frac{ART^2}{\beta E_A} \right) \cdot \left( 1 - \frac{2RT}{E_A} \right) \cdot \exp \left( -\frac{E_A}{RT} \right)$$  \hspace{1cm} (4.20)

By logarithm transform of Eq.(4.20) results in:
\[
\ln \left( \frac{a}{T^2} \right) = \ln \left( \frac{AR}{\beta E_A} \right) \left( 1 - \frac{2RT}{E_A} \right) \frac{E_A}{RT} \tag{4.21}
\]

Thus, a plot of \(-\ln(a/T^2)\) versus \(1/T\) should give a straight line with slope \(E_A/R\) since \(\ln(AR/\beta E_A)(1-2RT/E_A)\) is nearly constant. As a result, \(E_A\) is obtained from one curve at one constant heating rate. Substituting \(E_A\) into Eq.(4.17), the values of \(A\) at different decomposition degrees, \(a\), are obtained. Since the terms of \(a^2\) (and higher which are related to \(n\) in Eq.(4.19)) are neglected in the Coats-Redfern method [4.6],[4.7], the value of \(n\) cannot be directly calculated on this approach. Considering that only one curve is available, reference to Eq.(4.7) of the Kissinger method [4.2] can be made. Substituting the values of \(E_A\) and \(A\) into Eq.(4.7), the value of \(n\) at different heating rates is obtained.

![Figure 4.5 Typical \(E_A\) calculation for the Coats-Redfern method [4.6],[4.7],[4.9].](image)

In the current work the Friedman [4.1] method has been used to estimate the polymer resin decomposition kinetic parameters \(A, E_A, n\). As mentioned earlier the Friedman method is a multi-curve method, so four heating rates (2, 5, 10 and 20\(^{0}\)C/min) were used for the TGA tests, the results for the inert atmosphere TGA tests are presented in Table 4.1.

<table>
<thead>
<tr>
<th>Activation energy, (E_A)</th>
<th>66978.12 J/mol</th>
</tr>
</thead>
<tbody>
<tr>
<td>Frequency factor, (A)</td>
<td>7223.32 sec(^{-1})</td>
</tr>
<tr>
<td>Reaction order, (n)</td>
<td>2.76</td>
</tr>
</tbody>
</table>

Table 4.1 Single-stage kinetic parameters.
4.2 Multi-stage Kinetics

4.2.1 Background

The decomposition process can be considered as a series of sequential reactions. The reaction rate can be defined as the derivative of the conversion degree. During thermogravimetric measurements, conversion degree may be defined as the ratio of actual weight loss to total weight loss \([4.10],[4.11]\) given by Eq.(4.22):

\[
a = \frac{M_0 - M_d}{M_0 - M_f}
\]  

(4.22)

where \(M_0, M_f, M_d\) are the initial, final, and “dynamic” weight of the sample, respectively. The conversion rate in a TGA test under a constant heating rate can be expressed using the following relation:

\[
\frac{da}{dt} = \beta \cdot \frac{da}{dT} = k(T) \cdot f(\alpha)
\]  

(4.23)

where, \(\beta\) is the heating rate, \(k(T)\) is the rate constant and \(f(\alpha)\) is the conversion function. For polymers it may be assumed that the conversion rate is directly related to the non-reacted material \([4.10],[4.11]\) (undegraded/unreacted material), and it is given by Eq.(4.24),

\[
f(\alpha) = (1-\alpha)^n
\]  

(4.24)

The rate constant \(k(T)\) is temperature dependent and can be described by the Arrhenius expression, of Eq.(4.25),

\[
k(T) = A \cdot \exp(-E / RT)
\]  

(4.25)

where, \(A\) is the Arrhenius pre-exponential factor (frequency factor), \(E\) is the activation energy and \(R\) is the gas constant. Combining Eq.(4.22)-(4.25), the following expression is derived:
\[
\frac{da}{dt} = \beta \cdot \frac{da}{dT} = A \cdot (1-a)^{g} \cdot \exp \left( -\frac{E}{RT} \right)
\]

(4.26)

Integrating with respect to conversion degree and temperature, the following integrated form is obtained,

\[
g(a) = \int_{0}^{a} \frac{da}{(1-a)^{g}} = \frac{AE}{\beta R} \cdot \int_{\infty}^{x} \frac{\exp(-x)}{x^{2}} \, dx = \frac{AE}{\beta R} \cdot p(x)
\]

(4.27)

where,

\[
p(x) = \int_{\infty}^{x} \frac{\exp(-x)}{x^{2}} \, dx \quad \text{and} \quad x = \frac{E}{RT}.
\]

Several techniques using different approaches have been proposed for solving the integral equation for \( p(x) \). Among them Kissinger[4.2], Coats-Redfern[4.6],[4.7], van Krevelen et al.[4.12] and others. In the current work the van Krevelen et al. methodology was used. Van Krevelen et al.[4.12], provided an approximation of the integral \( p(x) \) and based on this, Eq.(4.27) concludes to the following expression,

\[
\log g(a) = \log B + \left[ \frac{E}{RT_{m}} + 1 \right] \cdot \log T
\]

(4.28)

where,

\[
B = \frac{A}{\beta} \left[ \frac{E}{RT_{m}} + 1 \right]^{-1} \cdot \frac{0.368}{T_{m}} \cdot \frac{E}{RT_{m}}
\]

(4.29)

For the expression given in Eq.(4.28), a plot of \( \log g(a) \) as a function of \( \log T \), should give a straight line with slope determining the activation energy \( E \). Using the slope and the intercept of the fitted line the pre-exponential (frequency factor) \( A \) can be calculated.

### 4.2.2 Materials

The materials used in the current work were a neat epoxy system and its variants modified using some additives. The system used was the Huntsman Araldite LY564-
Decomposition Kinetics.

Aradur 2654, which is a typical aerospace grade epoxy system. This system would be referred from now on as **LY-Ref**. Two modified batches of this reference system were produced. As additives were used the Exolit Ammonium Polyphosphate AP423 and Baytubes Multiwall Carbon Nanotubes. The first batch contained 20% w/w AP423 fine powder, and the second batch contained 20% w/w AP423 and 2% w/w MWCNT. These two batches would be referred as **AP423** and **MWCNT** respectively. Resin only samples and fiber reinforced ones were manufactured using the hand-lay-up technique and oven cured according to resin manufacturer datasheet.

### 4.2.3 TGA tests

Since the environment on the fire impinging surface of the samples is non-oxidative due to oxygen depletion, it was decided the TGA tests should be performed under inert atmosphere. Fiber oxidation is not present because of the non-oxidative conditions so TGA tests were performed only in resin samples.

Samples weighing 31mg from each one of these resin batches were cut and placed inside the **DTG-60** Thermogravimetry instrument. The heating rates used were 10, 20, 30 and 50 K per minute. Three samples per heating rate, per batch were tested and the mass loss curves are the average of the three. The environment conditions inside the TGA apparatus were inert using nitrogen gas at the flow rate of 30ml/min. In Figure 4.6 the comparative presentation of the three matrix materials is shown for one of the heating rates investigated.

![Figure 4.6](image)

**Figure 4.6** Mass loss versus Temperature for the three materials tested at 30K/min.
4.2.4 Thermogravimetry Processing

The complex nature of the polymer matrix decomposition can not be realistically described by a single step reaction process [4.11], but a more complex approach involving multi-stage reactions should be incorporated. A first approach in determining the number of steps in this multi-stage approximation is to observe the \( \frac{da}{dT} \) versus \( T \) plots, where \( a \) is the conversion degree. Since reactions are temperature triggered, peaks observed in these derivative plots would indicate maximum reaction points [4.2]. Another approach would be to quantitively analyze and determine the exact intermediate products and reactions involved during the decomposition process. This approach requires complex mass spectroscopy analyses for every product increasing testing costs when several polymer configurations need to be analyzed.

Considering the requirements of these two approaches the authors of the current work decided to select the simplified multi-stage approach to develop their progressive degradation material model, implement it in a FE code and then compare the results against lab-scale tests evaluating its performance. In Figures 4.7, 4.8, and 4.9 the plots of \( \frac{da}{dT} \) versus temperature for the three resin systems are presented. In every plot three distinctive peaks can be observed and according to the adopted approach the materials are considered to undergo a 3-step decomposition process.

![Reaction rate derivative curve versus Temperature for the LY-Ref material at 30K/min heating rate.](image)

Figure 4.7 Reaction rate derivative curve versus Temperature for the LY-Ref material at 30K/min heating rate.
Decomposition Kinetics.

The extraction of the activation energy $E$ and the frequency factor $A$ at each step was performed following the van Krevelen et al.\[4.12\] methodology presented earlier. In order to calculate the reaction order $n$, the expression of the Kissinger methodology \[4.2\],\[4.10\] was used for every maximum-rate temperature $T_m$ Eq.(4.30), and was numerically solved using the Mathematica commercial calculation package. Table 4.2 shows the results of the kinetic analysis and the kinetic triplets.

$$\frac{\beta E}{RT_m^n} = A \cdot n \cdot (1 - a_w)^n \cdot \exp \left( - \frac{E}{RT_m} \right)$$

(4.30)

<table>
<thead>
<tr>
<th></th>
<th>Step 1</th>
<th>Step 2</th>
<th>Step 3</th>
</tr>
</thead>
<tbody>
<tr>
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<td>$E$ (J/mol)</td>
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<td>607472</td>
<td>36236</td>
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<td>$A$ (min$^{-1}$)</td>
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<td>46431</td>
<td>11372</td>
</tr>
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<td>12412</td>
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<td>$n$</td>
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<td>1.938</td>
<td>9.825</td>
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<td>$n$</td>
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<td>1.978</td>
<td>14.524</td>
</tr>
</tbody>
</table>

Table 4.2: Kinetic parameters for the materials under investigation.

![Reaction rate derivative curve versus Temperature](image)

Figure 4.8 Reaction rate derivative curve versus Temperature for the AP423 material at 30K/min.

### 4.2.5 3-step decomposition

The simplified multi-stage approach mentioned earlier will be described in detail for every resin system investigated in this work. During this analysis a three-step decomposition process is assumed.
4.2.5.A LY-Ref

This is the reference resin system used as control for the modification and fire performance improvements of the additives used. As it is observed in Figure 4.7, three distinctive peaks at temperatures $T_1=645K$, $T_2=656K$ and $T_3=695K$ indicate the presence of three decomposition stages. At first only the “virgin” polymer material (referred as A) is present, as temperature rises it converts to an intermediate leathery state (referred as B). Further heating, results in breaking up to a mixture of lower molecular weight organic compounds (referred as C), those compounds in the presence of heat finally decompose to form a carbonaceous char residue (referred as D) and the production of decomposition gases (referred as E). In Figure 4.10a the described network is presented and the rate equations in matrix notation for this network is expressed by Eq.(4.31).

![Figure 4.9](image)

Figure 4.9 Reaction rate derivative curve versus Temperature for the MWCNT material at 30K/min.

4.2.5.B AP423

The first modified batch contained 20% w/w Exolit AP423. This additive is a fine particle ammonium polyphosphate produced by a special method. It is largely insoluble in water and is completely insoluble in organic solvents. Has the form of a white powder, non-hygroscopic and non-flammable. The AP423 additive was added in the LY-Ref resin and stirred in the Dispermat AE,VMA, Getzmann GmbH dissolver for 60 minutes constantly monitoring temperature and RPM. Similarly to the LY-Ref system the AP423 batch present three peaks in the derivative mass loss curve, Figure 4.8, (temperatures $T_1=599K$, $T_2=637K$, $T_3=675K$ respectively). AP423 powder
modifies the reaction network of the \textbf{LY-Ref}, at first the virgin resin/AP423 mixture (element-A) converts to an intermediate state (element-B) due to temperature rise. Further heating results in the decomposition of \textit{element-B} to a mixture of lower molecular weight organic compounds and phosphate esters (element-C) and carbonaceous char residue (element-D). If heating is continued then the \textit{element-C} mixture would completely pyrolize and char residue (element-D), pyrolysis gases (element-E) are formed. In Figure 4.10b the assumed reaction network is presented and in Eq.(4.32) the matrix notation of the network rate equations is described.

\textbf{4.2.5.C MWCNT}

This is the second modified batch, it contains 20\% w/w AP423 ammonium polyphosphate, like the previous batch, but also 2\% w/w Multi-Wall Carbon Nanotubes. The addition of MWCNT’s into the \textbf{AP423} batch was performed in an effort to promote carbonaceous char formation in earlier stages. Likewise to the previous resin systems, the mass loss derivative curve, Figure 4.9, presents three peaks at temperatures \(T_1=609\,\text{K}, T_2=625\,\text{K}, T_3=647\,\text{K}\) respectively.

The network is slightly modified compared to the \textbf{AP423} one. The only modification refers to the char formation (element-D) during the intermediate state (formation of element-B), the rest of the network remains the same as that of \textbf{AP423}. Schematically the \textbf{MWCNT} network is presented in Figure 4.10c and the rate equations are those of Eq.(4.33).
4.2.6 TGA reconstruction

So far the multi-stage decomposition approach and its simplifications have been presented, the next part of this work is to apply these considerations to the reconstruction of the TGA curves. Accurate TGA reconstruction would verify that the 3-stage simplified approach is acceptable. A finite difference numerical scheme was developed to solve the rate equations system of Eq.(4.31),(4.32),(4.33). The outcome of this scheme was the molar fractions $X_i$ of every reactant during the TGA test. Concerning the Eq.(4.31),(4.32),(4.33), $\{r\}$ and $\{X\}$ are vectors with the $r_i$ and $X_i$ as the reaction rate and molar fractions of the $i=$A, B, C, D, E elements. The rate coefficients $k_{1,2,3}$ are expressed in the Arrhenius equation form of Eq.(4.25) for every reaction step.

$$\{r\} = \begin{bmatrix} -k_1X_A^{n-1} & 0 & 0 & 0 & 0 \\ k_1X_A^{n-1} & -2k_2X_B^{n-1} & 0 & 0 & 0 \\ 0 & k_2X_B^{n-1} & -2k_3X_C^{n-1} & 0 & 0 \\ 0 & 0 & k_3X_C^{n-1} & 0 & 0 \\ 0 & 0 & 0 & k_3X_C^{n-1} & 0 \end{bmatrix} \cdot \{X\}$$

(4.31)

Figure 4.10 Reaction Network of: a) LY-Ref, b) AP423, c) MWCNT.
Decomposition Kinetics.

\[
\rho = \begin{bmatrix}
-k_1X_d^{n-1} & 0 & 0 & 0 \\
-k_2X_B^{n-1} & 0 & 0 & 0 \\
0 & k_2X_B^{n-1} & -2k_3X_C^{n-1} & 0 \\
0 & 0 & k_3X_C^{n-1} & 0 \\
\end{bmatrix} \cdot \{X\}
\]

(4.32)

\[
\rho = \begin{bmatrix}
-2k_1X_d^{n-1} & 0 & 0 & 0 \\
0 & -2k_2X_B^{n-1} & 0 & 0 \\
0 & k_2X_B^{n-1} & -2k_3X_C^{n-1} & 0 \\
0 & 0 & k_3X_C^{n-1} & 0 \\
\end{bmatrix} \cdot \{X\}
\]

(4.33)

Figure 4.11 TGA reconstruction of the LY-Ref material at 30K/min heating rate.

The normalized mass loss measured by TGA is the ratio of the “dynamic” mass, \(m^d\), of the specimen divided by the initial mass, \(m_\Theta\).

\[
\Delta m\% = \frac{m^d}{m_\Theta} \cdot 100\%
\]

(4.34)

The initial mass, \(m_\Theta\), the initial mass of element A and \(m^d\) is the sum of the masses of all elements (except element E). Utilizing the molar fractions and molecular weights Eq.(4.34) takes the form:
\[
\Delta m\% = \left( X_d^A + X_d^B \frac{MB_B}{MB_d} + X_d^C \frac{MB_C}{MB_d} + X_d^D \frac{MB_D}{MB_d} \right) \times 100\% 
\]

(4.35)

where \( X_d^i \) are the molar fractions calculated from the rate equations. In Table 4.3 the molecular weights considered in this work can be found. TGA reconstruction was performed for each one of the three resin systems LY-Ref, AP423, MWCNT and for every heating rate. The maximum observed error was less than 5% compared to the experimental data. In Figures 4.11, 4.12 and 4.13 reconstruction curves are presented for one heating rate.

Figure 4.12 TGA reconstruction of the AP423 material at 30K/min.

Figure 4.13 TGA reconstruction of the MWCNT material at 30K/min.
Decomposition Kinetics.

<table>
<thead>
<tr>
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<th>B</th>
<th>C</th>
<th>D</th>
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<td>195.326</td>
<td>140.161</td>
<td>12.0107</td>
<td></td>
</tr>
</tbody>
</table>

Table 4.3: Molecular weights of the assumed elements (units in gr/mol).

4.3 References


CHAPTER 5  Thermophysical Properties Change.

5.1 Single stage.

The thermophysical properties of the material are not constant but vary both with time and temperature. This variance is included through the decomposition degree, $\alpha$, dependence of the material properties. All thermophysical properties are expressed as functions of the decomposition degree, $\alpha$. In the current work a constant-volume model is considered. This means that the volume of the specimen remains constant. During decomposition part of the polymer material is lost, thus the specimen mass changes over time but its initial volume remains unchanged. Having this in mind, at every time-instance a unit-volume of the polymer material consists of three phases, polymer resin (matrix), fiber reinforcement and hot gases. These gases fill the space of the decomposed resin. In this work the hot gases were assumed to be hot air with thermophysical properties of equilibrium air at high temperatures [5.1]. With this assumption the unit-volume is shown in Figure 5.1. The factors $v_f$, $v_m$, $v_g$ are the volume fractions of fibers, resin and gases respectively.
According to the major assumption that only the resin decomposes the expression for the decomposition degree $\alpha$ obtains the form:

$$\alpha = \frac{m_m^0 - m_m^d}{m_m^0 - m_m^{END}}$$

(5.1)

where, $m_m^o$ is the initial matrix mass, $m_m^d$ is the mass of the matrix at every time instance and $m_m^{END}$ is the final mass of the matrix, which according to the assumption $m_m^{END}=0$, so:

$$m_m^d = (1 - \alpha)m_m^0$$

(5.2)

Using the volume fraction definition and Eq.(5.2) the expression for the “dynamic” matrix volume fraction $v_m^d$ is given by.

$$v_m^d = (1 - \alpha)v_m^0$$

(5.3)

Furthermore, as mentioned earlier hot gases fill the volume of the decomposed resin, so $V_g = V_m^o - V_m^d$. Writing this expression in dimensionless form and using Eq.(5.3):

$$v_g = \alpha \cdot v_m^0$$

(5.4)
where, $v_m^d$ is the initial matrix volume fraction. It is important to recall that according to the initial assumption the fiber volume fraction $v_f$ remains constant.

### 5.1.1 Thermal Conductivity

In order to calculate the non-constant through-thickness thermal conductivity of the composite material, the previously mentioned unit-volume is considered having a $Q_{IN}$ thermal flux impinging its front face. This composite unit-volume has a thermal resistivity $R_C$, the contribution of every phase would be $R_f, R_m, R_g$ for the fibers, matrix (resin) and gases respectively.

\[
R_C = v_f R_f + v_m^d R_m + v_g R_g \tag{5.5}
\]

\[
v_f + v_m^d + v_g = 1 \tag{5.6}
\]

where $v_m^d$ is the volume fraction of the remaining matrix, it is not constant but varies as matrix decomposes. Expressing Eq.(5.5) in the thermal conductivity form:

\[
\frac{1}{k_C} = \frac{v_f}{k_f} + \frac{v_m^d}{k_m} + \frac{v_g}{k_g} \tag{5.7}
\]

Combining Eq.(5.3),(5.4) the time and temperature dependent expression for the composite material thermal conductivity is concluded.

\[
\frac{1}{k_C} = \frac{v_f}{k_f} + (1 - \alpha) \frac{v_m^\theta}{k_m} + \alpha \frac{v_m^\theta}{k_g} \tag{5.8}
\]

$k_c, k_f, k_m, k_g$ are the thermal conductivity of the composite material, fibers, matrix and hot gases respectively. Similar approach for the through thickness conductivity of the composite material has been followed by other authors [5.2],[5.3], the new feature of the present work is that the properties of the hot gases are not constant but temperature dependent [5.1].
5.1.2 Specific Heat

The specific heat is a measure to quantify the internal energy of the material. It is defined as the amount of heat needed to raise the temperature of a unit mass of the material by one degree and has the mathematical form, \( E = mC_P \Delta T \), but according to the three phase approach the energy of the composite material \( E_C \) is the sum of the energies of the three phases, so \( E_C = E_f + E_m + E_g \)

\[
C_{P,C} = \frac{m_f}{m_{TOT}} C_{P,C} + \frac{m_m}{m_{TOT}} C_{P,m} + \frac{m_g}{m_{TOT}} C_{P,g}
\]  

(5.9)

using Eq.(5.4) and the volume fraction definition

\[
m_g = \frac{\rho_f}{\rho_m} \alpha \cdot m^g
\]

(5.10)

Combining Eq.(5.2),(5.9),(5.10) the non-constant composite material specific heat capacity is obtained.

\[
C_{P,C} = f_f C_{P,C} + (1 - \alpha) f_m C_{P,m} + \alpha \frac{\rho_f}{\rho_m} f_m C_{P,g}
\]

(5.11)

So far the effect of the Heat-of-Decomposition was not considered. Henderson [5.4],[5.5] was one of the first to account for it and its impact on the apparent specific heat of the decomposing polymer composite. Bai et. al [5.3] have accounted for the Heat-of-Decomposition as Henderson and other authors dictate.

The expression Bai et. al propose for the specific heat is similar in form as Eq.(5.12) of this work BUT neglects the hot gases contribution. The author of this work consider the contribution of the hot gases to be significant and temperature sensitive [5.1] so have incorporated this extra term in Eq.(5.12).

\[
C_{P,C} = f_f C_{P,C} + (1 - \alpha) f_m C_{P,m} + \alpha \frac{\rho_f}{\rho_m} f_m C_{P,g} + \frac{d\alpha}{dT} C_D
\]

(5.12)
where, \( C_{P,C}, C_{P,f}, C_{P,m}, C_{P,g} \) are the composite material, fiber, matrix and hot gases specific heat, \( f_f, f_m \) are the fiber and matrix mass fractions.

5.1.3 Density and mass fractions

Since it is a material decomposition problem (mass loss problem) mass is not conserved, but according to the constant-volume assumption, of this work, total volume is conserved. At any time instance the total mass of the composite material is the sum of the mass of every phase \( m_{TOT} = m_f + m_m + m_g \) and using Eq.(5.2),(5.10) a mass-loss expression of the total mass is extracted.

\[
m_{TOT} = m_f + (1 - \alpha)m_m^0 + \alpha \frac{\rho_g}{\rho_m} m_m^0
\]

(5.13)

Expressing Eq.(5.13) in density form the non-constant density of the composite material takes the form:

\[
\rho_{TOT} = \rho_f \phi_f + \rho_m \phi_m + \alpha \rho_g \phi_m \left( 1 - \frac{\rho_g}{\rho_m} \right)
\]

(5.14)

Using the expression of Eq.(5.13) for the total mass, the expression of the “dynamic” total mass of the composite material is expressed as a function of the initial fiber and matrix fractions \( f_{f,0}, f_{m,0} \) and the initial total mass of the composite material \( m_{TOT,0} \):

\[
m_{TOT} = \left[ f_{f,0} + \left( 1 - \alpha + \alpha \frac{\rho_g}{\rho_m} \right) f_{m,0} \right] m_{TOT,0}
\]

(5.15)

If Eq.(5.15) and the expressions for the fiber and matrix mass fractions \( f_f = \frac{m_f}{m_{TOT}}, f_m = \frac{m_m}{m_{TOT}} \) are combined, the non-constant fiber and matrix mass fractions are:
5.2 Multi-stage approach

Since the material decomposes with the increase of temperature, the material properties do not remain constant. A way to take into consideration the polymer matrix multi-stage decomposition is proposed here by the author. Already has been presented (section 4.2) the simplified 3-step decomposition approach of the polymer matrix, according to which it undergoes four “phases” A, B, C, D and pyrolysis gases E. This simplified approach assumes that the only known products are A (virgin polymer) and D (carbonaceous char residue, mostly amorphous carbon), the intermediate products B and C, which are fictional, are considered to feature the same thermophysical properties of the virgin polymer A, Finally, the pyrolysis gases are assumed to have properties (density, thermal conductivity specific heat capacity) of air at elevated temperature [5.1].

These all apply to the polymer matrix, since in the current work composite materials having carbon fibers (and glass fibers in the case of GLARE) as reinforcement were investigated, all properties should be expressed using mixtures law and the volume fractions of matrix and fiber reinforcement. Following these assumptions the analytical expressions for the estimated composite apparent thermophysical properties were developed and implemented into the developed material model described in following chapters.

5.2.1 Density

Concerning the density of the different phases it applies that:
Thermophysical Properties Change.

\[ \rho_A = \rho_B = \rho_C = \rho_{\text{CHARGE}} \]  \hspace{1cm} (5.17)

As there is mass loss present during decomposition the total mass of the composite is constantly changing. At every moment the total mass of the composite material is the sum of its constituents: fiber reinforcement, undecomposed/virgin matrix (element-A), elements B and C and the char residue (element-D).

\[ m_{\text{TOT}} = m_f + m_A + m_B + m_C + m_D \]  \hspace{1cm} (5.18)

The volume fractions \( v_{A,B,C,D} \) of each reactant at every moment can be calculated from the rate equations solution, Eq.(4.31),(4.32),(4.33) and they take the form of Eq.(5.19) where \( v_A^0 \) is the initial matrix volume fraction.

\[ v_A = v_A^0 \frac{X_A}{X_d}, \quad v_B = v_B^0 \frac{X_B}{X_d} \frac{M_B}{M_A}, \quad v_C = v_C^0 \frac{X_C}{X_d} \frac{M_C}{M_A}, \quad v_D = v_D^0 \frac{1}{X_D} \left( X_d^0 - X_A \frac{M_B}{M_A} - X_C \frac{M_C}{M_A} \right) \]  \hspace{1cm} (5.19)

Using Eq.(5.17),(5.18),(5.19) the final equation for the total density of the composite material is extracted:

\[ \rho_{\text{TOT}} = v_f \rho_f + v_A^0 \left( \frac{X_A}{X_d} X_d^0 + \frac{X_B}{X_d} \frac{M_B}{M_A} + \frac{X_C}{X_d} \frac{M_C}{M_A} + \frac{X_D}{X_d} \frac{M_D}{M_A} \right) \cdot \rho_A \]  \hspace{1cm} (5.20)

Since in this modeling approach the total volume remains constant and char residue is by nature porous, its bulk density is given by the following expression, Eq.(5.21).

\[ \rho_{\text{CHARGE}}^{\text{BULK}} = \frac{m_A}{m_A^0 - m_A - m_B - m_C} \cdot \rho_A \]  \hspace{1cm} (5.21)

where, \( m_A^0 \) is the initial mass of the matrix (element-A initial mass), \( m_A, m_B, m_C \) are the masses of elements A, B, C and \( m_{\text{CHARGE}} \) is the mass of the char residue. Another
expression for the char bulk density is the one of Eq. (5.22) where the molar fractions are used. The porosity \( e \) of the char is calculated by Eq. (5.23),

\[
\rho_{\text{CHAR}}^\text{BULK} = \frac{X_D}{\left(X^A_A - X_D\right)} \cdot \frac{X_B}{\left(X^B_B - X_D\right)} \cdot \frac{X_C}{\left(X^C_C - X_D\right)} \cdot \left(\frac{\text{MB}_D}{\text{MB}_A}\right) \cdot \rho_A
\]

(5.22)

\[
e = 1 - \frac{\rho_{\text{CHAR}}^\text{BULK}}{\rho_{\text{TRUE}}} = \rho_{\text{GRAPHITE}} \Rightarrow e = 1 - \frac{X_D}{\left(X^A_A - X_D\right)} \cdot \frac{X_B}{\left(X^B_B - X_D\right)} \cdot \frac{X_C}{\left(X^C_C - X_D\right)} \cdot \left(\frac{\text{MB}_D}{\text{MB}_A}\right) \cdot \rho_A
\]

(5.23)

5.2.2 Thermal Conductivity

The through thickness conductivity of a fiber reinforced polymer matrix composite can be calculated using a mixtures law of the thermal resistances.

\[
\frac{1}{k_{\text{app}}} = v_f \cdot \frac{1}{k_f} + v_A \cdot \frac{1}{k_A} + v_B \cdot \frac{1}{k_B} + v_C \cdot \frac{1}{k_C} + v_{\text{CHAR}} \cdot \frac{1}{k_{\text{CHAR}}}
\]

(5.24)

where, \( k_f, k_A, k_B, k_C \), \( k_{\text{app}} \) are the thermal conductivity of the fiber reinforcement, elements \( A, B, C \) and the apparent thermal conductivity of the composite material respectively. Since elements \( B \) and \( C \) have the same properties as \( A \) then \( k_A = k_B = k_C = k_{\text{matrix}} \) and \( k_{\text{AIR}} \) is the thermal conductivity of air calculated from NASA polynomials [5.1], the thermal conductivity of the porous char [5.6],[5.7],[5.8] is calculated from Eq. (5.25),

\[
k_{\text{CHAR}} = \frac{k_{\text{GRAPHITE}} \cdot k_{\text{AIR}}}{e \cdot (k_{\text{GRAPHITE}} - k_{\text{AIR}}) + k_{\text{AIR}}}
\]

(5.25)

5.2.3 Specific Heat Capacity

The specific heat capacity of the composite material quantifies its ability to store thermal energy. Since energy is conserved the following expression of the total energy has the form,
Expressing Eq.(5.26) in specific heat capacity form it is obtained that:

\[ C_{p_{\text{TOT}}} = \frac{m_f}{m_{\text{TOT}}} C_{p_f} + \frac{m_d}{m_{\text{TOT}}} C_{p_d} + \frac{m_b}{m_{\text{TOT}}} C_{p_b} + \frac{m_c}{m_{\text{TOT}}} C_{p_c} + \frac{m_a}{m_{\text{TOT}}} C_{p_a} + \frac{1}{m_{\text{TOT}}} E_{\text{DECOMP}} \Delta T \]  

(5.27)

where \( f_i = m_i / m_{\text{TOT}} \) are the mass fractions of every constituent. Since \( C_{p_d} = C_{p_b} = C_{p_f} \), and as mentioned earlier element-D is the char residue, then substituting these in Eq.(5.27) the following expression is extracted:

\[ C_{p_{\text{TOT}}} = f_f \left( C_{p_f} - C_{p_d} \right) + f_{\text{CHAR}} \left( C_{p_{\text{CHAR}}} - C_{p_d} \right) + C_{p_d} + \frac{E_{\text{DECOMP}}}{m_{\text{TOT}}} \]  

and, \( f_f = \frac{\rho_f}{\rho_{\text{TOT}}} \)  

(5.28)

where, \( E_{\text{DECOMP}} \) is the polymer matrix Heat-of-Decomposition [5.5]. In Eq.(5.28) the specific heat capacities \( C_{p_d} \) and \( C_{p_{\text{CHAR}}} \) are considered to be constant with temperature. On the other hand, regarding the fiber reinforcement specific heat capacity \( C_{p_f} \) Differecial Scanning Calorimetry DSC tests were performed [5.9] on two types of polyacrylonitrile (PAN) based carbon fiber tows. After some calculations the final expression for char mass fraction is given by Eq.(5.29).

\[ f_{\text{CHAR}} = \frac{v_f}{v_f + \frac{X_A}{X_A + \frac{X_{MB}}{X_{MB}}}} \frac{\rho_{\text{MB}}}{\rho_{\text{TOT}}} \]  

(5.29)

The specific heat of the char residue (element-D) is calculated having in mind that the char is a porous material that consists of graphite and air “bubbles”, so its final expression has to consider porosity as well:

\[ C_{p_{\text{CHAR}}} = (1 - e) \frac{\rho_{\text{GRAPHITE}}}{\rho_{\text{CHAR}}} C_{p_{\text{GRAPHITE}}} + e \frac{\rho_{\text{AIR}}}{\rho_{\text{CHAR}}} C_{p_{\text{AIR}}} \]  

(5.30)
5.3 Aluminium thermophysical properties

The thermophysical properties of the 2024-T3 aluminium alloy were not assumed to be constant but varying with temperature [5.10],[5.11],[5.12]. The available data were not only for the solidus region of the Al alloy but also for the liquidus one. The temperature variation of Aluminium 2024-T3 thermal conductivity, density and specific heat capacity are shown in Figure 5.2.

![Figure 5.1: (a) Density, (b) Thermal conductivity and specific heat versus temperature for aluminum.](image)

5.4 References


CHAPTER 6  Fire Load Measurement and Calibration.

In this chapter the two fire burnthrough load options will be described. At first the ISO 2685 Burner [6.1], fire load measurement and calibration will be described and at the second part the smaller scale propane burner (from now on referred as AML Burner). The ISO 2685 burner handles large specimens 406x406 mm (16”x16”) whilst the AML Burner deals with smaller lab-scale ones [100x100mm].

6.1  ISO 2685 Burner.

The burnthrough burner was manufactured and assembled by the Health and Safety Laboratory, HSL in the UK, in accordance to the ISO 2685 Standard [6.1]. A brief description on the key geometry and flow parameters are mentioned in this section. At first a mesh sensitivity analysis was performed and a preliminary transient simulation performed to simulate the ISO Standard test calibration procedure. In a second step, a more detailed calibration procedure under steady state conditions was performed.
6.1.1 Preliminary analysis

6.1.1.A CFD Methodology

Four different configurations have been tested in the current work, Figure 6.1.

a.) Simplified burner and plate configuration.
b.) Heat flux calibration configuration.
c.) Temperature calibration configuration.
d.) Burner and plate test configuration, with finite plate thickness.

The first of these, Configuration a, was used to undertake a range of sensitivity tests to examine the influence of the computational grid, turbulence model, radiation model and domain size on the CFD predictions.

Figure 6.1 The four configurations studied: a.) Simplified burner and plate; b.) Heat flux calibration; c.) Temperature calibration; d.) Burner and plate test with finite plate thickness
All of the CFD simulations used an identical burner geometry and inlet fuel/air conditions. A detailed view of the burner geometry is shown in Figure 6.2. Premixed fuel/air is released through 332 holes in the burner face plate and secondary air through 373 holes. The total mass flow rate of premixed fuel/air was 3.49g/hr and of secondary air 3.52g/hr. The fraction of propane in the fuel/air mixture was 17% by mass.

Since all of the flows considered are symmetric about the vertical mid-plane, only one half of the geometry was simulated to minimize computing time. It was assumed that the ambient environment around the burner was at a temperature of 30 °C. All of the simulations were performed using ANSYS-CFX 11.

![Figure 6.2 Detailed view of the modelled burner showing the matrix of primary premixed fuel-air inlets and the secondary air inlets](image)

Tests have been undertaken using the simplified burner and plate geometry (Configuration a) to examine the sensitivity of the CFD model predictions to the choice of computational grid, turbulence model, radiation model and domain size. In each of the tests, the plate temperature was fixed at 30 °C and results are compared in terms of their predicted heat flux on the plate.
In the first set of tests, simulations were performed on a coarse grid of approximately 400000 nodes and a fine grid with 800000 nodes. Figure 6.3 shows a cross-section through the coarse and fine grids and surface mesh on the burner plate. The computing time required to perform CFD simulations increases in proportion to the number of grid nodes and the fine grid was at the limit of what was practicable, given the available computing resources. In both simulations, the SST turbulence model and P1 radiation model were used.

![Figure 6.3 Cross-sectional views of the coarse and fine meshes on the mid-plane and close-up views of the mesh on the burner face](image)

Heat flux predictions using the coarse and fine grids are shown in Figure 6.4. There are some differences between the two sets of results in and above the centre of the plate. However, the general values of heat flux are similar in the two cases, and for the subsequent simulations presented in this report the coarser grid was used.
A range of different turbulence models exists and each features advantages and limitations in terms of their accuracy in different flows and their computing demands. Two models were tested in the current work, the Shear Stress Transport (SST) model and the $k$-$\varepsilon$ model. Both of these are widely used in industrial CFD simulations.

The Shear-Stress Transport (SST) $k$-$\omega$ model was developed by Menter to effectively blend the robust and accurate formulation of the $k$-$\omega$ model in the near-wall region with the free-stream independence of the $k$-$\varepsilon$ model in the far field. To achieve this, the $k$-$\varepsilon$ model is converted into a $k$-$\omega$ formulation. The SST $k$-$\omega$ model is similar to the standard $k$-$\omega$ model, but includes the following refinements:

- The standard $k$-$\omega$ model and the transformed $k$-$\varepsilon$ model are both multiplied by a blending function and both models are added together. The blending function is designed to be one in the near-wall region, which activates the standard $k$-$\omega$ model, and zero away from the surface, which activates the transformed $k$-$\varepsilon$ model.
- The SST model incorporates a damped cross-diffusion derivative term in the $\omega$ equation.
- The definition of the turbulent viscosity is modified to account for the transport of the turbulent shear stress.
- The modeling constants are different.
These features make the SST $k-\omega$ model more accurate and reliable for a wider class of flows than the standard $k-\omega$ model. Other modifications include the addition of a cross-diffusion term in the $\omega$ equation and a blending function to ensure that the model equations behave appropriately in both the near-wall and far-field zones.

The SST $k-\omega$ model has a similar form to the standard $k-\omega$ model:

$$\frac{\partial}{\partial t} (\rho k) + \frac{\partial}{\partial x_i} (\rho k u_i) = \frac{\partial}{\partial x_j} \left( \Gamma_k \frac{\partial k}{\partial x_j} \right) + \tilde{G}_k - Y_k + S_k$$  \hspace{1cm} (6.1)

and

$$\frac{\partial}{\partial t} (\rho \omega) + \frac{\partial}{\partial x_i} (\rho \omega u_i) = \frac{\partial}{\partial x_j} \left( \Gamma_\omega \frac{\partial \omega}{\partial x_j} \right) + \tilde{G}_\omega - Y_\omega + D_\omega + S_\omega$$  \hspace{1cm} (6.2)

In these equations, $\tilde{G}_k$ represents the generation of turbulence kinetic energy due to mean velocity gradients, $G_\omega$ represents the generation of $\omega$, $\Gamma_k$ and $\Gamma_\omega$ represent the effective diffusivity of $k$ and $\omega$ respectively, $Y_k$ and $Y_\omega$ represent the dissipation of $k$ and $\omega$ due to turbulence, $D_\omega$ represents the cross-diffusion term, $S_k$ and $S_\omega$ are user-defined source terms.

The standard $k-\epsilon$ model. The simplest "complete models" of turbulence are two-equation models in which the solution of two separate transport equations allows the turbulent velocity and length scales to be independently determined. The standard $k-\epsilon$ model falls within this class of turbulence model and has become the workhorse of practical engineering flow calculations in the time since it was proposed by Launder and Spalding. Robustness, economy, and reasonable accuracy for a wide range of turbulent flows explain its popularity in industrial flow and heat transfer simulations. It is a semi-empirical model, and the derivation of the model equations relies on phenomenological considerations and empiricism.

As the strengths and weaknesses of the standard $k-\epsilon$ model have become known, improvements have been made to the model to improve its performance. Two of these variants are: the RNG $k-\epsilon$ model and the realizable $k-\epsilon$ model.
The standard $k\text{-}\epsilon$ model is a semi-empirical model based on model transport equations for the turbulence kinetic energy ($k$) and its dissipation rate ($\epsilon$). The model transport equation for $k$ is derived from the exact equation, while the model transport equation for $\epsilon$ was obtained using physical reasoning and bears little resemblance to its mathematically exact counterpart. In the derivation of the $k\text{-}\epsilon$ model, the assumption is that the flow is fully turbulent, and the effects of molecular viscosity are negligible. The standard $k\text{-}\epsilon$ model is therefore valid only for fully turbulent flows.

The turbulence kinetic energy, $k$, and its rate of dissipation, $\epsilon$, are obtained from the following transport equations:

\begin{align}
\frac{\partial}{\partial t}(\rho k) + \frac{\partial}{\partial x_i} (\rho k u_i) &= \frac{\partial}{\partial x_j} \left[ \left( \mu + \frac{\mu_t}{\sigma_k} \right) \frac{\partial k}{\partial x_j} \right] + G_k + G_b - \rho \epsilon - Y_M + S_k \tag{6.3} \\
\frac{\partial}{\partial t}(\rho \epsilon) + \frac{\partial}{\partial x_i} (\rho \epsilon u_i) &= \frac{\partial}{\partial x_j} \left[ \left( \mu + \frac{\mu_t}{\sigma_\epsilon} \right) \frac{\partial \epsilon}{\partial x_j} \right] + C_{1\epsilon} \frac{\epsilon}{k} (G_k + \alpha_\epsilon G_b) - C_{2\epsilon} \frac{\epsilon^2}{k} + S_\epsilon \tag{6.4}
\end{align}

In these equations, $G_k$ represents the generation of turbulence kinetic energy due to the mean velocity gradients, $G_b$ is the generation of turbulence kinetic energy due to buoyancy, $Y_M$ represents the contribution of the fluctuating dilatation in compressible turbulence to the overall dissipation rate, $C_{1\epsilon}, C_{2\epsilon}$, and $\alpha_\epsilon$ are constants. $\sigma_k$ and $\sigma_\epsilon$ are the turbulent Prandtl numbers for $k$ and $\epsilon$, respectively. $S_k$ and $S_\epsilon$ are user-defined source terms.

The SST model is perhaps slightly more suited to the case considered here where heat transfer to the wall is of primary importance and where the dimensionless near-wall cell size is small ($y^+ < 9$). The $k\text{-}\epsilon$ model implemented in the CFD code ANSYS-CFX11 relies upon assumed profiles of the velocity and temperature near the wall which are more appropriate for high-speed aerodynamic flows.
Heat flux predictions using the SST and $k$-$\varepsilon$ models are presented in Figure 6.5. In both cases the P1 radiation model and the coarse grid were used. There are differences in the predicted heat flux at the centre of the plate between the two models. The $k$-$\varepsilon$ model predicts a heat flux of around 40 kW/m$^2$, compared to a value of around 60 kW/m$^2$ for the SST model. It was considered that the SST model was likely to provide more reliable predictions than the $k$-$\varepsilon$ model in this flow and therefore for all subsequent simulations, the SST model was used.

![Figure 6.5 Heat flux predictions using different turbulence models](image)

- **Radiation Model**

All of the CFD simulations presented above used the P1 radiation model. This is a relatively simple radiation model best suited to situations where the medium through which the radiation passes is optically thick (as opposed to transparent). Two further radiation models have been tested in the present work: the Discrete Transfer model and the Monte Carlo model. The former is computationally more efficient since the trajectories of ray paths are computed only once at the start of the CFD simulation. With the Monte Carlo model, the photon trajectories are functions of the absorption coefficient and wall emissivity, and therefore need to be recalculated every few iterations, which requires slightly longer computing times.

The P-1 radiation model is the simplest case of the more general P-N model, which is based on the expansion of the radiation intensity $I$ into an orthogonal series of
spherical harmonics. If only four terms in the series are used, the following equation is obtained for the radiation flux $q_r$:

$$q_r = -\frac{1}{3(a + \sigma_s) - C\sigma_s} \nabla G$$  \hspace{1cm} (6.5)

where $a$ is the absorption coefficient, $\sigma_s$ is the scattering coefficient, $G$ is the incident radiation, and $C$ is the linear-anisotropic phase function coefficient, described below. After introducing the parameter

$$\Gamma = \frac{1}{(3(a + \sigma_s) - C\sigma_s)}$$  \hspace{1cm} (6.6)

Eq.(6.5) simplifies to

$$q_r = -\Gamma \nabla G$$  \hspace{1cm} (6.7)

The transport equation for $G$ is

$$\nabla \cdot (\Gamma \nabla G) - aG + 4a\sigma T^4 = S_G$$  \hspace{1cm} (6.8)

where $\sigma$ is the Stefan-Boltzmann constant and $S_G$ is a user-defined radiation source. This equation is solved to determine the local radiation intensity when the P-1 model is active. Combining Eq.(6.7),(6.8), the following equation is obtained:

$$-\nabla \cdot q_r = aG - 4a\sigma T^4$$  \hspace{1cm} (6.9)

The expression for $-\nabla \cdot q_r$ can be directly substituted into the energy equation to account for heat sources (or sinks) due to radiation.

The main assumption of the Discrete Transfer Radiation Model, DTRM is that the radiation leaving the surface element in a certain range of solid angles can be approximated by a single ray.
The equation for the change of radiant intensity, \( dI \), along a path, \( ds \), can be written as:

\[
\frac{dI}{ds} + aI = \frac{\sigma T^4}{\pi}
\]  

(6.10)

where: \( a \) is the gas absorption coefficient, \( I \) is the intensity, \( T \) is the gas local temperature and \( \sigma \) is the Stefan-Boltzmann constant (5.672E-8W/m\(^2\)K\(^4\)).

Here, the refractive index is assumed to be unity. The DTRM integrates Eq.(6.10) along a series of rays emanating from boundary faces. If \( a \) is constant along the ray, then \( I(s) \) can be estimated as:

\[
I(s) = \frac{\sigma T^4}{\pi} \left(1 - e^{-as}\right) + I_0 e^{-as}
\]  

(6.11)

where \( I_0 \) is the radiant intensity at the start of the incremental path, which is determined by the appropriate boundary condition. The energy source in the fluid due to radiation is then computed by summing the change in intensity along the path of each ray that is traced through the fluid control volume. The "ray tracing" technique used in the DTRM can provide a prediction of radiative heat transfer between surfaces without explicit view-factor calculations. The accuracy of the model is limited mainly by the number of rays traced and the computational grid.

Heat fluxes predicted using the P1, Discrete Transfer and Monte Carlo models are presented in Figure 6.6. The P1 values are significantly lower than either the Discrete Transfer or Monte Carlo values, by a factor of between 2 and 2.5 (notice the difference in scales between Figures 6.6 and the previous Figures 6.4,6.5). It appears that the P1 model may have predicted too much absorption within the gas phase which has produced a significantly lower, and generally more homogeneous pattern of heat flux to the plate. The Discrete Transfer and Monte Carlo models show a similar heat flux distribution, with the Monte Carlo model producing slightly higher peak values in the centre of the plate.
Fire Load Measurement and Calibration.

Figure 6.6 Heat flux predictions using different radiation models

⇒ **Domain Size**

In all of the results presented above the computational domain in the vertical and horizontal directions extended to the edges of the plate, which had dimensions of 0.406 × 0.406 m (or 16”×16”). This domain size was chosen in order to maximize the grid resolution and avoid unnecessary grid nodes outside the main region of interest which would unnecessarily lengthen the computing time. However, the predicted heat fluxes towards the upper edge of the plate were high compared to what was observed experimentally. Tests were therefore undertaken using a slightly larger domain size to assess whether the location of the computational domain boundaries affected the flow behaviour near the edges of the plate. In these tests, the computational domain was extended a further 0.0508m in the vertical and horizontal directions around all sides of the plate. Since in the experiments the plate was mounted in a heat-shield frame, the
additional area around the plate in the CFD model was treated as a wall, fixed at the same temperature as the plate.

Heat flux predictions using the original domain and the larger domain size are compared in Figure 6.7, in both cases using the Discrete Transfer radiation model. The results show that the location of the domain boundaries in the previous tests adversely affected the flow behaviour. Using a larger domain boundary removed the high heat fluxes near the upper edge of the domain, whilst the behaviour in the central region of the plate remained relatively unchanged.

![Figure 6.7 Heat flux predictions using different domain sizes](image)

### 6.1.1.C CFD Results and Calibration.

- **Heat Flux Calibration Tests**

  The ISO 2685 Standard [6.1] specifies that the heat flux density received by the continuous flow calorimeter should be $116\pm10\text{ kW/m}^2$. The calorimeter consists of a copper tube with a nominal outside diameter of 0.0127m (ISO 2685 suggests a diameter between 12 and 13mm) through which water flows at a known rate. The standard specifies that the water supply temperature should be between 10°C and 21°C. By measuring the water temperature at the pipe outlet, the heat flux density to the pipe is then calculated using the following formula:
Fire Load Measurement and Calibration.

\[
q = \frac{Q}{A} = \frac{\dot{V}_{\text{water}} \rho (T_2 - T_1)}{37.7 \times 10^{-3} \times L} \tag{6.12}
\]

where \(Q\) is the total heat flux to the water, \(A\) is the pipe surface area, \(\dot{V}_{\text{water}}\) is the water volume flow rate, \(\rho\) is the water density, \(c\) is the specific heat capacity of water, \(T_1\) is the temperature of the water at the inlet to the pipe, \(T_2\) the water temperature at the outlet, and \(L\) the length of the portion of the pipe exposed to the flame. The constant \(37.7 \times 10^{-3}\) in the denominator of the above equation is the circumference of the pipe. In the calibration tests, the outlet water temperature, \(T_2\), is recorded every 30s over a 3 minutes period, i.e. the flow is allowed to reach a steady state.

CFD Simulations of the heat flux calibration tests were performed using the geometry shown in Figure 6.1b where the centre of the pipe is positioned at a distance of 0.0762m from the burner plate. The pipe length in the half domain model was 0.203m and the pipe wall was assigned a constant temperature of 15°C. The SST turbulence model and Monte Carlo radiation model were used for these tests. Figure 6.8 shows the predicted heat flux on the pipe surface, with the greatest value in the region exposed directly to the flame.

In order to calculate the heat flux density using the above formula, it is necessary to determine the width of the flame, \(L\). This is not explicitly calculated in the CFD model but can be estimated from the temperature immediately upstream of the calorimeter pipe, Figure 6.9. The temperature profile suggests that the flame half-width is between 60 and 80mm. The heat flux density, \(q\), calculated from the CFD model is a function of the flame width and is also shown in Figure 6.9. For a flame width of between 60 and 80mm, the heat flux density varies between 180 and 140kW/m². This is somewhat higher than the value given in the ISO 2685 Standard [6.1] of between 106 and 126kW/m².
Temperature Calibration Tests

The ISO 2685 Standard [6.1] also specifies calibration conditions for the flame temperature. The temperature is recorded using an array of nine thermocouples at specified locations and it is required that the flame temperature is 1100°C±80°C.
Since the thermocouples are unshielded, the true temperature may be higher than the measured value due to radiation effects.

The CFD model used to compare to these measurements is shown in Figure 6.1c. Predicted temperatures are shown in Figure 6.10 on the plane of the thermocouples, positioned 76.2mm from the burner plate. In the lower third of the flame the predicted temperatures are well below the target value of 1100°C (predicted values are 203°C and 325°C) and those in the upper third are significantly higher (values between 1536°C and 1562°C). Overall, the mean temperature predicted by all nine thermocouples is 1113°C.

![Figure 6.10: Predicted temperatures in the plane of the thermocouples](image)

- **Burner and Plate with Finite Thickness**

  It is challenging to model the combined effects of combustion, heat transfer and heat conduction within a single CFD simulation. The main difficulty relates to the different timescales needed to resolve simultaneously the combustion phenomena and heat conduction. Whilst a computational time-step of the order of a few milliseconds at most is required for modelling combustion, the temperature of the plate rises over a
period of minutes. To resolve both timescales in the same simulation requires substantial computing resources, beyond the current capabilities.

Three approaches have been tested to predict the transient thermal behaviour of the plate. In the first, a large time-step of the order of 1 second was used and the CFD simulations were started from a well-converged previous result with a plate temperature of 30°C. Unfortunately, the large time-step produced numerical instabilities in the combustion region of the CFD simulations which gave unphysically high temperatures. The second approach involved holding the dynamic field constant (freezing the fluid velocity, pressure and turbulence quantities in the CFD simulation) and solving only for the thermal field. This also produced unphysical temperatures. The third approach tested involved applying a fixed heat flux to the burner side of the plate, i.e. a heat flux which remained constant over time, and then solving for the heat conduction through the plate and heat transfer from the plate to its surroundings. Results from this third approach are presented below.

The heat flux applied to the plate was calculated from CFD simulations using the simplified burner and plate configuration outlined in the previous section, as shown in Figure 6.7 for the large domain. The modelled aluminium plate had a thickness of 3mm and the CFD model also included an air gap on the back of the plate of thickness 12.7mm with an assumed ambient temperature of 30°C. In the simulations, as the plate temperature increased, the air in this gap was warmed and due to its natural buoyancy gave rise to a vertical velocity which increased to around 1 to 2m/s over a period of 5 minutes.

To examine the relative importance of convective and radiative heat transfer modes, three sets of CFD simulations were performed, in which:

1.) Neither convection nor radiation were modelled.
2.) Convection from the plate to the air was modelled but radiative heat transfer was ignored.
3.) Convection and radiative heat transfer were modelled.
The temperature at the centre of the plate for the three cases is shown in Figure 6.11. In Cases 1 and 2, the temperature rose rapidly and after 5 minutes reached values of approximately 2000°C and 1800°C respectively. In Case 3, where both convection and radiation effects were accounted for, the temperature at the centre of the plate rose rapidly in the first minute but thereafter its rate of temperature rise decreased so that after 5 minutes the plate had reached a temperature of around 800°C. The results suggest that natural convection appears to transfer a relatively modest amount of heat compared to radiative heat transfer.

The temperature on the back of the plate for Case 3 is shown in Figure 6.12 at intervals of 30 seconds for a period of 5 minutes. The results show that highest temperatures are reached in the centre of the plate.

The approach taken to model the transient thermal behaviour of the plate is likely to have overpredicted the plate temperatures. The heat flux imposed on the side of the plate facing the burner was set from a previous calculation in which the plate temperature was 30°C. In reality, as the plate warms, the temperature difference between the hot combustion gases and the plate surface will decrease and as a consequence the heat flux will decrease. The scenario modelled instead uses a heat flux which remains fixed at its maximum value for the duration of the test.
Figure 6.12  Predicted temperatures at 30 second intervals on the back of the plate
Figure 6.13  Experimental images of temperature at 30 second intervals on the back of the plate
Figure 6.13 shows infrared images of the back of an Aluminium plate recorded at 30 second intervals during a test. Prior to the experiment, a rectangular area was painted with a substance of known emissivity. This rectangular area is clearly visible in each of the infrared images and it allows the temperatures on the back of the plate to be inferred.

Comparison of the images presented in Figure 6.13 with the CFD results presented in Figure 6.12 reveals some differences. In particular, whereas the CFD simulation predicted that the temperatures would reach 600°C after 60 seconds, the temperatures in the experiment did not reach 600°C until after approximately 200 seconds.

This observation is supported by the data plotted in Figure 6.14, which provides temperature data at two points; point 5, which is located at the centre of the plate and point 6, which is located 8cm vertically above point 5.

A possible explanation for this was outlined above. Namely, that the heat flux (from the simulation performed with the plate boundary fixed at a temperature of 30°C) used in the CFD simulation was constant and did not diminish with time as the plate warmed up, which is what would have occurred in the experiment.
6.1.2 Detailed Calibration.

ISO 2685 [6.1] provides a procedure for testing the fire resistance of materials used in the manufacture of aircraft. The test consists of subjecting the material to a specified heat flux from a propane (or jet fuel) burner for 15 minutes. To pass, the material must not allow the flame to burn through the material for the duration of the test. ANSYS CFX 12.1 was used to carry out a comprehensive analysis of the ISO 2685 [6.1] test. The simulations were three-dimensional and steady-state. Three different resolution tetrahedral meshes with prismatic inflation layers in the near-wall region were used. The simulations were performed using the Eddy Dissipation combustion model and the SST turbulence model. Simulations were performed with 1-step and 2-step chemistry and the thermal boundary condition on the plate was a fixed temperature of 300K.

The Eddy-Dissipation Model. Most fuels are fast burning, and the overall rate of reaction is controlled by turbulent mixing. In non-premixed flames, turbulence slowly convects/mixes fuel and oxidizer into the reaction zones where they burn quickly. In premixed flames, the turbulence slowly convects/mixes cold reactants and hot products into the reaction zones, where reaction occurs rapidly. In such cases, the combustion is said to be mixing-limited, and the complex and often unknown, chemical kinetic rates can be safely neglected.

ANSYS CFX 12.1 provides a turbulence-chemistry interaction model, based on the work of Magnussen and Hjertager, called the Eddy-Dissipation model. The net rate of production of species \( \dot{i} \) due to reaction \( r \), \( R_{\dot{i},r} \), is given by the smaller of the two expressions below:

\[
\begin{align*}
R_{\dot{i},r} &= v_{i,r} M_{wi,i} A \rho \frac{\epsilon}{k} \min \left( \frac{Y_r}{v_{r,r} M_{wi,r}} \right) \\
R_{\dot{i},r} &= v_{i,r} M_{wi,i} A B \rho \frac{\epsilon}{k} \sum_j \frac{Y_j}{v_{j,r} M_{wi,j}} 
\end{align*}
\]

(6.13)  
(6.14)
where: \( Y_P \) is the mass fraction of any product species, \( P \), \( Y_R \) is the mass fraction of a particular reactant, \( R \), \( A \) is an empirical constant equal to 4.0, \( B \) is an empirical constant equal to 0.5.

In Eq.(6.13),(6.14) the chemical reaction rate is governed by the large-eddy mixing time scale, \( k/\epsilon \), as in the eddy-breakup model of Spalding. Combustion proceeds whenever turbulence is present (\( k/\epsilon > 0 \)), and an ignition source is not required to initiate combustion. This is usually acceptable for non-premixed flames, but in premixed flames, the reactants will burn as soon as they enter the computational domain, upstream of the flame stabilizer. To remedy this, ANSYS CFX 12.1 provides the finite-rate/eddy-dissipation model, where both the Arrhenius, and eddy-dissipation reaction rates are calculated. The net reaction rate is taken as the minimum of these two rates. In practice, the Arrhenius rate acts as a kinetic "switch", preventing reaction before the flame holder. Once the flame is ignited, the eddy-dissipation rate is generally smaller than the Arrhenius rate, and reactions are mixing-limited.

Heat transfer to the plate occurs by convection and radiation. The distribution of convective heat transfer was linked to a flow stagnation region, which was clearly visible in the experiments and the CFD modelling. The maximum predicted convective heat flux was about 54.4kW/m\(^2\). Radiative heat transfer has a number of effects on the flame including lowering of the flame temperature below the adiabatic limit. However, the present analysis focused on an examination of the radiative heat transfer from the burner flame to a test plate. Radiation was modelled using the Discrete Transfer radiation model coupled with a Gray spectral model. Simulations were performed with plate emissivities of 1.0 and 0.2, the latter value being more realistic and giving maximum radiative heat flux of approximately 8kW/m\(^2\).

In summary, the main conclusions from the work are as follows:

1) The main characteristics of the ISO 2685[6.1] propane burner flame including the location of the flow stagnation region were well predicted by the CFD model.
2) Predicted flame temperatures were in good agreement with the calculated adiabatic flame temperature.

3) The wall heat transfer coefficient was sensitive to the mesh resolution. However, predictions obtained using the highest resolution mesh were in good agreement with empirical correlations.

4) The maximum wall heat transfer coefficient corresponded to a maximum convective heat flux of 54.4kW/m². This was less than the heat flux measurements of 70 to 100kW/m² reported by Abu-Talib et al, [6.2].

5) The radiative heat flux was strongly dependent on the material emissivity and flame luminosity. Predictions of the radiative heat flux from a non-luminous flame ranged from 8 to 28kW/m², although higher values are possible for a luminous flame.

### 6.1.2.A CFD Modelling Without Radiation

- **Geometry and Boundary Conditions**
  
  The ISO 2685 burner [6.1] releases premixed propane and air through 373 holes and secondary air through a further 332 holes. The diameter of the premixed fuel holes is 1.778mm and the diameter of the secondary air holes is 2.578mm. Images of the experimental and CFD model propane burners are shown in Figure 6.15 (note that the premixed fuel holes appear larger in the experimental burner because they have tapered ends).
The premixed fuel and secondary air holes were spaced at regular 7.62mm intervals in agreement with the experimental setup. The fuel and secondary air holes were surrounded by a casing having a diameter of 184.15mm, a width of 6.35mm and a depth of 15.875mm.

ISO 2685 [6.1] provides guideline pressures for the gas release rates. However, in practice the gas release rates are controlled in terms of volumetric flow rates. The gas inlet boundary conditions used in the experiments and CFD model were provided by Mullender, [6.3] and are summarised in Table 6.1. The propane was supplied at 0.51g/s, the mixing air was supplied at 2.93g/s and the secondary air was supplied at 4.18g/s.

<table>
<thead>
<tr>
<th>ISO 2685 Pressure (Pa)</th>
<th>CFD Model Flow rate (gr/sec)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Propane gas</td>
<td>440</td>
</tr>
<tr>
<td>Mixing air</td>
<td>4265</td>
</tr>
<tr>
<td>Secondary air</td>
<td>2950</td>
</tr>
</tbody>
</table>

Table 6.1  Inlet boundary conditions.

In the CFD model a temperature of 300K was assumed for the premixed fuel and secondary air inlets and the surrounding open boundaries, which are shown in Figure 6.16. Figure 6.16 also shows the test plate, which was located a distance of 76.2mm from the burner, in accordance with the ISO Standard [6.1].
Modelling Approach.

CFD simulations were performed using ANSYS CFX 12.1. The simulations were three dimensional and steady-state. Transient simulations were attempted but adequate convergence of the heat transfer was not achieved. Three different resolution tetrahedral meshes with prismatic inflation layers were used as shown in Figure 6.17. The coarse mesh used 2.2 million nodes, the medium mesh used 6.6 million nodes and the fine mesh used 18.6 million nodes.

![Figure 6.16 Different views of the computational geometry including burner, open boundaries and test plate](image)

Heat transfer was modelled using the total energy model and the thermal boundary condition on the plate was a fixed temperature of 300K. The simulations were performed using the SST turbulence model and the Eddy Dissipation combustion model with 1-step or 2-step chemistry. In 1-step chemistry propane and oxygen are converted straight to water plus carbon dioxide plus heat. In 2-step chemistry there is an intermediate step involving carbon monoxide. An overview of the simulations performed during this work is provided in Table 6.2.

<table>
<thead>
<tr>
<th>Simulation filename</th>
<th>Mesh</th>
<th>Turbulence model</th>
<th>Chemistry</th>
<th>Plate temperature (K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>sim034</td>
<td>medium</td>
<td>SST</td>
<td>1-step</td>
<td>300</td>
</tr>
<tr>
<td>sim035</td>
<td>fine</td>
<td>SST</td>
<td>1-step</td>
<td>300</td>
</tr>
<tr>
<td>sim036</td>
<td>coarse</td>
<td>SST</td>
<td>1-step</td>
<td>300</td>
</tr>
<tr>
<td>sim037</td>
<td>medium</td>
<td>SST</td>
<td>2-step</td>
<td>300</td>
</tr>
</tbody>
</table>

Table 6.2 Overview of simulations.
Results and Discussion

Streamlines and temperature contours from simulation *sim036* are presented in Figure 6.18 to highlight the main characteristics of the fuselage burnthrough simulations. The streamlines show the flammable gases exiting the burner,
combusting and impinging on the plate boundary. Most of the hot combustion gases are directed upwards due to buoyancy. However, some are forced downwards leading to recirculation and stagnation regions. These recirculation and stagnation regions were clearly visible in the experiments.

Figure 6.18 Centre plane streamlines (left) and temperature contour predictions (right) from simulation sim036

The temperature distribution, which is also shown in Figure 6.18, is intrinsically linked to the fluid flow field. Hot gases flow upwards upon meeting the plate boundary. The maximum flame temperature predicted by the CFD model was about 2400K, which is in good agreement with the adiabatic flame temperature, section 6.1.2.B

The fluid flow field also governs the fluid to plate heat transfer distribution. In this work the heat transfer is expressed in terms of the heat transfer coefficient, $h_c$. 


where \( q \) is the heat flux, \( T_f \) is a reference flame temperature and \( T_w \) is the wall temperature. The reference flame temperature was set to 2400K.

The heat transfer distribution for simulation \textit{sim036} is shown in Figure 6.19. The location of the maximum heat transfer corresponds to the stagnation region. The maximum heat transfer coefficient in simulation \textit{sim036} was approximately 18W/m\(^2\)K.

Contour plots of heat transfer coefficient from all four simulations are shown in Figure 6.20. These provide a qualitative comparison of the predicted heat transfer coefficient, which had a broadly similar distribution in each simulation with the
maximum heat transfer coefficient corresponding to the location of the flow stagnation region.

Figure 6.20 Contour plots of heat transfer coefficient predictions on a test plate; sim034 medium mesh, sim035 coarse mesh, sim036 fine mesh and sim037 using 2-step chemistry.

The results for heat transfer coefficient from simulations on the coarse (sim036) and medium (sim034) resolution meshes are similar. However, the results from the fine mesh (sim035) showed higher values of the heat transfer coefficient. The need to
use a fine near wall mesh for heat transfer applications is well documented and is the probable course of the mesh effects observed here.

A more quantitative comparison of the different simulations is provided in Table 6.3. The maximum heat transfer coefficient obtained using the coarse, medium and fine resolution meshes were 18.7W/m²K, 23.2W/m²K and 25.9W/m²K, respectively.

<table>
<thead>
<tr>
<th>Simulation filename</th>
<th>Mesh</th>
<th>Average $h_c$ (W/m²K)</th>
<th>Maximum $h_c$ (W/m²K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>sim034</td>
<td>medium</td>
<td>10.0</td>
<td>23.2</td>
</tr>
<tr>
<td>sim035</td>
<td>fine</td>
<td>11.0</td>
<td>25.9</td>
</tr>
<tr>
<td>sim036</td>
<td>coarse</td>
<td>9.6</td>
<td>18.7</td>
</tr>
<tr>
<td>sim037</td>
<td>medium</td>
<td>10.0</td>
<td>23.2</td>
</tr>
</tbody>
</table>

Table 6.3 Overview of simulations performed.

### 6.1.2.B Model Validation.

Model validation was particularly important so to produce a unified model for the ISO 2685 burner test [6.1]. However, model validation in this case was difficult because there is only limited experimental data in literature and there remains considerable uncertainty regarding the relative importance of convective and radiative heat transfer processes. Radiative heat transfer processes are especially difficult to determine and have been observed to vary from test to test, as it is mentioned in section 6.1.2.C.

- Earlier Work

In section 6.1.1 model validation was performed using the ISO 2685 calibration procedures [6.1], which required calibration of flame temperature to 1100±80°C and heat flux to 116±10kW/m² at a distance of 76.2mm from the burner. Reasonable agreement was achieved between the model predictions and calibration measurements. However, it was later realised that the calibration measurements were not suitable for model validation as they are not reliable. For example, the thermocouples used for the temperature calibration measurements emit back radiation when exposed to the burner flame such that the temperatures they record are significantly lower than the actual flame temperatures, [6.2].
Abu-Talib et al. [6.2], proposed that actual flame temperatures are closer to 1900°C and reported heat flux measurements of 70 to 100kW/m². They used a Thin Film Gauge in conjunction with an insulating substrate to measure the heat flux, \( q \), with,

\[
q = \Delta T \left( \frac{k_s}{\Delta x} \right)
\]  

(6.16)

where \( k_s \) is the thermal conductivity of the substrate, \( \Delta x \) is thickness of the substrate and \( \Delta T \) is the temperature across the substrate.

The thermal resistance of the substrate, \( k_s/\Delta x \), was obtained by Abu-Talib et al., [6.2] by calibration experiments performed at low temperatures. The temperatures on the front and rear surfaces of the substrate were then measured during an ISO 2685 [6.1] test, thus allowing the heat flux, \( q \), to be calculated.

Model validation using these heat flux data was attempted in this section (6.1.2). However, the experimental data were not readily comparable to model predictions. In CFD models the heat flux is typically expressed by means of a heat transfer coefficient, \( h_c \), as follows,

\[
q = h_c (T_f - T_w)
\]  

(6.17)

where \( T_f \) is a reference flame temperature and \( T_w \) is the wall surface temperature. Thus the heat flux is a function of the wall temperature and decreases as the wall temperature increases. This is a standard way to express heat fluxes, for example Holman [6.4] and the literature on heat transfer from impinging jets, e.g. Baughn and Shimizu [6.5] and Cooper et al. [6.6], which has been reviewed by Hoyes and Ledin [6.7].

Using Equation (6.16) with a reference flame temperature of 2400K and a wall temperature of 300K, the maximum heat transfer coefficient from the CFD simulation \textit{sim035} of 25.9W/m²K gives a maximum predicted heat flux of 54.4kW/m². This is
less than the heat flux measurements of 70 to 100 kW/m$^2$ reported by Abu-Talib et al. [6.2].

**Validation Against Theory**

No further experimental data for the ISO 2685 [6.1] burner tests were available. Therefore, model validation was attempted using theoretical and semi-empirical expressions from the literature. Two thermodynamic properties are of interest initially; the adiabatic flame temperature and the total heat output.

The adiabatic flame temperature depends on the equivalence ratio. An equivalence ratio of 1.1 was calculated using the data in Table 6.1 (i.e. a slightly rich mixture). However, it is likely that some air is entrained from the surroundings, which will act to lower the equivalence ratio. Thus a reasonable approximation is to consider an equivalence ratio of 1, i.e. a stoichiometric mixture. The adiabatic flame temperature for a stoichiometric mixture of propane and air at 298K and 1 atm is 2267K according to Turns [6.8]. However, if the combustion products are restricted to water and carbon dioxide then the adiabatic flame temperature is 2393K (calculated using Gaseq [6.9]). This is in good agreement with the present CFD model, which is limited to one (or two) step chemistry.

The total heat output of the burner was calculated using the lower heating value (which assumes that the water produced by the combustion process remains gaseous) for propane in air. Assuming 298K and 1 atm, the lower heating value is 46357 kJ/kg [6.8]. Multiplying the lower heating value by the propane mass flow rate gives a total heat output for the burner of 23.6kW. Uniformly applying this heat output to a plate measuring 0.4 x 0.4 m gives a heat flux of 147kW/m$^2$. This provides an upper limit to the heat flux from the burner, as in practice much of the available energy will be lost to the surroundings.

A more detailed approach uses semi-empirical correlations for the heat flux from literature. These are typically expressed in terms of the dimensionless heat transfer coefficient called the Nusselt number.
\[ Nu = \frac{h_l}{k} \]  

(6.18)

where \( k \) is the thermal conductivity of the fluid and \( l \) is a characteristic length scale.

There are different expressions for different flow types. Holman [6.4] gives an expression for heat transfer in the case of forced convection normal to a flat plate as follows:

\[ Nu = 0.228 \text{Re}^{0.731} \]  

(6.19)

where \( \text{Re} \) is the Reynolds number,

\[ \text{Re} = \frac{ul}{v} \]  

(6.20)

Taking \( u=3\text{m/s} \) (typical velocity from CFD simulation) and \( l=0.4\text{m} \) (size of the test panel) gives a heat transfer coefficient of \( h_c=11.7\text{W/m}^2\text{K} \), which is in reasonable agreement with the plate average predicted heat transfer coefficients from CFD modelling, Table 6.3.

### 6.1.2.C Analysis of Radiation.

The following sections present a theoretical analysis of the radiation emitted from the burner surface and the burner flame. Values for the radiative heat flux are compared with values for the convective heat flux given above.

- **Radiation from the Burner Surface**

  The radiation emitted from the burner surface (see Figure 6.15) was analysed using a simple theoretical approach in which the burner surface and test plate are treated as infinite parallel grey plates. This approach assumes that the length of the plates is much greater than the distance between them. For the ISO 2685 test [6.1] the burner surface had a diameter of 0.184m and the distance between the burner surface and plate was 0.076m. Thus for the case of the ISO 2685 test [6.1], this theoretical approach is likely to over-estimate the radiative heat transfer.
According to Holman [6.4], the heat flux between two infinite parallel grey plates is,

$$ q = \frac{\sigma(T_1^4 - T_2^4)}{1/\varepsilon_1 + 1/\varepsilon_2 - 1} $$

(6.21)

where $\sigma$ is the Stefan-Boltzman coefficient, $T_1$ and $T_2$ are the temperatures of the parallel plates and $\varepsilon_1$ and $\varepsilon_2$ are their emissivities.

Here $T_1$ and $\varepsilon_1$ represent the temperature and emissivity of the burner surface and $T_2$ and $\varepsilon_2$ represent the temperature and emissivity of a test plate. The temperature and emissivity of a test plate ($T_2$ and $\varepsilon_2$) will depend on the test material and will vary during a test. The maximum radiative heat transfer will occur for a plate with a low temperature and a high emissivity. Therefore, to determine the maximum radiative heat transfer we set $T_2 = 300$K and $\varepsilon_2 = 1$.

The temperature and emissivity of the burner surface ($T_1$ and $\varepsilon_1$) will vary less than the temperature and emissivity of a test plate. However, there was some uncertainty concerning these values. The temperature of the burner surface will be greater than 300K (i.e. room temperature) but less than 800K (the temperature at which steel would exhibit a dull red glow, according to Drysdale [6.10]). Similarly, the emissivity of the burner surface will be greater than 0.2 (typical value for steel [6.10]) but less than 0.8 (typical value for steel covered with soot layer [6.10]). The radiative heat transfer was calculated for a range of values and the results are shown in Table 6.4.

<table>
<thead>
<tr>
<th>$T_2$ (K)</th>
<th>$\varepsilon_2$</th>
<th>0.2</th>
<th>0.4</th>
<th>0.8</th>
</tr>
</thead>
<tbody>
<tr>
<td>400</td>
<td>0.2</td>
<td>0.4</td>
<td></td>
<td></td>
</tr>
<tr>
<td>600</td>
<td>1.4</td>
<td>2.8</td>
<td>5.5</td>
<td></td>
</tr>
<tr>
<td>800</td>
<td>4.6</td>
<td>9.1</td>
<td>18.2</td>
<td></td>
</tr>
</tbody>
</table>

Table 6.4 Radiative heat transfer in kW/m² between infinite parallel plates for $T_1 = 300$K and $\varepsilon_1 = 1.0$, with different values of $T_2$ and $\varepsilon_2$.

The results in Table 6.4 illustrate the possible range in magnitude of the radiative heat transfer from the burner surface to a test plate. Possible values range from
Fire Load Measurement and Calibration.

0.2kW/m² up to 18.2kW/m². However, in reality the temperature of the burner surface was unlikely to exceed 600K and there was very little soot formation on the burner surface such that the emissivity was probably nearer 0.2. At these values the radiative heat transfer from the burner surface to a test plate would be 1.4kW/m² at most. This is sufficiently less than the values obtained for the convective heat transfer that radiative heat transfer from the burner surface might be neglected.

- Radiation from Burner Flame

The radiation emitted from a flame is harder to quantify than radiation emitted from a solid surface. Furthermore, the radiation emitted by the ISO 2685 [6.1] burner flame exhibited considerable variation depending on, for example, which material was being tested. When the test material was aluminium (and when the calibration measurements were performed) the flames tended to be non-luminous and emitted much less radiation than when composite materials were tested, which tended to give luminous flames.

The following analysis is limited to non-luminous flames and is therefore only valid for the tests on aluminium or when the calibration measurements were performed. Radiative heat transfer from hot non-luminous gases is described by Drysdale [6.10] and Siegel and Howell [6.11]. In particular, Drysdale [6.10] describes a simplified approach originally proposed by Hottel and Egbert [6.12] and it is this approach that was used here.

The radiation emitted by a hot non-luminous gas depends on its emissivity and this in turn is strongly dependent on the gas composition. In particular, homonuclear diatomic molecules including nitrogen and oxygen are transparent to thermal radiation, whilst heteronuclear molecules including carbon dioxide and water vapour absorb and emit radiation at certain discrete wavelengths. The ISO 2685 [6.1] burner flame consists of a mixture of these gases and therefore, its emissivity will exhibit a complex wavelength dependency [6.10].

Hottel and Egbert [6.12] developed an approach by which an equivalent grey body gas emissivity can be calculated. The approach requires as inputs a flame shape factor, a flame diameter, flame composition and flame temperature. For the ISO 2685 burner
[6.1] flame a suitable flame shape factor is 0.43 and the flame diameter may be taken as equal to the burner casing diameter, which is 0.184m. The flame shape factor and flame diameter are multiplied together to calculate a mean beam length of 0.079m.

The flame composition may be obtained by assuming complete combustion of a stoichiometric propane, air mixture. This gives a CO$_2$ concentration of 0.127v/v and a H$_2$O vapour concentration of 0.169v/v. Following the methodology of Hottel and Egbert [6.12] these are expressed as partial pressures of 0.127atm and 0.169atm, respectively. The partial pressures are then multiplied by the beam length and used in conjunction with the flame temperature to determine equivalent grey gas emissivities. Graphical representations of the relationship provide a convenient way to do this, as proposed by Drysdale [6.10]. Gas emissivities of 0.018 and 0.010 were obtained for carbon dioxide and water vapour, respectively. Finally, these were added together to obtain a combined hot gas emissivity of 0.028.

Once the emissivity had been determined the radiation emitted by the flame could be calculated using,

$$q = \varepsilon \sigma T_f^4 \quad (6.22)$$

and for a flame temperature of $T_f$=2400K, this gives a maximum radiative heat output of 52.7kW/m$^2$. This represents the upper limit on the radiative heat transfer in the case where the plate would absorb all radiation directed at it. It is comparable in magnitude to the value obtained for convective heat transfer from the CFD modelling.

6.1.2.D CFD Modelling With Radiation.

This section describes an analysis of the relative magnitudes of the convective and radiative heat transfer associated with the ISO 2685 [6.1] fuselage burnthrough test using CFD modelling. Following the theoretical analysis presented in section 6.1.2.C, this work neglects radiative heat transfer from the burner surface and focuses only on the radiative heat transfer from the burner flame.
It is likely that radiative heat transfer from the burner flame has a number of effects including lowering the flame temperature below the adiabatic limit. However, the main aim of this analysis is to examine radiative heat transfer to a test plate. The modelling approach is described and results are presented.

- **Modelling approach**

  CFD modelling of the radiative heat transfer from the ISO 2685 [6.1] burner flame was carried out using **ANSYS CFX 12.1**. The radiation was modelled using the Discrete Transfer radiation model coupled with a Gray spectral model, which assumes that all radiation quantities are uniform throughout the spectrum. The CFD modelling approach used is suitable for non-luminous flames, but unsuitable for luminous flames. Therefore, this analysis is valid for the tests on aluminium or when the calibration measurements were performed, but not for the tests on composite materials as mentioned in section 6.1.2.C.

  The Discrete Transfer radiation model requires values for the absorption coefficient of the different gas components. The absorption coefficient of oxygen and nitrogen was set to zero, whilst the absorption coefficient of carbon dioxide was set to 0.12 and the absorption coefficient of water vapour was set to 0.27 [6.10].

  Simulations were performed with two constant but different values for the plate emissivity, namely 1.0 and 0.2. The former allowed simulation of the maximum radiative heat transfer (which could then be compared with the theoretical value described in section 6.1.2.C), whilst the latter value is a more realistic emissivity for aluminium [6.10].

- **Results and Discussion**

  CFD predictions of the ISO 2685 burner [6.1] flame composition, temperature and absorption coefficient are presented in Figures 6.21, 6.22. Figure 6.21 shows that the carbon dioxide and water vapour concentration predictions were in reasonable agreement with the theoretical values given in section 6.1.2.C. In particular, the predicted carbon dioxide concentration was about 0.12v/v and the predicted water vapour concentration was about 0.16v/v.
The temperature predictions shown in Figure 6.22 display a similar distribution to the temperature predictions from simulations without radiation, section 6.1.2.A. When radiation was simulated, the predicted flame temperature was slightly lower than the adiabatic flame temperature because of radiative heat losses. The absorption coefficient shown in Figure 6.22 varied from nearly zero in the ambient air to approximately 0.05 in the flame, where combustion product carbon dioxide and water vapour are present.

![Figure 6.21 Centre plane carbon dioxide (left) and water vapour (right) concentration predictions from simulation sim036; coarse mesh and 1-step chemistry](image)

CFD model predictions of the radiative heat flux to test plates with emissivities of 1.0 and 0.2 are shown in Figure 6.23. For a plate with an emissivity of 1.0 the maximum predicted radiative heat flux was about 28kW/m², however for a plate with an emissivity of 0.2 the maximum predicted radiative heat flux was about 8kW/m². As discussed above the latter emissivity of 0.2 is more realistic for an aluminium plate [6.10].
The predictions of radiative heat flux for a plate with an emissivity of 1.0 from CFD modelling were much less than the theoretical prediction of 52.7kW/m\(^2\) calculated in section 6.1.2.C. The reasons for this were not determined. It is possible that the theoretical prediction is not valid for the geometry of the ISO 2685 burner [6.1] flame. The discrepancy does however serve to highlight the uncertainties in predictions of radiative heat transfer.

Figure 6.22  Centre plane temperature contours (left) and absorption coefficient (right) predictions from simulation sim036; coarse mesh and 1-step chemistry
Figure 6.23  Contour plots of radiative heat flux predictions on a test plate from simulations with plate emissivities of 1.0 (top) and 0.2 (bottom)

6.1.2.E  ISO 2685 Discussion

A comprehensive examination of the heat transfer in the ISO 2685 Standard [6.1] fuselage burnthrough tests has been carried out using CFD modelling and theoretical analysis. At first radiation was neglected and predictions of the convective heat flux were compared against the available experimental data and theoretical expressions,
for which reasonable agreement was achieved. Later radiation was included in the CFD simulations, although with only limited success. There remains some uncertainty in the radiative heat flux predictions, which did not agree with theoretical expressions.

- **Conclusions**

The main conclusions from the work are as follows:

1) The main characteristics of the ISO 2685 propane burner [6.1] flame, including the location of the flow stagnation region, were well predicted by the CFD model.

2) CFD predictions of the flame temperatures were in good agreement with the calculated adiabatic flame temperature.

3) The wall heat transfer coefficient was sensitive to the mesh resolution. However, predictions obtained using the highest resolution mesh were in good agreement with empirical correlations.

4) The maximum wall heat transfer coefficient corresponds to a maximum convective heat flux of 54.4kW/m². This is less than the heat flux measurements of 70 to 100kW/m² reported by Abu-Talib et al. [6.2].

5) The radiative heat flux is strongly dependent on the material emissivity and flame luminosity. Predictions of the radiative heat flux from a non-luminous flame ranged from 8 to 28kW/m², although higher values are possible for a luminous flame.
6.2 References


[6.9] http://www.gaseq.co.uk/


CHAPTER 7   Experimental Work.

7.1 Samples Manufacturing.

In this section the fabrication of a variety of flat panel is described. From these panels coupon and samples have been manufactured which were tested under different thermal/fire conditions in order to collect experimental data for the development, setup and calibration of the numerical models. Issues concerning the materials used, the facilities and the manufacturing processes applied will be also covered for an overall insight of the subject.

The selection of materials has been done according to the current Aeronautics Industry trends. The properties of Aluminum, Hybrid GLARE type and Carbon Composites were investigated as their specifications differ with temperature and high strain situations. Additionally, fire performance of these materials was also defined in order to develop and calibrate tools for new design strategies of the metallic/composite/hybrid aerostructures.
Aluminum is the backbone material of the current aircraft design and its fire performance definition is the control of all other options.

GLARE is the nowadays breakthrough of aircraft concepts and design technologies. The “GLAss Reinforced” Fiber Metal Laminate is composed of several thin layers of metal (usually aluminum) interspersed with layers of glass fiber “prepreg”, bonded together with an epoxy matrix. The unidirectional prepreg layers may be aligned in different directions to suit the desired stress conditions. Although GLARE is a composite material, its mechanical properties and fabrication are very similar to bulk aluminum metal sheets. Its major advantages are better damage tolerance, better corrosion resistance, significantly increased fire resistance and lower weight. Consequently, an aerostructure properly designed for GLARE could be significantly lighter and less complex compared to a conventional aluminum structure.

The increasing trend for the usage of Carbon Fiber Reinforced Plastics, CFRP’s, is something that should be taken into consideration. The higher strength and stiffness properties, the higher torsional stiffness, the improved corrosion resistance and the significant weight reduction have established the CFRPs as an ideal candidate solution for aerostructures. A very good example verifying the overall effectiveness of CFRP’s is that Boeing 787 currently in flight certification state consists over 50% by composite materials. Despite the improved mechanical properties CFRP’s appear to have tricky fire behavior depending on the polymer matrix system used, so extensive research is performed into that area too. This later fact is the issue analyzed into this dissertation. In the following sections more specific details of the materials and manufacturing processes employed are being presented, while in Table 7.1 the fire test matrix is presented.
Table 7.1: Fire Test Matrix.

<table>
<thead>
<tr>
<th>Type of Specimen</th>
<th>Number Of Test</th>
<th>Description of Test</th>
<th>Test Performed By</th>
<th>Length (mm)</th>
<th>Width (mm)</th>
<th>ALUMINIUM</th>
<th>GLARE 3</th>
<th>CFRP (Type 2)</th>
<th>CFRP (Type 5)</th>
<th>CFRP (Type 2)</th>
<th>CFRP (Type 4)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>F2.0</td>
<td>Coupon Test</td>
<td>INMAGNET - FMSW</td>
<td>1100</td>
<td>900</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>F2.1</td>
<td>Flat panel with acoustic insulation and liner attached</td>
<td>RNA</td>
<td>1000</td>
<td>900</td>
<td>5</td>
<td>5</td>
<td>5</td>
<td>5</td>
<td>5</td>
<td>5</td>
</tr>
<tr>
<td></td>
<td>F2.2</td>
<td>Flat panel</td>
<td>Response to flame</td>
<td>1000</td>
<td>9000</td>
<td>2</td>
<td>2</td>
<td>2</td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
</tbody>
</table>
7.1.1 Aluminum Samples

The type of aluminum sheets used was AL 2024-T3, having nominal thickness of 1mm. Laser cutting machines and hand operated shears were employed to fabricate the different dimensional combinations of the required samples.

7.1.1.A AL 2024-T3

Here the general properties of the aluminum sheets used are presented as received by the supplier Table 7.2.

Material Notes:

General 2024 characteristics and uses (from Alcoa): Good machinability and surface finish capabilities. A high strength material of adequate workability. It has largely superseded 2017 for structural applications.

Uses:

Aircraft fittings, gears and shafts, bolts, clock parts, computer parts, couplings, fuse parts, hydraulic valve bodies, missile parts, munitions, nuts, pistons, rectifier parts, worm gears, fastening devices, veterinary and orthopedic equipment, structures.

<table>
<thead>
<tr>
<th>Properties</th>
<th>Value</th>
<th>Min</th>
<th>Max</th>
<th>Comment</th>
</tr>
</thead>
<tbody>
<tr>
<td>Density, g/cc</td>
<td>2.78</td>
<td>--</td>
<td>--</td>
<td>AA; Typical</td>
</tr>
<tr>
<td>Mechanical</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Hardness, Brinnell</td>
<td>120</td>
<td>--</td>
<td>--</td>
<td>AA; Typical; 500 g load; 10 mm ball</td>
</tr>
<tr>
<td>Hardness, Knoop</td>
<td>150</td>
<td>--</td>
<td>--</td>
<td>Converted from Brinnell Hardness Value</td>
</tr>
<tr>
<td>Hardness, Rockwell A</td>
<td>46.8</td>
<td>--</td>
<td>--</td>
<td>Converted from Brinnell Hardness Value</td>
</tr>
<tr>
<td>Hardness, Rockwell B</td>
<td>75</td>
<td>--</td>
<td>--</td>
<td>Converted from Brinnell Hardness Value</td>
</tr>
<tr>
<td>Hardness, Vickers</td>
<td>137</td>
<td>--</td>
<td>--</td>
<td>Converted from Brinnell Hardness Value</td>
</tr>
<tr>
<td>Ultimate Tensile Strength, MPa</td>
<td>483</td>
<td>--</td>
<td>--</td>
<td>AA; Typical</td>
</tr>
<tr>
<td>Tensile Yield Strength, MPa</td>
<td>345</td>
<td>--</td>
<td>--</td>
<td>AA; Typical</td>
</tr>
<tr>
<td>Elongation at Break, %</td>
<td>18</td>
<td>--</td>
<td>--</td>
<td>AA; Typical; 1/16 in. (1.6 mm) Thickness</td>
</tr>
</tbody>
</table>
Experimental Work.

Table 7.2: Aluminum 2024-T3 Properties.

<table>
<thead>
<tr>
<th>Modulus of Elasticity, GPa</th>
<th>73.1</th>
<th>--</th>
<th>--</th>
<th>AA; Typical; Average of tension and compression. Compression modulus is about 2% greater than tensile modulus.</th>
</tr>
</thead>
<tbody>
<tr>
<td>Notched Tensile Strength, MPa</td>
<td>379</td>
<td>--</td>
<td>--</td>
<td>2.5 cm width x 0.16 cm thick side-notched specimen, Kt = 17.</td>
</tr>
<tr>
<td>Ultimate Bearing Strength, MPa</td>
<td>855</td>
<td>--</td>
<td>--</td>
<td>Edge distance/pin diameter = 2.0</td>
</tr>
<tr>
<td>Bearing Yield Strength, MPa</td>
<td>524</td>
<td>--</td>
<td>--</td>
<td>Edge distance/pin diameter = 2.0</td>
</tr>
<tr>
<td>Poisson’s Ratio</td>
<td>0.33</td>
<td>--</td>
<td>--</td>
<td></td>
</tr>
<tr>
<td>Fatigue Strength, MPa</td>
<td>138</td>
<td>--</td>
<td>--</td>
<td>AA; 500,000,000 cycles completely reversed stress; RR Moore machine/specimen</td>
</tr>
<tr>
<td>Machinability, %</td>
<td>70</td>
<td>--</td>
<td>--</td>
<td>0-100 Scale of Aluminum Alloys</td>
</tr>
<tr>
<td>Shear Modulus, GPa</td>
<td>28</td>
<td>--</td>
<td>--</td>
<td></td>
</tr>
<tr>
<td>Shear Strength, MPa</td>
<td>283</td>
<td>--</td>
<td>--</td>
<td>AA; Typical</td>
</tr>
<tr>
<td>Properties</td>
<td>Value</td>
<td>Min</td>
<td>Max</td>
<td>Comment</td>
</tr>
<tr>
<td>Electrical</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Electrical Resistivity, ohm-cm</td>
<td>5.82e-006</td>
<td>--</td>
<td>--</td>
<td>AA; Typical at 68°F</td>
</tr>
<tr>
<td>Thermal</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>CTE, linear 68°F, μm/m°C</td>
<td>23.2</td>
<td>--</td>
<td>--</td>
<td>AA; Typical; Average over 68-212°F range.</td>
</tr>
<tr>
<td>CTE, linear 250°C, μm/m°C</td>
<td>24.7</td>
<td>--</td>
<td>--</td>
<td>Average over the range 20-300°C</td>
</tr>
<tr>
<td>Specific Heat Capacity, J/g°C</td>
<td>0.875</td>
<td>--</td>
<td>--</td>
<td></td>
</tr>
<tr>
<td>Thermal Conductivity, W/m-K</td>
<td>121</td>
<td>--</td>
<td>--</td>
<td>AA; Typical at 77°F</td>
</tr>
<tr>
<td>Melting Point, °C</td>
<td>--</td>
<td>502</td>
<td>638</td>
<td>AA; Typical range based on typical composition for wrought products 1/4 inch thickness or greater. Eutectic melting is not eliminated by homogenization.</td>
</tr>
<tr>
<td>Solidus, °C</td>
<td>502</td>
<td>--</td>
<td>--</td>
<td>AA; Typical</td>
</tr>
<tr>
<td>Liquidus, °C</td>
<td>638</td>
<td>--</td>
<td>--</td>
<td>AA; Typical</td>
</tr>
</tbody>
</table>

Table 7.2: Aluminum 2024-T3 Properties.

### 7.1.2 GLARE Samples

The type of Glare that was manufactured was Glare-3 5/4. The Aluminum layers were 2024-T3 with 0.4mm thickness and the tape that was utilized for the fabrication of the GLARE was a FM® 94–S2 glass unidirectional tape, by Cytec Engineered Materials’ industry, which is a standard 120°C curing toughened epoxy prepreg.
GLARE is a Fiber Metal laminate made of aluminum layers in the range of 0.3-0.5mm thick and glass fiber reinforced composite layers in the range of 0.25-0.5mm thick. The polymer in the prepreg is an epoxy resin. The metal and composite layers are assembled and bonded together. The metal layers are pre-treated before the bonding process and there can be virtually unlimited combinations of metal and composite layers. For the fire testing type GLARE-3 5/4 was fabricated. This means that the stacking sequence was:

- 5 layers of Aluminum (0.4mm thick)
- 4 layers of Glass Unidirectional tape with $0^\circ$ and $90^\circ$ direction (0.125mm thick).

The Aluminum sheets before being cured with the prepreg according to datasheet were subjected to chromic acid anodizing without sealing (I.A.W. MIL-A-8625 “Anodic Coatings for Aluminum and Aluminum Alloys” and AMS 2470 ‘’Anodic Treatment of Aluminum Alloys (Chromic acid Process)’’). Following the anodizing process the aluminum sheets were coated with a corrosion inhibiting primer BR127.
Experimental Work.

which offers superior durability and resistance to hostile environments when used in conjunction with chromic and phosphoric acid anodisation surface pre-treatments.

Figure 7.3 Preparation and drying of the Aluminum “Layers”.

7.1.2.A UD-glass tape

The tape that was utilized for the fabrication of the GLARE was a FM® 94–S2 glass unidirectional tape, by Cytec Engineered Materials’ industry, which is a standard 120°C curing toughened epoxy prepreg.

Features & Benefits:

Aerospace approved 120°C curing toughened epoxy resin system, good balance of shear and peel properties for 80°C service applications, high strain to failure resin
system results in improved FML fatigue and impact performance for primary structure applications, cure flexibility allows a wide range of processing conditions.

<table>
<thead>
<tr>
<th>Typical physical properties</th>
</tr>
</thead>
<tbody>
<tr>
<td>Prepreg mass per unit area:</td>
</tr>
<tr>
<td>Fibre mass per unit area:</td>
</tr>
<tr>
<td>Resin content:</td>
</tr>
<tr>
<td>Prepreg volatile content:</td>
</tr>
<tr>
<td>Resin flow:</td>
</tr>
<tr>
<td>Prepreg cured density:</td>
</tr>
<tr>
<td>Prepreg cured ply thickness:</td>
</tr>
<tr>
<td>Prepreg cured dry Tg:</td>
</tr>
<tr>
<td>Shelf life:</td>
</tr>
<tr>
<td>Shop life:</td>
</tr>
<tr>
<td>Operating temperatures – dry:</td>
</tr>
<tr>
<td>Operating temperatures – wet:</td>
</tr>
<tr>
<td>Cure temperature range:</td>
</tr>
</tbody>
</table>

Table 7.3: Glass Tape Physical Properties.

<table>
<thead>
<tr>
<th>Mechanical properties</th>
<th>Test temperature (°C)</th>
<th>Test results</th>
</tr>
</thead>
<tbody>
<tr>
<td>Single lap shear strength, MPa -55</td>
<td>-55</td>
<td>20</td>
</tr>
<tr>
<td>EN 2243-1 23</td>
<td>23</td>
<td>22</td>
</tr>
<tr>
<td>0° fibre direction 80</td>
<td>80</td>
<td>18</td>
</tr>
<tr>
<td>120</td>
<td>12</td>
<td></td>
</tr>
<tr>
<td>Floating roller peel strength, N/25 mm -55</td>
<td>23</td>
<td>150</td>
</tr>
<tr>
<td>EN 2243-2 90° fibre direction 80</td>
<td>80</td>
<td>1200</td>
</tr>
<tr>
<td>0° Tensile strength, MPa -55</td>
<td>1850</td>
<td></td>
</tr>
<tr>
<td>EN 2747</td>
<td>23</td>
<td>1700</td>
</tr>
<tr>
<td>80</td>
<td>1200</td>
<td></td>
</tr>
<tr>
<td>0° Tensile modulus, GPa -55</td>
<td>50</td>
<td></td>
</tr>
<tr>
<td>EN 2747</td>
<td>23</td>
<td>50</td>
</tr>
<tr>
<td>80</td>
<td>52</td>
<td></td>
</tr>
<tr>
<td>±45°C Tensile strength, MPa -55</td>
<td>310</td>
<td></td>
</tr>
<tr>
<td>EN 6031</td>
<td>23</td>
<td>200</td>
</tr>
<tr>
<td>80</td>
<td>110</td>
<td></td>
</tr>
</tbody>
</table>

Table 7.4: Glass Tape Mechanical Properties.
7.1.3 CFRP Samples

Two sample campaigns were performed, the first one manufactured in the framework of the VULCAN EU-STREP Project and tested with the ISO2685 Burner and the second one to support the lab-scale AML burner.

7.1.3.A VULCAN Project Samples

The CFRP panels were fabricated by different partners according to the Type presented in the fire test matrix of Table 7.1. Those types were:

- **Israel Aircraft Industries Ltd**
  - Matrix: Epocast 50-A1
  - Reinforcement: Plain Weave carbon fabric Style 3K-70-P by HEXCEL.
  - Flame Retardants (FRs): Hardener 9816
  - CFRP Type 2

- **Institute für Verbundwerkstoffe GmbH**
  - Matrix: Epocast 54-A
  - Reinforcement: Plain Weave carbon fabric Style 3K-70-P by HEXCEL.
  - Flame Retardants (FRs): Hardener 9816
  - CFRP Type 5

  - Matrix: Epoxy Resin System LY 564 + HY 2954
  - Reinforcement: Quasi UD carbon fabric type G0947
  - Flame Retardants (FRs): Ammonium Polyphosphate Exolit AP 423 by Clariant
  - CFRP Type 3

  - Matrix: Epoxy Resin System LY 564 + HY 2954
  - Reinforcement: Quasi UD carbon fabric type G0947
  - Flame Retardants (FRs): Melamine Polyphosphate Melapur 200 by Ciba
  - CFRP Type 4

![Figure 7.5 CFRP Specimens.](image-url)
**CFRP Type 2 & Type 5**

The CFRP panels fabricated by the IAI utilized the hand lay up process using the materials that were mentioned previously. The datasheets details of the used materials are presented. Manufacturing comprised from the following steps:

Wet lay-up technique of flat panels – Consolidation

Wet lay-up technique of flat panels:

The CFRP plates for sample preparation were manufactured by hand wet lay-up, Figure 7.6. Therefore the carbon fiber woven fabric, Style 3K, was cut in plies accordingly to the dimensions specified from the fire test matrix. For each CFRP panel, 10 fabric plies were wet laminated with the same amount of matrix material. The impregnated layers were stacked together and finally the plates were packed in a vacuum tight plastic bag and prepared for the consolidation step.

![Figure 7.6 Wet Lay Up Manufacturing Process.](image)

Consolidation inside an oven:

The recommended curing program in accordance to the data sheet was applied (24h RT, 2h-80°C). The curing at the first 24 h at RT was performed under vacuum bag. After this first stage the bag was removed and the plate was placed in the oven to finalize the cure cycle. The vacuum in the bag was maintained less than 0.03mbar.

- The fiber weight content of the 10 ply and ~2 mm thick panels were about 50%.
- The mixing ratio was 100 parts by weight of Epocast 50-A1 resin and 14 parts by weight of hardener 9816. Both components were mixed for several minutes to insure a complete uniform mixture.
Experimental Work.

**CFRP Type 3 & Type 4**
For these types of flat panels the already mentioned materials were used and the panels manufactured by IVW. The Flame Retardants, FR used were selected based on extended pre-tests. The manufacturing process included the following steps:

Modification of the matrix – Wet layup technique of flat panels – Consolidation

**Modification of the matrix:**
The dispersion of the FRs in the epoxy took place in a dissolver device (Dispermat AE, VMA Getzmann GmbH). The dissolver introduces high shear forces by a high speed rotating disc. The compound is stirred in a vacuum container to avoid air inclusion. The vortex flow achieved by the geometry of the disc leads to continuous mixing of the compound. During processing temperature, rotational frequency, energy input and time are controlled for reproducible test conditions.

The FRs were pre-dried and afterwards pre-mixed with the resin LY564 using an overhead stirrer. For personal safety this was done under an extractor hood. For degassing the mixture was then put into the dissolver device, which is equipped with a vacuum chamber.

Dispersion took place for 20 minutes and with a rotational speed of the dissolver disc of 3500 rpm. After dispersing, the mixture was degassed and cooled down to around 40°C. Then the appropriate amount of hardener was added and the whole mixture was stirred again in an overhead stirrer for 15 minutes.

Figure 7.7 Dissolver device and schematic diagram of the operation mode.
Wet lay-up technique of flat panels:

The CFRP panels for sample preparation were manufactured by hand wet lay-up. Therefore the carbon fiber woven fabric, type twill 2/2, was cut in plies of 480x480mm. For each CFRP panel 12 fabric plies were wet laminated with the same amount of matrix material. The impregnated layers were put on each other. The panels were then packed inside a vacuum tight plastic bag and prepared for the consolidation step inside the autoclave.

Consolidation inside the autoclave:

Inside the autoclave chamber 20bar pressure was applied, the vacuum in the bag was maintained less than 0.03mbar. The recommended curing program in accordance to the data sheet was applied (1h-80°C, 8h-140°C). Modified CFRP panels with FR doped resin have been produced as well as neat ones.

![Figure 7.8 Autoclave equipment used and typical cure cycle applied.](image)

The specimens produced showed a high quality with a very low void content as seen in the micrograph of the grinding surface in Figure 7.9. The fiber volume content of the 12 ply and ~2.1 mm thick panels were about 50%.

![Figure 7.9 Micrograph of the grinding surface.](image)
Experimental Work.

**CFRP Type 2 & 5**

Reinforcement

**Textile Reinforcement for High Performance Composites**

<table>
<thead>
<tr>
<th>DEFINITION / DESCRIPTION</th>
<th>Type de fils / Type of yarns</th>
<th>Masse nominale / Nominal weight</th>
<th>Armure / Weave style</th>
<th>Poudrage / Powdering</th>
<th>Traitement / Finish</th>
</tr>
</thead>
<tbody>
<tr>
<td>Chaîne / Warp : T300B 3K 40B C4</td>
<td>Trame / Weft : T300B 3K 40B C4</td>
<td>193 g/m² / 5.69 oz/sqy²</td>
<td>TAFFETAS / PLAIN</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Largeur standard</th>
<th>Standard width</th>
</tr>
</thead>
<tbody>
<tr>
<td>1070 mm</td>
<td>42 in</td>
</tr>
</tbody>
</table>

**CARACTERISTIQUES / CHARACTERISTICS**

<table>
<thead>
<tr>
<th>Contexture nominale / Nominal construction</th>
<th>Chaîne / Warp : 4.9 fils-yarns/cm</th>
<th>Trame / Weft : 4.8 coups-picks/cm</th>
</tr>
</thead>
<tbody>
<tr>
<td>Répartition en masse / Weight distribution</td>
<td>Chaîne / Warp : 51 %</td>
<td>Trame / Weft : 49 %</td>
</tr>
<tr>
<td>Epaisseur / Thickness (*)</td>
<td>0.20 mm</td>
<td></td>
</tr>
</tbody>
</table>

**PROPRIETE MECANIQUES SUR STRATIFIE* / MECHANICAL PROPERTIES ON LAMINATE**

Mise en oeuvre (80 min à 120°C, vide 0.85 bar, pression 3 bars) / Cure cycle (60 min at 120°C, vacuum 0.85 bar, pressure 3 bars)

<table>
<thead>
<tr>
<th>Traction chaîne / Warp tensile</th>
<th>Flexion chaîne / Warp flexural</th>
<th>C.I.L. chaîne / Warp / L.S.S.</th>
</tr>
</thead>
<tbody>
<tr>
<td>Contrainte / Stress (Mpa)</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Module / Modulus (Gpa)</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Normes / Standards</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Table 7.5: Carbon Fabric 3K-70-P Properties.
Matrix

**Epocast 50-A1 Resin – Hardener 9816**

Epocast 50-A1 resin/hardener 9816 epoxy laminating system is an unfilled, solvent-free, easy-to-handle material for the manufacture or repair of composite structures as well as for filament winding. Epocast 50-A1 resin/Hardener 9816 epoxy laminating system is self-extinguishing; qualification to BMS 8-201, Type 3 is pending.

<table>
<thead>
<tr>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>Colour</td>
<td>Amber</td>
<td>Amber</td>
<td>Amber</td>
<td>Visual</td>
</tr>
<tr>
<td>Density, g/cc</td>
<td>1.21</td>
<td>1.05</td>
<td>1.18</td>
<td>ASTM-D-792</td>
</tr>
<tr>
<td>Viscosity, at 25°C, cP</td>
<td>7,770</td>
<td>250</td>
<td>2,400</td>
<td>ASTM-D-2196</td>
</tr>
<tr>
<td>Gel time, at 25°C, 100gms, minutes</td>
<td>-</td>
<td>-</td>
<td>65</td>
<td>ASTM-D-2471</td>
</tr>
<tr>
<td>Shelf life, at 25°C, unopened, months</td>
<td>12</td>
<td>12</td>
<td>-</td>
<td>-</td>
</tr>
</tbody>
</table>

Table 7.7: Typical physical properties.

<table>
<thead>
<tr>
<th>Test</th>
<th>Results</th>
<th>Test Method</th>
</tr>
</thead>
<tbody>
<tr>
<td>Compressive Strength, Ksi (MPa) at 77°F (25°C)</td>
<td>45.86 (316) 49.00 (338) 46.24 (319)</td>
<td>ASTM-D-790</td>
</tr>
<tr>
<td>Compressive Modulus, Msi (MPa)</td>
<td>3.52 (24.3) 3.13 (21.6) 4.16 (28.7)</td>
<td>ASTM-D-790</td>
</tr>
<tr>
<td>Flammability, 60 second vertical test, self extinguishing time, sec</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>Flammability, 60 second vertical test, drip extinguishing time, sec</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>Flammability, 60 second vertical test, burn length, in (cm)</td>
<td>&lt;6 (&lt;15)</td>
<td>&lt;6 (&lt;15)</td>
</tr>
</tbody>
</table>

Table 7.8: Typical cured properties.
Experimental Work.

Matrix

Epocast 54-A

Epocast 54-A epoxy laminating system is an unfilled, solvent-free, easy to handle product. It is self extinguishing and well suited for the manufacture and repair of composite structures. In additions to laminating applications, the Epocast 54-A epoxy system can be used for filament winding.

<table>
<thead>
<tr>
<th>Property</th>
<th>54-A Resin</th>
<th>54-A/B System</th>
<th>Test Method</th>
</tr>
</thead>
<tbody>
<tr>
<td>Color</td>
<td>Light Yellow</td>
<td>Amber</td>
<td>Visual</td>
</tr>
<tr>
<td>Density, g/cc.</td>
<td>1.19</td>
<td>1.05</td>
<td>ASTM-D-792</td>
</tr>
<tr>
<td>Viscosity, cP @ 77°F (25°C)</td>
<td>9,000</td>
<td>400</td>
<td>ASTM-D-2196</td>
</tr>
<tr>
<td>Gel time, 100 gms at 77°F (25°C), min.</td>
<td>--</td>
<td>--</td>
<td>ASTM-D-2471</td>
</tr>
<tr>
<td>Shelf life at 77°F (25°C), unopened, months</td>
<td>12</td>
<td>12</td>
<td>--</td>
</tr>
</tbody>
</table>

Table 7.10: Typical physical properties.

<table>
<thead>
<tr>
<th>Test</th>
<th>Results</th>
<th>Test Method</th>
</tr>
</thead>
<tbody>
<tr>
<td>Compressive Strength, 12-ply 7781 glass cloth, at 77°F (25°C), psi (MPa)</td>
<td>49,010 (338)</td>
<td>ASTM D695</td>
</tr>
<tr>
<td>Compressive Modulus, 12-ply 7781 glass cloth, at 77°F (25°C), Ksi (MPa)</td>
<td>3,399 (23,442)</td>
<td>ASTM D695</td>
</tr>
<tr>
<td>Flammability, 2-ply 7781 glass cloth, 60 sec., vertical</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Self Extinguish Time, sec.</td>
<td>0</td>
<td>FAR 25.853A</td>
</tr>
<tr>
<td>Drip Extinguish Time, sec.</td>
<td>0</td>
<td></td>
</tr>
<tr>
<td>Burn Length, in. (cm)</td>
<td>3.5 (8.89)</td>
<td></td>
</tr>
</tbody>
</table>

Table 7.11: Typical cured properties.
Reinforcement

**HS Carbon Fabric G0947 1040 (Textile Reinforcement for High Performance Composites)**

<table>
<thead>
<tr>
<th>Nominal Weight</th>
<th>Weight Distribution</th>
<th>Type of Yarns</th>
<th>Weave Style</th>
<th>Thickness</th>
</tr>
</thead>
<tbody>
<tr>
<td>160 gr/m²²</td>
<td>Warp: 97%</td>
<td>Warp: 3K HR</td>
<td>Plain</td>
<td>0.16mm</td>
</tr>
<tr>
<td></td>
<td>Weft: 3%</td>
<td>Weft: EC5 5.5 X 2</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Table 7.13: Carbon fabric properties.

Matrix

**Hot Curing Epoxy System Based on Araldite LY 564 / Aradur 2954**

Applications / Qualifications:

Due to excellent handling behaviour the system is suitable for various production processes. It combines low viscosity with long pot life at elevated temperatures. The cured system shows excellent mechanical, dynamic and thermal (hot/wet) properties and good chemical resistance.

Mechanical properties

**Tensile Test**

<table>
<thead>
<tr>
<th></th>
<th>Cure</th>
<th>1h 80°C+</th>
<th>8h 140°C+</th>
<th>1h 80°C+</th>
<th>4h 160°C+</th>
</tr>
</thead>
<tbody>
<tr>
<td>Tensile Strength</td>
<td>[MPa]</td>
<td>71-77</td>
<td>78-82</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Elongation at Tensile Strength</td>
<td>[%]</td>
<td>4.5-5.5</td>
<td>6.3-7.3</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Ultimate Strength</td>
<td>[MPa]</td>
<td>71-77</td>
<td>78-82</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Ultimate elongation</td>
<td>[%]</td>
<td>4.5-5.5</td>
<td>6.3-7.3</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Tensile Modulus</td>
<td>[MPa]</td>
<td>2550-2650</td>
<td>2450-2550</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

**Flexural Test**

<table>
<thead>
<tr>
<th></th>
<th>Cure</th>
<th>1h 80°C+</th>
<th>8h 140°C+</th>
</tr>
</thead>
<tbody>
<tr>
<td>Flexural Strength</td>
<td>[MPa]</td>
<td>120-124</td>
<td></td>
</tr>
<tr>
<td>Ultimate elongation</td>
<td>[%]</td>
<td>6.5-7.5</td>
<td></td>
</tr>
<tr>
<td>Flexural Modulus</td>
<td>[MPa]</td>
<td>2600-2800</td>
<td></td>
</tr>
</tbody>
</table>

Fracture Properties Bend Notch Test
<table>
<thead>
<tr>
<th>Property</th>
<th>Unit</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Fracture Toughness $K_{IC}$</td>
<td>[MPa/m]</td>
<td>0.69-0.76</td>
</tr>
<tr>
<td>Fracture Energy $G_{IC}$</td>
<td>[J/m$^2$]</td>
<td>149-181</td>
</tr>
</tbody>
</table>

**Coefficient of linear thermal expansion**

<table>
<thead>
<tr>
<th>Cure</th>
<th>1h 80°C+ 8h 140°C</th>
</tr>
</thead>
<tbody>
<tr>
<td>Mean value up to 80°C</td>
<td>$10^{-6}$/K</td>
</tr>
<tr>
<td>[μ]</td>
<td>0.35</td>
</tr>
</tbody>
</table>

**Interlaminar Shear Strength**

<table>
<thead>
<tr>
<th>Cure</th>
<th>1h 80°C+ 8h 140°C</th>
</tr>
</thead>
<tbody>
<tr>
<td>Shear Strength</td>
<td>[MPa]</td>
</tr>
<tr>
<td>[MPa]</td>
<td>59-63</td>
</tr>
</tbody>
</table>

**Transverse Tensile Test**

<table>
<thead>
<tr>
<th>Cure</th>
<th>1h 80°C+ 8h 140°C</th>
</tr>
</thead>
<tbody>
<tr>
<td>Tensile Strength</td>
<td>[MPa]</td>
</tr>
<tr>
<td>Tensile Strain</td>
<td>[%]</td>
</tr>
<tr>
<td>Elastic Modulus</td>
<td>[MPa]</td>
</tr>
<tr>
<td>[MPa]</td>
<td>43-49</td>
</tr>
<tr>
<td>[%]</td>
<td>1.8-2.0</td>
</tr>
<tr>
<td>[MPa]</td>
<td>15700-15900</td>
</tr>
</tbody>
</table>

**Transverse Compressive Test**

<table>
<thead>
<tr>
<th>Cure</th>
<th>1h 80°C+ 8h 140°C</th>
</tr>
</thead>
<tbody>
<tr>
<td>Compressive Strength</td>
<td>[MPa]</td>
</tr>
<tr>
<td>Elastic Modulus</td>
<td>[MPa]</td>
</tr>
<tr>
<td>[MPa]</td>
<td>110-140</td>
</tr>
<tr>
<td>[MPa]</td>
<td>15500-16000</td>
</tr>
</tbody>
</table>

**Torsional Test**

<table>
<thead>
<tr>
<th>Cure</th>
<th>1h 80°C+ 8h 140°C</th>
</tr>
</thead>
<tbody>
<tr>
<td>Shear Strength</td>
<td>[MPa]</td>
</tr>
<tr>
<td>Shear Modulus</td>
<td>[MPa]</td>
</tr>
<tr>
<td>[MPa]</td>
<td>60-64</td>
</tr>
<tr>
<td>[MPa]</td>
<td>5000-6000</td>
</tr>
</tbody>
</table>

Table 7.14: Typical cured properties.
Flame Retardants

CFRP Type 3

Exolit AP 423

Product Description

Exolit AP 423 is a fine particle ammonium polyphosphate produced by a special method. The crystal modification in phase II. The product is largely insoluble in water and completely insoluble in organic solvents. It is a white powder, non-hygrosopic and non-flammable. Exolit AP423 differs from most other commercial products in the following ways:

1) Greatly reduced solubility in water
2) Lower viscosity in aqueous suspensions
3) Low acid number

Applications:

1) The application of Exolit on wood and plastics enables these materials to qualify for building material class B1
2) Steel structures can meet the requirements of fire resistance classes specified in DIN, BS and ASTM
3) Imparts a good flame retardant effect to adhesives and sealants when it is incorporated into the base formulation at the rate of 10-20%

Physical Properties

<table>
<thead>
<tr>
<th>Property</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Density</td>
<td>1.9gr/cm³</td>
</tr>
<tr>
<td>Bulk Density</td>
<td>approx. 0.7gr/cm³</td>
</tr>
<tr>
<td>Viscosity</td>
<td>max. 100mPa*s</td>
</tr>
<tr>
<td>pH Value</td>
<td>5.0-7.5</td>
</tr>
<tr>
<td>Solubility in water (20°C)</td>
<td>max. 1.0% (w/w)</td>
</tr>
<tr>
<td>Decomposition Temperature</td>
<td>&gt;275°C</td>
</tr>
<tr>
<td>Average Particle Size</td>
<td>approx. 8μm</td>
</tr>
</tbody>
</table>

Table 7.15: Exolit AP423 properties.
Experimental Work.

7.1.3.B AML Samples

The AML Burner samples were manufactured using the Huntsman Araldite LY564-Aradur 2654 system, which is a typical aerospace grade epoxy system. This system would be referred from now on as **LY-Ref**. Two modified batches of this reference system were also produced. As additives were used the Exolit Ammonium Polyphosphate AP423 and Baytubes Multiwall Carbon Nanotubes. The first batch contained 20% w/w AP423 fine powder, and the second batch contained 20% w/w AP423 and 2% w/w MWCNT. These two batches would be referred as **AP423** and **MWCNT** respectively. CFRP panels were manufactured using the hand wet lay-up and vacuum bag technique, from the three investigated matrix materials having the TENAX 80gr/m² UD carbon fiber reinforcement. Samples 100x100mm were cut and instrumented with thermocouples on the rear face for temperature monitoring.
7.2 Cone Calorimetry Testing and Data.

7.2.1 Introduction
In the aerospace field a number of tests are employed for both research and certification purposes. These include a variety of calorimeters and other standard tests such as the Bunsen burner protocol for seating materials. The standard calorimeter normally used for aerospace materials is the OSU version standardized as ASTM E906 (1983) modified by the FAA (1990). However, recently Quintiere et al have suggested that a wider range of tests needs to be performed to fully specify the fire performance of an aerospace composite. These include cone calorimetry carried out at a range of incident heat fluxes, thermo-gravimetric analysis, measurements of thermal conductivity and specific heat and differential scanning calorimetry.

Since no OSU apparatus was available, it was decided that small scale testing should comprise a combination of cone calorimetry. The cone calorimetry, carried out to standard ISO 5660 (1993).

7.2.2 Sample details
Table 7.17 indicates the type and number of samples tested, their manufacturer and the dates of reception and testing.
Experimental Work.

Table 7.17: Sample details.

<table>
<thead>
<tr>
<th>Material number</th>
<th>Sample type</th>
<th>Number of samples</th>
<th>Supplier</th>
<th>Dates, mm/yy</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>CFRP 1+ insulation foil</td>
<td>6</td>
<td>IAI</td>
<td>09/07</td>
</tr>
<tr>
<td>2</td>
<td>Insulation foil</td>
<td>15</td>
<td>IAI</td>
<td>12/07</td>
</tr>
<tr>
<td>3</td>
<td>Unhardened CFRP 2</td>
<td>6</td>
<td>IVW</td>
<td>10/07</td>
</tr>
<tr>
<td>4</td>
<td>CFRP 2+ 20% AP423</td>
<td>6</td>
<td>IVW</td>
<td>10/07</td>
</tr>
<tr>
<td>5</td>
<td>200</td>
<td>6</td>
<td>IVW</td>
<td>10/07</td>
</tr>
<tr>
<td>6</td>
<td>GLARE (3 ply)</td>
<td>5</td>
<td>HAI</td>
<td>11/07</td>
</tr>
</tbody>
</table>

The range of materials supplied for testing included a Fibre-metal composite GLARE-3 5/4. Two CFRP materials were supplied both epoxy based. The material supplied by IVW (CFRP2) was supplied in various forms including a base case unhardened state and also with two fire retardant additives – an ammonium phosphate-based retardant and a melamine material. IAI also supplied some layers of thermo-acoustic insulation for lining the composites along with their own composite (CFRP1). This also had a metallic skin attached.

7.2.3 Test principle

The cone calorimeter comprises a conical heater producing a carefully controlled and constant heat flux up to 100kW/m² on the 100 mm square test sample. The sample can be positioned either horizontally or vertically and is mounted on a load cell which allows its mass to be constantly monitored. Any combustion products emitted from the sample are ignited by an electrical spark igniter. The combustion gases are fed down a duct where they are analysed using a number of sensors to allow calculation of the heat release rate and production rates of smoke and toxic gasses. Observations are also made of the time to ignition and extinction of any flaming.

A schematic diagram of the cone apparatus is shown in Figure 7.11 and a close up photograph of the cone, sample mounting and load cell arrangement included as Figure 2.
Figure 7.11 Schematic of the Cone calorimeter installation.

Figure 7.13 shows a close-up photograph of the sample holder. The sample is contained within a sample pan which in turn is mounted on a load cell. The bottom of the sample is wrapped in aluminum foil to avoid leaking of the material in the rig, and the top is protected by a frame to avoid swelling - Figure 7.13.

In conducting the test the sample the heat flux is first stabilized and the value measured using a calibrated heat flux meter. This is shown in Figure 7.12. In this figure a piece of mineral board shields the sample holder and load cell while the heat flux meter, shown, is used to check the output heat flux. A shutter is then slid into position to prevent sample exposure whilst it is undergoing installation. The length of exposure can be varied.
Experimental Work.

Figure 7.12  Schematic of the Cone calorimeter installation.

Figure 7.13  Insulation foil sample in its casing.

Figures 7.14, 7.15, 7.16 show the progress of cone calorimeter test. Figure 7.14 shows the start of a test. The sample holder can be clearly seen with the test sample beginning to melt. Figure 7.15 shows a later stage in the test. The sample material is beginning to emit volatile vapours and the spark igniter is seen operating in the centre of the apparatus. In Figure 7.16 the ignition temperature has been reached and the sample has ignited. Here no flame was used to ignite the sample - the combustion gasses ignited initially and burned back to the sample. Figure 7.17 shows the remains
of the sample at the end of the test. In this case the test exposure was 10 minutes at 35kW/m². This sample self-extinguished when all the combustible material had been consumed. This was the case for all the samples tested.

Figure 7.14  Placement of the sample in its container.

Figure 7.15  Melting of insulation material and electrical spark to ignite gasses.
7.2.4 Test programme

All tests were carried out at a single radiant heat flux of 35 kW/m². Each test was nominally set at 10 minutes duration and most were repeated three times to check for repeatability and reproducibility.

The CFRP samples from IAI with their insulation layer were used as orientation samples to determine the test duration and the heat flux. The test duration was determined at 10 minutes and the heat flux used was 35 kW/m². Table 7.18 summarises the tests carried out. The programme comprised all materials alone and then in combination with the insulating blanket. In these latter tests the blanket was positioned uppermost towards the flame.
One supplementary test was executed for comparison reasons: this was with the IAI composite alone, CFRP1. This allows a comparison of the raw skin material results and the results for the materials with an insulation layer to be made.

<table>
<thead>
<tr>
<th>Test designation</th>
<th>Sample type</th>
<th>Number of samples</th>
<th>Source of sampler</th>
</tr>
</thead>
<tbody>
<tr>
<td>1, 2, 3</td>
<td>CFRP 1 + insulation foil</td>
<td>3</td>
<td>IAI</td>
</tr>
<tr>
<td>4, 5, 6</td>
<td>Insulation foil only</td>
<td>3</td>
<td>IAI</td>
</tr>
<tr>
<td>7, 8, 9</td>
<td>CFRP pure</td>
<td>3</td>
<td>IVW</td>
</tr>
<tr>
<td></td>
<td>CFRP + 18% Melapur</td>
<td></td>
<td></td>
</tr>
<tr>
<td>10, 11, 12</td>
<td>200</td>
<td>3</td>
<td>IVW</td>
</tr>
<tr>
<td>13, 14, 15</td>
<td>CFRP + 20% AP423</td>
<td>3</td>
<td>IVW</td>
</tr>
<tr>
<td>16, 17, 18</td>
<td>Glare (3ply)</td>
<td>3</td>
<td>HAI</td>
</tr>
<tr>
<td>19</td>
<td>CFRP pure + foil</td>
<td>1</td>
<td>IVW</td>
</tr>
<tr>
<td></td>
<td>CFRP + 20% AP423 +</td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>insulation foil</td>
<td></td>
<td></td>
</tr>
<tr>
<td>21</td>
<td>CFRP + 18% Melapur +</td>
<td>1</td>
<td>IVW</td>
</tr>
<tr>
<td></td>
<td>insulation foil</td>
<td></td>
<td></td>
</tr>
<tr>
<td>20</td>
<td>Glare (3 ply) + insulation foil</td>
<td>1</td>
<td>HAI</td>
</tr>
<tr>
<td>22</td>
<td>CFRP without insulation</td>
<td>1</td>
<td>IAI</td>
</tr>
<tr>
<td>23, 24</td>
<td></td>
<td>2</td>
<td></td>
</tr>
</tbody>
</table>

Table 7.18: Details of tests carried out.

### 7.2.5 Test results

For each test the data given are the standard cone calorimeter outputs. These comprise time series plots of the mass loss as measured from the load cell, the rate of heat release (and cumulative total heat release) as calculated using measurements of oxygen concentration and the flow rate in the exhaust and the smoke production rate (and cumulative total smoke production) calculated from the measured obscuration and flow rate in the exhaust duct.

In tabulated form the control software also extracts and outputs the time to ignition, the average, peak and total heat release, mass loss and smoke production. Some observations of sample behaviour can also be made and noted.
Experimental Work.

ISO 5660, CONE TEST

Test Specimen 1
Reference of the specimen: CFRPI + foil on fire exposed side
Test date: 2007-11-19
Wire grid used: Yes
Orientation: Horizontal
Fixing: Standard
Heat Flux Level (kW/m²): 35

Observations:
| Ignition (s) | 460 |
| Test duration (s) | 900 |
| early termination of the test at the demand of the sponsor after extinction |     |
| Flaking | Yes |
| Melting | No |
| Swelling | No |
| Carbonisation | Yes |

Rate of Heat Release (RHR) & Total Heat Release (THR):
- Average RHR (kW/m²) at end test: 10.98
- Average RHR (kW/m²) at 300s after ignition: 13.87
- Average RHR (kW/m²) at 600s after ignition: 15.09
- Max. RHR (kW/m²): 300.57
- Time of appearance (s): 375

THR (MJ/m²): 36.80

Mass Loss:
- Initial Mass (g): 49
- Total mass produced (g): 19.84
- Total mass loss (g/m²): 2490.45
- Mass Loss Rate (g/m²/s): 15.48

Average Effective Heat of combustion (MJ/kg): 15.25

Rate of Smoke Production (RSP) & Total Smoke Production (TSP):
- TSP (m³/m²): 3312.32
- Average RSP at end test (m³/m²/s): 3.66
- Max. RSP (m³/m²/s): 38.87
- Time of appearance (s): 345

MAHRE (kW):
- Time of appearance (s): 650
ISO 5660. CONE TEST

Test Specimen 2
Reference of the specimen: CFRP1 + foil on fire exposed side
Test date: 2007-11-19
Wire grid used: Yes
Orientation: Horizontal
Fixing: Standard
Heat Flux Level (kW/m²): 35

Observations:
Ignition (s): 425
Test duration (s): 510
Early termination of the test at the demand of the sponsor after extinction: Yes
Flashing: No
Melting: No
Swelling: No
Carbonisation: Yes

Rate of Heat Release (RHR) & Total Heat Release (THR):
Average RHR (kW/m²) at end test: 31.53
Average RHR (kW/m²) at 180s after ignition: 101.15
Average RHR (kW/m²) at 300s after ignition: 101.42
Max RHR (kW/m²): 371.55
Time of appearance (s): 560
THR (MJ/m²): 29.84

Mass loss:
Initial Mass (g): 48
Total mass pyrolysed (g): 18.20
Total mass loss (g/m²): 2275.46
Mass Loss Rate (g/m²/s): 35.89

Average Effective Heat of combustion (MJ/kg): 14.12

Rate of Smoke Production (RSP) & Total Smoke Production (TSP):
TSP (m³/m²): 3349.71
Average RSP at end test (m³/m²/s): 3.57
Max RSP (m³/m²/s): 36.50
Time of appearance (s): 540

MAHRL (kW):
Time of appearance (s): 655
ISO 5660, CONE TEST

Test Specimen 3
Reference of the specimen: CFRP1 + foil on fire exposed side
Test date: 2007-11-19
Wire grid used: Yes
Orientation: Horizontal
Fixing: Standard
Heat Flux Level (kW/m²): 35

<table>
<thead>
<tr>
<th>Observations</th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>Ignition (s)</td>
<td>468</td>
</tr>
<tr>
<td>Test duration (s)</td>
<td>900</td>
</tr>
<tr>
<td>early termination of the test at the demand of the sponsor after ignition</td>
<td></td>
</tr>
<tr>
<td>Flashing</td>
<td>Yes</td>
</tr>
<tr>
<td>Melting</td>
<td>No</td>
</tr>
<tr>
<td>Swelling</td>
<td>No</td>
</tr>
<tr>
<td>Carbonisation</td>
<td>Yes</td>
</tr>
</tbody>
</table>

Rate of Heat Release (RHR) & Total Heat Release (THR):

- Average RHR (kW/m²) at end test: 33.05
- Average RHR (kW/m²) at 180s after ignition: 33.04
- Average RHR (kW/m²) at 300s after ignition: 96.77
- Max. RHR (kW/m²): 183.55
- Time of appearance (s): 615
- THR (kJ/m²): 28.76

Mass loss:

- Initial Mass (g): 48
- Total mass produced (g): 16.03
- Total mass loss (g/mol): 213.61
- Mass Loss Rate (g/mol s): 10.39

Average Effective Heat of combustion (MJ/kg): 16.12

Rate of Smoke Production (RSP) & Total Smoke Production (TSP):

- TSP (m²/m³): 34.00
- Average RSP at end test (m²/m³): 4.40
- Max. RSP (m²/m³): 19.78
- Time of appearance (s): 605
- MAHR (kJ): 40.56
  - Time of appearance (s): 600
ISO 5660, CONE TEST

Test Specimen 4
Reference of the specimen: reference insulation
Test date: 2008-02-25
Wire grid used: Yes
Orientation: Horizontal
Fixing: Standard
Heat Flux Level (kW/m²): 35

Observations:
- Ignition (s): 30
- Test duration (s): 900
- Early termination of the test at the demand of the sponsor after extinction
- Flashing: Yes
- Melting: No
- Swelling: No
- Carbonisation: Yes
- Rate of Heat Release (RHR) & Total Heat Release (THR):
  - Average RHR (kW/m²) at end test: 12.74
  - Average RHR (kW/m²) at 180s after ignition: 51.33
  - Average RHR (kW/m²) at 300s after ignition: 32.40
  - Max. RHR (kW/m²): 17.54
  - Time of appearance (s): 75
  - THR (MJ/m²): 11.67
- Mass loss:
  - Initial Mass (g): 13
  - Total mass produced (g): 8.90
  - Total mass loss (g/m²): 1124.12
  - Mass Loss Rate (g/m².s): 4.23
- Average Effective Heat of Combustion (MJ/kg):
  - 10.48
- Rate of Smoke Production (RSP) & Total Smoke Production (TSP):
  - TSP (m³/m²): 1044.13
  - Average RSP at end test (m³/m²): 1.16
  - Max. RSP (m³/m²): 25.97
  - Time of appearance (s): 60
  - MAHRE (kW):
    - 30.67
    - Time of appearance (s): 105
**Experimental Work.**

---

**ISO 5660, CONE TEST**

**Test Specimen**
- Reference of the specimen: reference insulation
- Test date: 2008-02-25
- Wire grid used: Yes
- Orientation: Horizontal
- Fixing: Standard
- Heat Flux Level (kW/m²): 35

**Observations:**

<table>
<thead>
<tr>
<th>Observation</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Ignition (s)</td>
<td>30</td>
</tr>
<tr>
<td>Test duration (s)</td>
<td>900</td>
</tr>
<tr>
<td>Early termination of the test at the demand of the sponsor after extinction</td>
<td>Yes</td>
</tr>
<tr>
<td>Flashing</td>
<td>Yes</td>
</tr>
<tr>
<td>Melting</td>
<td>No</td>
</tr>
<tr>
<td>Swelling</td>
<td>No</td>
</tr>
<tr>
<td>Carbonisation</td>
<td>Yes</td>
</tr>
<tr>
<td>Rate of Heat Release (RHR) &amp; Total Heat Release (THR)</td>
<td></td>
</tr>
<tr>
<td>Average RHR (kW/m²) at end test</td>
<td>11.51</td>
</tr>
<tr>
<td>Average RHR (kW/m²) at 180s after ignition</td>
<td>66.98</td>
</tr>
<tr>
<td>Average RHR (kW/m²) at 300s after ignition</td>
<td>37.88</td>
</tr>
<tr>
<td>Max. RHR (kW/m²)</td>
<td>176.45</td>
</tr>
<tr>
<td>Time of appearance (s)</td>
<td>100</td>
</tr>
<tr>
<td>THR (MJ/m²)</td>
<td>12.19</td>
</tr>
<tr>
<td>Mass loss</td>
<td></td>
</tr>
<tr>
<td>Initial Mass (g)</td>
<td>12</td>
</tr>
<tr>
<td>Total mass pyrolysed (g)</td>
<td>8.48</td>
</tr>
<tr>
<td>Total mass loss (g)</td>
<td>1657.33</td>
</tr>
<tr>
<td>Mass Loss Rate (g/m²)</td>
<td>4.68</td>
</tr>
<tr>
<td>Average Effective Heat of combustion (MJ/kg)</td>
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<tr>
<td>Rate of Smoke Production (RSP) &amp; Total Smoke Production (TSP)</td>
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<tr>
<td>TSP (m³/m²)</td>
<td>0.08</td>
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<tr>
<td>Average RSP at end test (m³/m²)</td>
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<td>Max. RSP (m³/m²)</td>
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<td>70</td>
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<tr>
<td>MAHFRE (kW)</td>
<td>85.32</td>
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<td>Time of appearance (s)</td>
<td>120</td>
</tr>
</tbody>
</table>

---

**Heat Release Rate (kW/m²) and Total Heat Release (MJ/m²)**

**Mass Loss (g/m²)**

**Rate of Smoke Production (m³/m²) and Total Smoke Production (m³/m²)**
**ISO 5660, CONE TEST**

- Test Specimen 6
- Reference of the specimen: reference insulation
- Test date: 2008-02-25
- Wire grid used: Yes
- Orientation: Horizontal
- Fixing: Standard
- Heat Flux Level (kW/m²): 35

**Observations:**
- Ignition (s): 25
- Test duration (s): 890
- Early termination of the test at the demand of the sponsor after extinction
- Flashing: No
- Melting: No
- Swelling: No
- Carbonization: Yes

**Rate of Heat Release (RHR) & Total Heat Release (THR):**
- Average RHR (kW/m²) at end test: 13.43
- Average RHR (kW/m²) at 180s after ignition: 66.16
- Average RHR (kW/m²) at 300s after ignition: 40.85
- Max. RHR (kW/m²): 177.18
- Time of appearance (s): 95
- THR (MJ/m²): 13.74

**Mass loss:**
- Initial Mass (g): 13
- Total mass pyrolysed (g): 4.69
- Total mass loss (g/m²): 108.10
- Mass Loss Rate (g/m²s): 3.65

**Average Effective Heat of combustion (MJ/kg):** 12.77

**Rate of Smoke Production (RSP) & Total Smoke Production (TSP):**
- TSP (m³/m²): 955.47
- Average RSP at end test (m³/m²): 1.04
- Max. RSP (m³/m²): 10.17
- Time of appearance (s): 70
- MAHRE (kW): 86.92
- Time of appearance (s): 125
Experimental Work.

ISO 5600, CONE TEST

Test Specimen:
Reference of the specimen: CFRP2 only
Test date: 2008-02-25
Wire grid used: Yes
Orientation: Horizontal
Exposure: Standard
Heat Flux Level (kW/m²): 35

Observations:
Ignition (s) 95
Test duration (s) 900
early termination of the test at the demand of the sponsor after extinction
Flashing No
Melting No
Swelling No
Carbonisation Yes

Rate of Heat Release (RHR) & Total Heat Release (THR):
Average RHR (kW/m²) at end test 35.15
Average RHR (kW/m²) at 180s after ignition 119.06
Average RHR (kW/m²) at 300s after ignition 71.77
Max. RHR (kW/m²) 356.91
Time of appearance (s) 180

THR (MJ/m²) 22.65

Mass loss:
Initial Mass (g) 28
Total mass produced (g) 9.50
Total mass loss (g/m²) 1187.84

Mass Loss Rate (g/m².s) 5.70

Average Effective Heat of combustion (MJ/kg) 23.44

Rate of Smoke Production (RSP) & Total Smoke Production (TSP):
TSP (m³/m²) 899.12
Average RSP at end test (m³/m²) 1.00
Max. RSP (m³/m²) 14.21
Time of appearance (s) 155
MAHR (kW) 95.05
Time of appearance (s) 205
ISO 5660, CONE TEST

Test Specimen 8
Reference of the specimen: CFRP2 only
Test date: 2008-02-29
Wire grid used: Yes
Orientation: Horizontal
Fixing: Standard
Heat Flux Level (kW/m²): 35

Observations:
- Ignition (s): 85
- Test duration (s): 900
- Early termination of the test at the demand of the sponsor after extension
- Flashing: No
- Melting: No
- Swelling: No
- Carbonisation: Yes

Rate of Heat Release (RHR) & Total Heat Release (THR):
- Average RHR (kW/m²) at end test: 30.56
- Average RHR (kW/m²) at 150s after ignition: 88.25
- Average RHR (kW/m²) at 300s after ignition: 82.85
- Max. RHR (kW/m²): 294.39
- Time of appearance (s): 160

THR (MJ/m²): 275.4

Mass loss:
- Initial Mass (g): 28
- Total mass pyrolysed (g): 10.60
- Total mass loss (g/m²): 1240.59
- Mass Loss Rate (g/m²-s): 6.95
- Average Effective Heat of combustion (MJ/kg): 26.19

Rate of Smoke Production (RSP) & Total Smoke Production (TSP):
- TSP (m²/m²): 1078.08
- Average RSP at end test (m²/m²): 1.20
- Max. RSP (m²/m²): 12.65
- Time of appearance (s): 140
- MAHRR (kW): 103.15
- Time of appearance (s): 215
Experimental Work.

ISO 5660, CONE TEST

Test Specimen 0
Reference of the specimen: CFRP 2 only
Test date: 2008-09-25
Wire grid used: Yes
Orienterion: Horizontal
Fixing: Standard
Heat Flux Level (kW/m²): 35

Observations:
Ignition (s) 80
Test duration (s) 900
Early termination of the test at the demand of the sponsor after extinction
Flashover No
Melting No
Swelling No
Carbonisation Yes

Rate of Heat Release (RHR) & Total Heat Release (THR):
Average RHR (kW/m²) at end test 27.04
Average RHR (kW/m²) at 180s after ignition 23.95
Average RHR (kW/m²) at 360s after ignition 38.22
Max. RHR (kW/m²) 344.48
Time of appearance (s) 165
THR (MJ/m²) 26.66

Mass loss
Initial Mass (g) 28
Total mass pyrolysed (g) 10.96
Total mass loss (g/m²) 1370.22
Mass Loss Rate (g/m²/s) 10.28
Average Effective Heat of combustion (MJ/kg) 19.93

Rate of Smoke Production (RSP) & Total Smoke Production (TSP)
TSP (m³/m²) 1224.13
Average RSP at end test (m³/m²) 1.36
Max. RSP (m³/m²) 14.41
Time of appearance (s) 169
MAHRF (L/W) 113.16
Time of appearance (s) 215
ISO 5660, CONE TEST

Test Specimen 10
Reference of the specimen: CFRP2 – 16 % Melamur 200
Test date: 2008-02-23
Wire grid used: Yes
Orientation: Horizontal
Fixing: Standard
Heat Flux Level [kW/m²]: 35

Observations:
- Ignition (s): 60
- Test duration (s): 920
- early termination of the test at the demand of the sponsor after extraction
- Flashing: No
- Melting: No
- Swelling: No
- Carbonisation: Yes

Rate of Heat Release (RHR) & Total Heat Release (THR):
- Average RHR (kW/m²) at end test: 24.24
- Average RHR (kW/m²) at 180s after ignition: 114.30
- Average RHR (kW/m²) at 300s after ignition: 70.08
- Max. RHR (kW/m²): 272.34
- Time of appearance (s): 135
- THR (MJ/m²): 22.31

Mass:
- Initial Mass (g): 28
- Total mass pyrolysed (g): 8.19
- Total mass loss (g/m²): 1023.93
- Mass Loss Rate (g/m²/s): 9.11

Average Effective Heat of combustion (MJ/kg):
- Average RSP at end test (MJ/kg): 23.17

Rate of Smoke Production (RSP) & Total Smoke Production (TSP):
- TSP (m³/m²): 798.50
- Average RSP at end test (m³/m²): 0.87
- Max. RSP (m³/m²): 10.69

Time of appearance (s): 140

MAHRE (kW):
- Time of appearance (s): 195
Experimental Work.

ISO 5660, CONE TEST

Test Specimen 11
Reference of the specimen: CFRP 2 + 16% Metapora 200
Test date: 2008-02-25
Wire grid used: Yes
Orientation: Horizontal
Fixing: Standard
Heat Flux Level (kW/m²): 35

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<tr>
<th>Observations:</th>
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<tr>
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<td>Test duration (s)</td>
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<td>Melting</td>
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<td>Swelling</td>
<td>No</td>
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<tr>
<td>Carbonisation</td>
<td>Yes</td>
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</table>

Rate of Heat Release (RHR) & Total Heat Release (THR):
- Average RHR (kW/m²) at end test: 30.67
- Average RHR (kW/m²) at 180s after ignition: 131.31
- Average RHR (kW/m²) at 300s after ignition: 93.13
- Max. RHR (kW/m²): 388.91
- Time of appearance (s): 155

THR (MJ/m²): 28.14

Mass loss:
- Initial Mass (g): 32
- Total mass pyrolysed (g): 11.62
- Total mass loss (g/m²): 1452.42
- Mass Loss Rate (g/m²/s): 10.04

Average Effective Heat of combustion (MJ/kg):
- Average RHR at end test (MJ/kg): 39.78

Rate of Smoke Production (RSP) & Total Smoke Production (TSP):
- TSP (m³/m²): 1275.47
- Average RSP at end test (m³/m²/s): 1.42
- Max. RSP (m³/m²/s): 12.12
- Time of appearance (s): 150
- MAHR (kW): 109.53
- Time of appearance (s): 225
ISO 5660, CONE TEST

Test Specimen I2
Reference of the specimen: CFRP2 + 16 % Melapur 200
Test date: 2008-02-25
Wire grid used: Yes
Orientation: Horizontal
Fixing: Standard
Heat Flux Level (kW/m²): 35

<table>
<thead>
<tr>
<th>Observations:</th>
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<td>Melting</td>
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<td>Swelling</td>
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<tr>
<td>Carbonisation</td>
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</table>

Rate of Heat Release (RHR) & Total Heat Release (THR):
- Average RHR (kW/m²) at end test: 34.82
- Average RHR (kW/m²) at 180s after ignition: 105.31
- Average RHR (kW/m²) at 300s after ignition: 105.42
- Max. RHR (kW/m²): 292.62

- Time of appearance (s): 185
- THR (MJ/m²): 32.22
- Mass loss
  - Initial Mass (g): 33
  - Total mass pyrolyzed (g): 12.00
  - Total mass loss (g/m²): 350.10
  - Mass Loss Rate (mg/m²/s): 11.41

Rate of Smoke Production (RSP) & Total Smoke Production (TSP):
- TSP (m³/m²): 1428.39
- Average RSP at end test (m³/m²): 1.54
- Max. RSP (m³/m²/s): 13.33
- Time of appearance (s): 150
- MAHRE (kW): 117.55
- Time of appearance (s): 235
Experimental Work.

ISO 5660, CONE TEST

Test Specimen 13
Reference of the specimen: CFRP2 – 20% AP423

Test date: 2008-02-23.
Wire grid used: Yes
Orientation: Horizontal
Fixing: Standard
Heat Flux Level (kW/m²): 35

<table>
<thead>
<tr>
<th>Observations</th>
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<td>Meltting</td>
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<td>Swelling</td>
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<tr>
<td>Carbonisation</td>
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<td>Rate of Heat Release (KHR) &amp; Total Heat Release (THR):</td>
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<tr>
<td>Average KHR (kW/m²) at end test</td>
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<td>Average KHR (kW/m²) at 180s after ignition</td>
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<td>Average KHR (kW/m²) at 300s after ignition</td>
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<td>Max KHR (kW/m²)</td>
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<td>THR (MJ/m²)</td>
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<td>Initial Mass (g)</td>
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<td>Total mass pyrolysed (g)</td>
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<td>Total mass loss (g/m²)</td>
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<td>Mass Loss Rate (gm/s)</td>
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<td>Average Effective Heat of combustion (MJ/kg)</td>
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<tr>
<td>TSP (m³/m²)</td>
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<td>Average RSP at end test (m³/m²/s)</td>
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<tr>
<td>Max RSP (m³/m²/s)</td>
<td>12.36</td>
</tr>
<tr>
<td>Time of appearance (s)</td>
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<tr>
<td>MAHRE (kJ)</td>
<td>80.53</td>
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<tr>
<td>Time of appearance (s)</td>
<td>195</td>
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</tbody>
</table>
ISO 5660, CONE TEST

Test Specimen 14
Reference of the specimen: CFRP2 + 30% AP423
Test date: 2008-02-25
Wire grid used: Yes
Orientation: Horizontal
Fixing: Standard
Heat Flux Level (kW/m²): 35

Observations:
Ignition (s) 80
Test duration (s) 905
Early termination of the test at the demand of the sponsor after extraction
Flashing No
Melting No
Swelling No
Carbonisation Yes

Rate of Heat Release (RHR) & Total Heat Release (THR)
- Average RHR (kW/m²) at end test 12.80
- Average RHR (kW/m²) at 180s after ignition 8.74
- Average RHR (kW/m²) at 300s after ignition 6.50
- Max. RHR (kW/m²) 258.16
- Time of appearance (s) 130
- THR (MJ/m²) 15.57

Mass loss
- Initial Mass (g) 27
- Total mass pyrolysed (g) 7.25
- Total mass loss (g/m²) 966.28
- Mass Loss Rate (g/m²/s) 8.39
- Average Effective Heat of combustion (MJ/kg) 13.88

Rate of Smoke Production (RSP) & Total Smoke Production (TSP)
- TSP (m³/m²) 81.96
- Average RSP at end test (m³/m²/s) 0.99
- Max. RSP (m³/m²) 14.19
- Time of appearance (s) 123
- MAHRE (L/W) 82.96
- Time of appearance (s) 170
Experimental Work.

ISO 5660, CONE TEST

Test Specimen 15
Reference of the specimen: CFRP2 + 20% AP423
Test date: 2008-02-25
Wire grid used: Yes
Orientation: Horizontal
Fixing: Standard
Heat Flux Level (kW/m²): 35

Observations:
- Ignition (s): 5
- Test duration (s): 885
- Early termination of the test at the demand of the sponsor after extension
- Flashing: No
- Melted: No
- Swelling: No
- Carbonization: Yes

Rate of Heat Release (RHR) & Total Heat Release (THR):
- Average RHR (kW/m²) at end test: 16.53
- Average RHR (kW/m²) at 180s after ignition: 96.29
- Average RHR (kW/m²) at 300s after ignition: 66.80
- Max. RHR (kW/m²): 290.71
- Time of appearance (s): 145
- THR (MJ/m²): 17.27

Mass loss:
- Initial Mass (g): 78
- Total mass produced (g): 7.34
- Total mass loss (g/m²): 0.18.10
- Mass Loss Rate (g/m²/s): 8.20

Average Effective Heat of combustion (MJ/kg):
- 18.17

Rate of Smoke Production (RSP) & Total Smoke Production (TSP):
- TSP (m³/m²): 87.81
- Average RSP at end test (m³/m²/s): 0.92
- Max. RSP (m³/m²/s): 14.40
- Time of appearance (s): 140
- MAHRE (kW):
  - Ignition (s): 80.23
  - Time of appearance (s): 190
ISO 5660. CONE TEST

Test Specimen 16
Reference of the specimen: 3 ply GLARE
Test date: 2008-02-25
Wire grid used: Yes
Orientation: Horizontal
Fixing: Standard
Heat Flux Level (kW/m²): 35

Observations:
- Ignition (s): 373
- Test duration (s): 905
- Early termination of the test at the demand of the sponsor after extinction

<table>
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<th>Value</th>
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<td>Total mass pyrolysed (g)</td>
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<tr>
<td>Mass Loss Rate (g/m²/s)</td>
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<tr>
<td>Average Effective Heat of combustion (MJ/kg)</td>
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<td>Rate of Smoke Production (RSP) &amp; Total Smoke Production (TSP)</td>
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<tr>
<td>TSP (m³/m²)</td>
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<td>Average RSP at end test (m³/m²/s)</td>
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<td>Max RSP (kJ/m²)</td>
<td>12.31</td>
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<td>357</td>
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</tbody>
</table>
Experimental Work.

ISO 5660, CONE TEST

Test Specimen 17
Reference of the specimen: 3-ply GLARE
Test date: 2008-02-26
Wire grid used: Yes
Orientation: Horizontal
Fixing: Standard
Heat Flux Level (kW/m²): 35

Observations:
- Ignition (s): 550
- Test duration (s): 900
- Early termination of the test at the demand of the sponsor after combustion:
  - Flashing: No
  - Melting: No
  - Sooting: No
- Carbonisation: Yes

Rate of Heat Release (RHR) & Total Heat Release (THR):
- Average RHR (kW/m²) at end test: 4.75
- Average RHR (kW/m²) at 100s after ignition: 24.88
- Average RHR (kW/m²) at 300s after ignition: 14.35
- Max. RHR (kW/m²): 73.78
- Time of appearance (s): 320

THR (MB/m²): 4.81

Mass loss:
- Initial Mass (g): 43
- Total mass produced (g): 3.02
- Total mass loss (g/m²): 377.62
- Mass Loss Rate (g/m²/s): 1.92

Average Effective Heat of combustion (MJ/kg): 17.61

Rate of Smoke Production (RSP) & Total Smoke Production (TSP):
- TSP (m³/m²): 222.67
- Average RSP at end test (m³/m²/s): 0.30
- Max. RSP (m³/m²/s): 3.04
- Time of appearance (s): 503
- MAXRE (kW): 12.43
- Time of appearance (s): 365
ISO 5660, CONE TEST

Test Specimen 18
Reference of the specimen: 3-ply GLARE
Test date: 2008-02-26
Wire grid used: Yes
Orientation: Horizontal
Fixing: Standard
Heat Flux Level (kW/m²): 35

Observations:
- Ignition (s): 235
- Test duration (s): 300
- Early termination of the test at the demand of the sponsor after extinction: No
- Flashing: No
- Meltng: No
- Swelling: No
- Carbonisation: Yes

Rate of Heat Release (RHR) & Total Heat Release (THR):
- Average RHR (kW/m²) at end test: 5.04
- Average RHR (kW/m²) at 180s after ignition: 30.14
- Average RHR (kW/m²) at 300s after ignition: 17.42
- Max. RHR (kW/m²): 24.47
- Time of appearance (s): 315

THR (MJ/m²): 5.47

Mass loss:
- Initial Mass (g): 44
- Total mass pyrolysed (g): 3.28
- Total mass loss (g/m²): 409.47
- Mass Loss Rate (g/m²/s): 1.11

Average Effective Heat of combustion (MJ/kg):
- 15.89

Rate of Smoke Production (RSP) & Total Smoke Production (TSP):
- TSP (m³/m²): 344.33
- Average RSP at end test (m³/m²): 0.37
- Max. RSP (m³/m²): 0.50
- Time of appearance (s): 300

MAHRE (kW):
- 14.17
- Time of appearance (s): 365
Experimental Work.

ISO 5660, CONE TEST

Test Specimen 10
Reference of the specimen: CFRP + insulation
Test date: 2008-02-26
Wire grid used: Yes
Orientation: Horizontal
Fixing: Standard
Heat Flux Level (kW/m²): 35

Observations:
Ignition (s) 420
Test duration (s) 000
Early termination of the test at the demand of the sponsor after extinction
Flashing No
Melting No
Swelling No
Carbonisation Yes

Rate of Heat Release (RHR) & Total Heat Release (THR):
Average RHR (kW/m²) at end test 34.11
Average RHR (kW/m²) at 180s after ignition 35.95
Average RHR (kW/m²) at 300s after ignition 83.84
Max. RHR (kW/m²) 191.98
Time of appearance (s) 680
THR (MJ/m²) 30.82

Mass loss
Initial Mass (g) 39
Total mass pyrolysed (g) 14.82
Total mass loss (g/m²) 1832.63
Mass Loss Rate (g/m²/s) 6.24

Average Effective Heat of combustion (MJ/kg) 18.04
Rate of Smoke Production (RSP) & Total Smoke Production (TSP)
TSP (m³/m²) 2432.40
Average RSP at end test (m³/m²/s) 2.70
Max. RSP (m³/m²/s) 13.41
Time of appearance (s) 650
MAHRE (kW) 40.09
Time of appearance (s) 750
ISO 5660, CONE TEST

Test Specimen 20
Reference of the specimen: CFRP2 + 16% Metapoor 200 + insulation
Test date: 2008-02-26
Wire grid used: Yes
Orientation: Horizontal
Fixing: Standard
Heat Flux Level (kW/m²): 35

Observations:
Ignition (s) 445
Test duration (s) 900
Early termination of the test at the demand of the sponsor after extinction
Flashing  No
Melting  No
Swelling  No
Carboxylation  Yes

Rate of Heat Release (RHR) & Total Heat Release (THR):
Average RHR (kW/m²) at end test 35.90
Average RHR (kW/m²) at 180s after ignition 94.92
Average RHR (kW/m²) at 300s after ignition 107.49
Max. RHR (kW/m²) 233.80
Time of appearance (s) 619

THR (MJ/m²) 32.57

Mass loss
Initial Mass (g) 42
Total mass polypropylene (g) 16.23
Total mass loss (g/m²) 2028.34
Mass loss rate (g/m²/s) 9.12

Average Effective Heat of combustion (MJ/kg) 17.11

Rate of Smoke Production (RSP) & Total Smoke Production (TSP):
TSP (m³/m²) 1951.92
Average RSP at end test (m³/m²) 2.18
Max. RSP (m³/m²) 14.59
Time of appearance (s) 605

MAHRE (kW) 45.86
Time of appearance (s) 720
Experimental Work.

ISO 5660, CONTE TEST

Test Specimen 21
Reference of the specimen: CFRP2 + 20% AP423 – insulation
Test date: 2008-02-26
Wire grid used: Yes
Orientation: Horizontal
Fixing: Standard
Heat Flux Level (kW/m²): 35

Observations:
- Ignition (s): 465
- Test duration (s): 900
- Early termination of the test at the demand of the sponsor after extinction: No
- Melting: No
- Swelling: No
- Carbonisation: Yes

Rate of Heat Release (RHR) & Total Heat Release (THR):
- Average RHR (kW/m²) at end test: 29.64
- Average RHR (kW/m²) at 180s after ignition: 30.58
- Average RHR (kW/m²) at 300s after ignition: 34.18
- Max. RHR (kW/m²): 209.02
- Time of appearance (s): 500
- THR (MJ/m²): 26.76

Mass loss:
- Initial Mass (g): 39
- Total mass probed (g): 13.37
- Total mass loss (g/m²): 1671.28
- Mass Loss Rate (g/m²/s): 7.86
- Average Effective Heat of combustion (MJ/kg): 17.75

Rate of Smoke Production (RSP) & Total Smoke Production (TSP):
- TSP (m³/m²): 1733.83
- Average RSP at end test (m³/m²): 1.02
- Max. RSP (m³/m²): 15.19
- Time of appearance (s): 560

MARRE (kW): 36.30
- Time of appearance (s): 705
ISO 5660, CONT TEST

Test Specimen 22
Reference of the specimen: 3-ply GLARE + insulation
Test date: 2008-02-25
Wire grid used: Yes
Orienteration: Horizontal
Fixing: Standard
Heat Flux Level (kW/m²): 35

Observations:
Ignition (s) 950
Test duration (s) 1280
Early extinguishment of the test at the demand of the sponsor after extinction
Flashing
Melting
Swelling
Carbonization: Yes

Rate of Heat Release (RHR) & Total Heat Release (THR):
Average RHR (kW/m²) at end test 8.20
Average RHR (kW/m²) at 180s after ignition 5.19
Average RHR (kW/m²) at 300s after ignition 3.48
Max. THR (kW²/m²) 88.03
Time of appearance (s) 1025
THR (MW/m²) 10.54
Mass loss
Initial Mass (g) 95
Total mass consumed (g) 9.13
Total mass loss (g/m²) 1144.18
Mass Loss Rate (g/m²/s) 3.70
Average Effective Heat of combustion (MJ/kg) 21.61
Rate of Smoke Production (RSP) & Total Smoke Production (TSP)
TSP (m³/m²) 759.25
Average RSP at end test (m³/m²/s) 0.50
Max. RSP (m³/m²/s) 0.35
Time of appearance (s) 1030
MAHRE (kW)
Time of appearance (s) 1165
Experimental Work.

ISO 5660, CONE TEST

Test Specimen 23
Reference of the specimen: CFRP1 only
Test date: 2008-02-26
Wire grid used: Yes
Orientation: Horizontal
Fixing: Standard
Heat Flux Level (kW/m²): 35

<table>
<thead>
<tr>
<th>Observations</th>
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<tbody>
<tr>
<td>Ignition (s)</td>
<td>75</td>
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<tr>
<td>Test duration (s)</td>
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<td>Early termination of the test at the demand of the sponsor after extinction</td>
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</tr>
<tr>
<td>Flashing</td>
<td>No</td>
</tr>
<tr>
<td>Melting</td>
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</tr>
<tr>
<td>Swelling</td>
<td>No</td>
</tr>
<tr>
<td>Carbonisation</td>
<td>Yes</td>
</tr>
<tr>
<td>Rate of Heat Release (RHR) &amp; Total Heat Release (THR):</td>
<td></td>
</tr>
<tr>
<td>Average RHR (kW/m²) at end test</td>
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</tr>
<tr>
<td>Average RHR (kW/m²) at 180s after ignition</td>
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<td>Average RHR (kW/m²) at 300s after ignition</td>
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<td>Max. HRR (kW/m²)</td>
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<td>Time of appearance (s)</td>
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<tr>
<td>THR (MJ/m²)</td>
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<td>Mass loss</td>
<td></td>
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<tr>
<td>Initial Mass (g)</td>
<td>35</td>
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<tr>
<td>Total mass produced (g)</td>
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</tr>
<tr>
<td>Total mass loss (g/m²)</td>
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<tr>
<td>Mass Loss Rate (g/m² s)</td>
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<td>Average Effective Heat of combustion (MJ/kg)</td>
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</tr>
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<td>Rate of Smoke Production (RSP) &amp; Total Smoke Production (TSP)</td>
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</tr>
<tr>
<td>TSP (m³/m²) at end test</td>
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<td>Average RSP at end test (m³/m²)</td>
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<td>Max. RSP (m³/m²)</td>
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<td>Time of appearance (s)</td>
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<tr>
<td>MAHRE (kW)</td>
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<td>Time of appearance (s)</td>
<td>210</td>
</tr>
</tbody>
</table>

APPLIED MECHANICS LABORATORY 7-49
ISO 5660, CONE TEST

Test Specimen 24
Reference of the specimen: CFRP1 only
Test date: 2008-02-26
Wire grid used: Yes
Orientation: Horizontal
Fixing: Standard
Heat Flux Level (kW/m²): 35

Observations:
Ignition (s) 75
Test duration (s) 900
Early termination of the test at the demand of the sponsor after extraction
Flashover No
Melting No
Swelling No
Carbonisation Yes
Rate of Heat Release (RHR) & Total Heat Release (THR)
Average RHR (kW/m²) at end test 29.30
Average RHR (kW/m²) at 180s after ignition 142.21
Average RHR (kW/m²) at 300s after ignition 85.79
Max. THR (MJ/m²) 204.41
Time of appearance (s) 176
THR (MJ/m²) 26.38

Mass loss:
Initial Mass (g) 35
Total mass produced (g) 16.69
Total mass loss (g/m²) 2011.89
Mass Loss Rate (g/m²/s) 12.87
Average Effective Heat of combustion (MJ/kg) 16.18
Rate of Smoke Production (RSP) & Total Smoke Production (TSP)
TSP (m³/m²) 1844.36
Average RSP at end test (m³/m²) 2.05
Max. RSP (m³/m²) 23.12
Time of appearance (s) 132
MAHR (LW) 107.77
Time of appearance (s) 220
7.3 Thermogravimetry, TGA.

The principles of thermogravimetry are illustrated in Figure 7.18. The sample, indicated by number 3, is kept in a controlled furnace, 2, whose temperature is monitored by the thermocouple, 4, via the millivoltmeter, 5. The balance, 1, allows continuous mass determination. A plot of mass as a function of temperature, T, or time, t, represents the essential thermogravimetry result. The schematic of a typical thermogravimetric system is illustrated based on the classical, high-precision instrument, the Mettler Thermoanalyzer [7.1]. The block diagram of Figure 7.19 gives a general overview of the instrumentation and control, and Figure 7.20 is a sketch of a basic thermoanalyzer installation. The center table provides space for the high temperature furnace, the balance, and the basic vacuum equipment. The cabinet on the right houses the control electronics and computer. On the left is the work bench and gas-cleaning setup.

![Schematic for a thermogravimetric experiment](image)

Figure 7.18 TGA schematic representation.

The weighing principle is shown in the upper right diagram in Figure 7.20. At the center is the beam balance with a sapphire wedge support. The operation is based on the substitution principle. As a sample is added to the balance pan, an equivalent mass is lifted off above the pan to keep the balance in equilibrium. In this classical balance the main weights still are moved manually, as in standard analytical balances. For continuous recording, there is compensation by an electromagnetic force that acts on the right balance beam. A photoelectric scanning system detects any imbalance and adds an electromagnetic force to compensate the pull of gravity. This electromagnetic force can correct imbalances between 0 and 1000mg, and is recorded with an accuracy of 50μg over the whole 16g weighing range. Modern instruments cover more, or all of the weighing range electromagnetically.
The gas flow diagram is illustrated in the bottom diagram of Figure 7.20. The pumping system produces a vacuum of about $10^{-3}$ Pa ($=10^{-5}$ mm Hg). After evacuation of the balance, a chosen inert gas can be added through the left inlet. A flow rate as high as 30 L/hr is possible without affecting weighing precision. Corrosive gases are entered separately through the top inlet. This second gas flow is arranged so that corrosive gases added to or developed by the sample cannot diffuse back into the balance compartment. A cold trap and a manometer are added on the right side, located at a point just before the gas outlet. At this position one can add further analysis equipment to identify the gases evolved from the sample.
Experimental Work.

In the following figures the results of the CFRP samples are presented. First the **CFRP Type 2 & Type 5** are presented, Figure 7.21 and later the fire retarded **Type 3 & Type 4**, Figure 7.22,7.23. Finally in Figure 7.24 the 5-ply GLARE samples are presented.

![Figure 7.21 TGA graphs of the CFRP Type 2 & Type 5 manufactured by IAI. Tested at various heating rates.](image1)

![Figure 7.22 TGA graphs of the CFRP Type 3 & Type 4 manufactured by IVW. Samples containing the CIBA Melapur 200 fire retardant.](image2)
7.4 Differential Scanning Calorimetry, DSC.

7.4.1 Carbon Fiber Specific Heat Capacity, $C_P$

Two different PAN based CF tows having different number of fibers each, were tested [7.2]. The first CF tow was the (HTA40J/E-3K-E13/E13 type with ca. 1.3%, sizing based on epoxy resin) consisting of 3000 fibers and the second one was the (T700S-12K-60E/sizing type and amount 60E 0.3%) consisting of 12000 fibers.
7.4.1.A Air atmosphere.

The variation of specific heat capacity versus temperature was measured with DSC tests of the fibers under investigation. The procedure was the one described in the operation manual of the DSC apparatus [7.3]. At first the aluminum sample pans were run empty and the heat flow was measured. This curve was the reference curve. Then the sample tin was filled with a small amount (approximate 6–10 mg) of carbon fibers, it was sealed with a lid and put inside the chamber. The same as before heating profile was applied and the heat flow was measured. The difference between the two heat flow curves (reference empty pans and fiber filled), along with the calibration coefficient of the apparatus, were used for the derivation of the relationship of specific heat capacity versus temperature.

The results indicate that a linear relation exist between specific heat capacity and temperature, as shown in Figure 7.25. The experimental results are in good agreement with Pradere et al. [7.4].

![Figure 7.25 Specific heat capacity cp as a function of temperature for the T700S-12K and HTA40-3K carbon fiber tows.](image)

7.5 Burnthrough Tests.

7.5.1 ISO2685 Propane Burner

7.5.1.A Test instrumentation

The main instrumentation on these tests comprised of still and video equipment. Thus digital still cameras were deployed to view both the front and rear faces and
were set to frame at 30s intervals using a self-timer. Similarly, two digital video cameras were used to record the response of the front and rear faces of the sample from positions approximately 1m to the right of the sample (when viewed from the front of the sample) and 1m to the front and rear. These records were stored in real time on a hard disc recorder and again subsequently downloaded as video files to a PC computer.

A thermal imaging record of the back face of the sample tests was also attempted to determine sample temperatures. The IR camera system used was a FLIR SC2000 – an un-cooled thermal imaging system operating in the 7-13 μm wavelength bandwidth. This employs a micro-bolometer array detector producing high-resolution images at the rate of 50 frames per second (fps). It records directly to a PC-based DVD recording system with a 12-bit temperature resolution over a range of –30 to 2000°C located in the Control Room. The available optical systems range from 100-μm close-up to a 12° wide-angle lenses. The camera was located on the side wall of the test enclosure on the axis of the sample and some 1.5m directly behind it. The data record was subsequently downloaded on to DVD and then sampled at 30s intervals. These false colour images for each test are presented in Section 7.5.1.F.

A rectangular strip of standard paint with a known emissivity was applied to the rear face of some of the samples during the tests so as to provide a temperature reference point. These data were used to determine the emissivity of the material at different temperatures and use the results to calculate the implied plate temperatures from the measured IR surface emissive power. A further attempt to validate the temperature obtained with the imager was made by using a surface K-type spring-loaded surface temperature thermocouple having a disc 2mm in diameter, to record a spot temperature on the rear of each sample. This proved of limited success since the spring loading was not always sufficient to maintain contact with the surface. The data from this instrument were recorded via a thermocouple data conditioning unit, at a frequency of 1Hz on a PC computer.
7.5.1.B Rig calibration.

The standard calibration procedures were carried out with each test as detailed in Section 6.1.1.C and according to the ISO2685 Standard [7.5]. Thus measurements were taken on the burner using the thermocouple calibration array and the continuous water flow calorimeter pre-test and with the thermocouple array post-test. Data from the thermocouple calibrations were recorded on a PC computer and retained.

7.5.1.C Test samples

The test rig was designed to accomodate samples 400mm square so samples were cut to this size. However, the supplied GLARE panels were 370x400mm so the gap had to be “filled” with a 30x400mm patch attached with ceramic glue to make up the required sample size. The sample was then mounted in the sample holder with the joint running horizontally along the bottom of the sample. Subsequently, in one test there was early flame penetration at one point along ignited and burned through this joint producing a small flame stream at the back face of the sample. For the other GLARE sample flame penetration at the ceramic joint only occurred very late in the test and there was no flaming on the rear face. Details of the samples tested are given in Table 7.19.

<table>
<thead>
<tr>
<th>Sample number</th>
<th>Test</th>
<th>Material</th>
<th>Source</th>
<th>Dimensions, mm</th>
</tr>
</thead>
<tbody>
<tr>
<td>NBF499-1</td>
<td>1</td>
<td>Aluminium alloy</td>
<td>Peciney Rhenalu</td>
<td>400x400x2.03</td>
</tr>
<tr>
<td>NBF499-1R</td>
<td>2</td>
<td></td>
<td>Issoue</td>
<td></td>
</tr>
<tr>
<td>NBF499-2</td>
<td>3</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>NBF0047-1</td>
<td>4</td>
<td>Multi-layer monolithic carbon fibre reinforced epoxy</td>
<td>Israeli Aircraft Industries</td>
<td>400x400x3</td>
</tr>
<tr>
<td>NBF0047-2</td>
<td>5</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>NBF0052-1</td>
<td>6</td>
<td>5 ply GLARE</td>
<td>Wentzel Engineering</td>
<td>370x400x3 mated with 30x400x3</td>
</tr>
<tr>
<td>NBF0052-2</td>
<td>7</td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Table 7.19: Fuselage burnthrough test samples.
7.5.1.D  Test parameters.
The tests were carried out according to ISO 2685 Standard [7.5]. The separation of the burner face and sample was set at 75mm for all tests and calibration activities both before and after sample exposure to the flame ensured that the burner was fully compliant with the test standard. Thus the burner was delivering a heat flux of 116±10kW/m² and the “flame temperature” (without any radiation correction) was 1100±80°C. The test duration was set at 15 minutes (900sec) unless the sample failed before that time.

7.5.1.E  Test results
The major test results are given in Table 7.20. This summarises the main test observations and lists the time each sample resisted penetration from the burner flame. This was determined both at the time and post-test from examination of the video record.

The thermal infra-red data are given in the form of false colour images. These were captured from the video records at a number of discrete times. Thus for those tests which were of short duration - essentially the aluminium plate tests – images are provided at 15s, then every 30s during the test, with a final image as the burner is moved away. For the longer duration tests – the GLARE and CFRP composite – the images provided are at 15, 30 and 60s into each test followed by images at 1 minute intervals.

A correction for emissivity must be made to some of the temperatures implied by these images. Essentially the thermal imaging camera measures the surface emitted power from the sample. This \( H_s \) is given by:

\[
H_s = \varepsilon_s \sigma (T_s^4 - T_A^4)
\]

where \( \sigma \) is the Stefan-Boltzmann constant \( (5.67 \times 10^{-8} \text{ W/m}^2\text{K}^4) \), \( \varepsilon_s \) is the surface emissivity, which varies with material, surface finish, and temperature, \( T_s \) is the ‘true’ surface temperature and \( T_A \) the ambient temperature.
The correction is facilitated by the use of standard emissivity paint having a value of 0.92. A band of this paint is applied to the rear of the sample and the emissivity set to this value in the imaging software. The temperatures output from the processing software assume this value of emissivity applies over the entire plate. Temperatures outside the paint are therefore in error. For two neighbouring points on the sample, one located within the painted band and a second outside it, which are sufficiently close to allow the assumption of an identical temperature, the surface emissive powers measured by the instrument are:

\[
H_{SU} = \varepsilon_P \sigma \left( T_{SM}^4 - T_A^4 \right) \tag{7.2}
\]

and

\[
H_{SP} = \varepsilon_P \sigma \left( T_{SP}^4 - T_A^4 \right) \tag{7.3}
\]

where \(H_{SU}\) and \(H_{SP}\) are the surface emitted powers measured from unpainted and painted parts of the plate respectively, \(\varepsilon_P\) is the emissivity of the paint and \(T_{SP}\) and \(T_{SM}\) are the measured temperatures on and outside the paint. Also:

\[
H_{SU} = \varepsilon_S \sigma \left( T_{SU}^4 - T_A^4 \right) \tag{7.4}
\]

where \(T_{SU}\) is the real temperature of the sample. For neighbouring points it is assumed \(T_{SU} = T_{SP}\) and thus the emissivity of the material/plate is given by:

\[
\varepsilon_S = \frac{\varepsilon_P}{\varepsilon_S} \frac{T_{SM}^4 - T_A^4}{T_{SP}^4 - T_A^4} \tag{7.5}
\]

and the corrected temperature is:

\[
T_{SU} = \left\{ \frac{\varepsilon_P}{\varepsilon_S} \left( T_{SM}^4 - T_A^4 \right) + T_A^4 \right\}^{1/4} \tag{7.6}
\]
<table>
<thead>
<tr>
<th>Sample number</th>
<th>Material</th>
<th>Test conditions (from calibration rigs)</th>
<th>Main test observations</th>
<th>Time to failure (flame penetration), s</th>
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<tbody>
<tr>
<td></td>
<td></td>
<td>Temperature, °C</td>
<td>Heat flux, kWm⁻²</td>
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</tr>
<tr>
<td>NBF499-1R</td>
<td>Aluminium alloy</td>
<td>1152</td>
<td>118.4</td>
<td>30 s Bowing away from burner</td>
</tr>
<tr>
<td></td>
<td></td>
<td>1158</td>
<td></td>
<td>90 s Severe buckling and cracking</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>150 s Centre glowing red with severe buckling</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>210 s Rear surface glowing red, crack opened up</td>
</tr>
<tr>
<td>NBF499-2</td>
<td></td>
<td>1174</td>
<td>118.0</td>
<td>30 s Specimen glowing</td>
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<td></td>
<td></td>
<td>1117</td>
<td></td>
<td>60 s Glowing + distortion</td>
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<td>120 s Increased distortion</td>
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<tr>
<td>NBF0047-1</td>
<td>Epoxy carbon fibre</td>
<td>1170</td>
<td>115.4</td>
<td>30 s Severe front flaring and smoking</td>
</tr>
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<td></td>
<td></td>
<td>1127</td>
<td></td>
<td>180 s Red glow in centre</td>
</tr>
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<td></td>
<td></td>
<td></td>
<td>240 s Flaring and smoke subsiding, red glow in centre</td>
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<tr>
<td></td>
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<td></td>
<td></td>
<td>No significant burn on</td>
</tr>
<tr>
<td>NBF0047-2</td>
<td></td>
<td>1161</td>
<td>119.6</td>
<td>30 s Significant front face flaring and smoke</td>
</tr>
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<td></td>
<td></td>
<td>1153</td>
<td></td>
<td>180 s Significant flaring, centre glowing red</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>360 s Centre getting hotter, burner sooted</td>
</tr>
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<td></td>
<td></td>
<td></td>
<td></td>
<td>420 s Flaring decreased</td>
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<td></td>
<td></td>
<td></td>
<td>No significant burn on time</td>
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Experimental Work.

<table>
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<th>PTB</th>
<th>Time</th>
<th>Observation</th>
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<tr>
<td>NBF0052-1</td>
<td>5-ply GLARE</td>
<td>1147</td>
<td>123.0</td>
<td>60 s Local jet flaring</td>
</tr>
<tr>
<td></td>
<td></td>
<td>1142</td>
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<td>120 s Significant</td>
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<td>local flaring</td>
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<td>specimen centre</td>
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<td></td>
<td>glowing, limited</td>
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<td></td>
<td></td>
<td>smoke</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>270 s Local cracking</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>300 s Smoking</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>from back surface</td>
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<td></td>
<td></td>
<td></td>
<td>360 s Back face</td>
</tr>
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<td></td>
<td>smoking but no</td>
</tr>
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<td></td>
<td></td>
<td></td>
<td>charring</td>
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<td></td>
<td>480 s Limited</td>
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<td>, pronounced</td>
</tr>
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<td></td>
<td></td>
<td></td>
<td>surface cracking</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>40 s burn on time</td>
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<td>NBF0052-2</td>
<td></td>
<td>1165</td>
<td>122.2</td>
<td>60 s Flaring with</td>
</tr>
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<td></td>
<td></td>
<td>1118</td>
<td></td>
<td>surface cracking</td>
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<td></td>
<td></td>
<td></td>
<td></td>
<td>180 s Flaring from</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>make-up joint</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>now glowing red</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>270 s Flame from</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>make-up joint</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>360 s Flaring from</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>centre</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>35 s burn on time</td>
</tr>
</tbody>
</table>

Table 7.20: Main observations of the fuselage burnthrough tests.
Figure 7.26  Test 1, Aluminum 1.
Experimental Work.

Aluminum sample 2 pre-test  
Front face post-test  
Rear face post-test  

15s  
30s  
60s
Figure 7.27  Test 2, Aluminum 2.
Figure 7.28  Test 3, Aluminum 3.
Experimental Work.

<table>
<thead>
<tr>
<th>CFRP Sample 1 front face, pre-test</th>
<th>Rear face, pre-test</th>
</tr>
</thead>
<tbody>
<tr>
<td>Front face, post-test</td>
<td>Rear face, post-test</td>
</tr>
<tr>
<td>15s</td>
<td>30s</td>
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</tbody>
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APPLIED MECHANICS LABORATORY
<table>
<thead>
<tr>
<th>Time</th>
<th>Image</th>
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</thead>
<tbody>
<tr>
<td>420s</td>
<td><img src="image1.png" alt="Image" /></td>
</tr>
<tr>
<td>480s</td>
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<tr>
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<tr>
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<td><img src="image4.png" alt="Image" /></td>
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<tr>
<td>660s</td>
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</tr>
<tr>
<td>720s</td>
<td><img src="image6.png" alt="Image" /></td>
</tr>
</tbody>
</table>
Figure 7.29  Test 4, CFRP 1.
Experimental Work.

CFRP Sample 2 front face, pre-test

Front face, post-test

Rear face, post-test

15s

30s

60s

APPLIED MECHANICS LABORATORY
<table>
<thead>
<tr>
<th>Time (s)</th>
<th>Image 1</th>
<th>Image 2</th>
</tr>
</thead>
<tbody>
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<td><img src="image2.png" alt="Image" /></td>
</tr>
<tr>
<td>180s</td>
<td><img src="image1.png" alt="Image" /></td>
<td><img src="image2.png" alt="Image" /></td>
</tr>
<tr>
<td>240s</td>
<td><img src="image1.png" alt="Image" /></td>
<td><img src="image2.png" alt="Image" /></td>
</tr>
<tr>
<td>300s</td>
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<td><img src="image2.png" alt="Image" /></td>
</tr>
<tr>
<td>360s</td>
<td><img src="image1.png" alt="Image" /></td>
<td><img src="image2.png" alt="Image" /></td>
</tr>
<tr>
<td>420s</td>
<td><img src="image1.png" alt="Image" /></td>
<td><img src="image2.png" alt="Image" /></td>
</tr>
</tbody>
</table>
Experimental Work.
Figure 7.30  Test 5, CFRP 2.
Experimental Work.

15s

30s

60s

120s

180s

240s

APPLIED MECHANICS LABORATORY
Figure 7.31  Test 6, GLARE 1.
<table>
<thead>
<tr>
<th>Front face, pre-test</th>
<th>Rear face, pre-test</th>
</tr>
</thead>
<tbody>
<tr>
<td>Front face, post-test</td>
<td>Rear face, post-test</td>
</tr>
<tr>
<td>15s</td>
<td>30s</td>
</tr>
</tbody>
</table>
Experimental Work.
Experimental Work.

Figure 7.32  Test 7, GLARE 2.

Figure 7.33  Aluminum 1, Time history of temperature at a point in the painted region near the center of the plate.
Figure 7.34  Aluminum 3, Temperatures at two points on the rear of the plate, points 5 and 6.

Figure 7.35  CFRP 1, Temperature history at selected points on the rear face.

Figure 7.36  CFRP 2, Temperature history at selected points on the rear face.
Experimental Work.

Figure 7.37  GLARE 1, Temperature variation of emissivity implied from thermal infrared imaging.

Figure 7.38  GLARE 1, Temperature histories at selected points on the rear face.

Figure 7.39  GLARE 2, Temperature histories at selected points on the rear face.
7.5.1.F Results discussion

The main data from the fuselage burnthrough testing are the thermal infra-red images of the rear face of the sample. These present intervals at set times throughout the test together with some time series temperatures corrected for emissivity. There were some difficulties with these measurements.

For the aluminium and one composite tests, the thermal imager was set on a higher measurement range and hence the instrument did not provide reliable measurements at temperatures below 216°C. Hence few of the selected points on the plate provided reliable temperature measurements. Additionally, for the first aluminium test thermal infrared data were obtained but burner data were lost. This test was subsequently repeated but without a strip of standard emissivity paint (Test 2). As a result of these difficulties there was limited data from which to obtain a value for emissivity of the material and thus allow correction of temperatures. What little data were available for computation of emissivity suggested a value between 7 and 12%. This is lower than the value of around 20% expected. This low value provided unrealistically high corrected temperatures – the few of these, which were available. As expected, the aluminium samples failed quickly with temperatures close to the point of failure up around the melting point of aluminium of 600°C.

The emissivity of the composite samples was taken to be close to that of the standard paint at 0.92, thus there is no need to correct the imager-provided temperatures for these samples. This is a reasonable assumption given the black finish of the material, though there may be a slight error due to the shiny nature of the surface. These samples endured for the full fifteen minutes of the tests and provided good temperature data. In general, on application of the burner, the resin ignited and burned until it was totally consumed in the high temperature region. This left the carbon fibre reinforcement which remained intact but glowed red in the presence of the burner flame.

There was no flame penetration. Back-face temperatures were similar from test to test. The temperature was generally uniform at around 300°C over the surface with a slightly higher temperature (~400°C) in the centre near the flame impingement point.
Experimental Work.

Good data were obtained for both GLARE tests. Standard emissivity paint was used on both these tests but the fact that the thermal response of the surface is similar in both painted and unpainted areas indicates that the GLARE surface has a similar emissivity to the standard value paint. The variation of emissivity with temperature is given for both GLARE tests. The data for the first test show very variable values but the second test gave a steady value around 0.80 for most of the test when the temperature had reached a high steady value. Thus the effect of painting the aluminium outer GLARE layer was to significantly increase the surface emissivity. During a test, on application of the flame, the material quickly delaminated with the flame side outer layer bowing towards the flame. This layer then cracked and melted allowing exposure of the inner composite layer. The composite resin then burned off. The rear painted surface charred during the test. This behaviour is reflected in the temperature behaviour particularly for the first GLARE test for which the back-face temperature initially rose quickly and then decayed as the front side surface layer delaminated reducing the thermal contact between layers. The overall temperature of the back face was steady during the test at a lower value ~250°C than for the composite. The second GLARE test gave slightly different behaviour in that the overall temperatures were slightly higher at around 300°C and the initial temperature ‘hump’ was much less pronounced. The visual behaviour was however very similar.

The use of a contact thermocouple to provide validation of the thermal infra-red imager temperatures proved unsuccessful with the thermocouple moving off the surface during the tests.

7.6 References


CHAPTER 8  
Modelling Work.

8.1  Boundary Conditions.

The modeling part of the fire burnthrough work involves the development of a thermal numerical model that is solved using a commercial FE Thermal solver, for the current work the ANSYS Thermal was selected. The boundary conditions used in these models are the impinging heat flux distribution and the rear face cooling conditions.

8.1.1  Heat Flux.

8.1.1.A  ISO2685 Burner

As thermal load in the current work the spatial distribution of the impinging heat-flux from the ISO 2685 propane-burner on the front surface of the flat panel was used. This distribution could not be measured experimentally at every point on the panel. Observing the burn-through tests, it was evident that the heat-flux was not constant on the surface of the tested panel but “followed” a distribution pattern that had to be determined. To tackle this problem a detailed Computational Fluid Dynamics, CFD model of the propane-burner was developed (as already described in chapter 6, section
6.1). The developed CFD model consisted of four different configurations: a) simplified burner and plate, b) Heat flux calibration, c) Temperature calibration and d) Burner and plate test configurations, as presented in Figure 8.1. The first of these (configuration a), was used to undertake a range of sensitivity tests to examine the influence of the computational grid, the turbulence model, the radiation model and the domain size on the CFD predictions. All the CFD simulations employed in the analyses use an identical burner geometry and inlet/outlet conditions. Since all flows (premixed fuel/air and secondary air) are symmetric about the vertical mid-plane, only half of the geometry was simulated to minimize computational effort, Figure 8.2.

After the sensitivity tests between the coarse mesh and the fine mesh (400000 and 800000 nodes respectively), it was concluded that despite the fact of some differences of the heat flux values, the coarse grid would be used for the rest of the analysis because the fine was “demanding” in computational power. For the turbulence and radiation models after a survey in available models, the SST model and Discrete Transfer model were selected and used respectively. In all tests the domain size was that of the flat plate (400x400mm) with a slight extension of 50.8mm around the plate to compensate for boundary effects, also burner was 76.2 mm in front of the plate. Simplified burner heat-flux and flame temperature calibrations were performed as
defined by the ISO Standard with a water calorimeter and an array of thermocouples at specific locations respectively, details in section 6.1. In Figure 8.3a contours and streamlines of the burner and flat plate CFD model are given in front view. It was observed that the flux was not constant over time but decreased as time elapsed. This was caused by the fact that as the panel heats up, the heat transfer rate between the flame and the panel decreases. At this point an approximation was introduced. The approach of a “frozen-flux” was considered as thermal load, meaning that the impinging heat-flux distribution is assumed constant over time. The spatial flux distribution when the front (“hot”) surface of the plate is at room temperature was considered in the “frozen-flux” assumption. This is a worst-case scenario, since this flux is measured at the maximum heat-transfer rate and would lead to an over prediction of back face temperatures. The “frozen-flux” spatial distribution was used as the thermal load in the FE burn-through model. In Figure 8.3b results of the burner CFD model for the “frozen-flux” spatial distribution are presented, the ones imported into the FE model.

Figure 8.2 Detail view of the modeled burner showing the matrix of primary premixed fuel/air inlets and the secondary air inlets.
8.1.1.B AML Burner

The thermal load boundary condition was the propane burner heat flux distribution on the small flat panel surface. The distribution was measured using a water-cooled Hukseflux Schmit-Boelter SBG01 sensor, Figure 8.4. The maximum flux was observed in the middle of the panel and 63kW/m² in magnitude.
The intensity of the impinging heat flux of this lab-scale burner is greater than the ISO2685. This happens because the propane burner lays at a greater distance from the panel surface, 76.2mm while the AML burner was 30mm away from the sample surface.

### 8.1.2 Rear Face Cooling.

Since the thermal load was defined as described in section 8.1.1, the back-face boundary conditions had to be defined. The assumption that the back-face is adiabatic, simplifies the problem but leads to erroneous unrealistic results. During the tests, both ISO2685 and AML ones, the back face was not insulated. Thus a realistic back-face boundary condition was needed. The flat plate is in vertical position and is not force-cooled, so natural convection of a vertical flat plate combined with irradiative cooling was incorporated. For simplicity reasons, the approximation of an “equal-convection” is used to account for both natural convection and radiation.

The impinging heat-flux $Q_{IN}$ on a unit-surface of the flat-plate is split into $Q_{STORED}, Q_{COND}, Q_{CONV}$ and $Q_{RAD}$ parts as shown in Figure 8.5, where $Q_{STORED}$ contributes in raising the temperature of the material (in steady state conditions it is equal to zero), $Q_{COND}$ is the amount that is conducted inside the material and $Q_{CONV}, Q_{RAD}$ are the amounts of heat “lost” to the environment, $Q_{IN}=Q_{STORED}+Q_{COND}=Q_{CONV}+Q_{RAD}$. Applying the previously mentioned simplification:

\[ \bar{h}_{\text{eq}} = \bar{h}_{N-\text{CONV}} + \varepsilon \sigma \left( \frac{T_{\text{BACK}}^4 - T_{\infty}^4}{T_{\text{BACK}} - T_{\infty}} \right) \]

(8.1)

where $\bar{h}_{\text{eq}}$ is the unit-surface average equivalent convective heat transfer coefficient, $\bar{h}_{N-\text{CONV}}$ is the natural convection average heat-transfer coefficient for a vertical flat plate, $\varepsilon$ is the back-face emissivity, $\sigma$ is the Stefan-Boltzmann constant $\sigma=5.670400 \times 10^{-8}$W/m²K⁴, $T_{\text{BACK}}$ and $T_{\infty}$ are the temperatures of the back-face and environment respectively. The $\bar{h}_{N-\text{CONV}}$ term is calculated using the Squire-Eckert [8.2] expression for the average heat transfer coefficient of a vertical isothermal flat plate subjected to natural convection.
\[
\bar{h}_{N-CONV} = 0.678 \left( \frac{k^2 \rho C_p}{v \cdot L} \right)^{1/4} \left( \frac{Pr}{0.952 + Pr} \right)^{1/4} \left( \frac{T_{BACK} - T_\infty}{T_\infty} \right)^{1/4}
\]

(8.2)

where, \( k, \rho, C_p, v, Pr \) are the conductivity, density, specific heat, kinematic viscosity and Prandtl number respectively of the air, calculated at the average temperature \( T_{AVE} = (T_{BACK} - T_\infty)/2 \).

Air thermophysical properties that are used both in the material model and the back-face boundary conditions are not constant but vary with temperature. The temperature dependent air properties are extensively described in NASA Technical Reports where curve-fits for the properties of equilibrium air at high temperatures are presented [8.3]. Those curve-fits were used in this work and can be found in section 8.2.5.

\begin{figure}[h]
\centering
\includegraphics[width=0.5\textwidth]{heat_flow.png}
\caption{Heat flow through the unit-volume.}
\end{figure}

\section{Material Model and FE Implantation.}

\subsection{Aluminium.}

The thermophysical properties of the 2024-T3 aluminium alloy were not assumed to be constant but varying with temperature [8.4],[8.5],[8.6]. The available data were not only for the solidus region of the Al alloy but also for the liquidus one. The temperature variation of Aluminium 2024-T3 thermal conductivity, density and specific heat capacity are shown in Figure 8.6.
8.2.2 Single-Stage Composite.

The single-stage decomposition of the polymer matrix has already been described in section 4.1 and the influence of the decomposition on the thermophysical properties of the composite are presented in section 5.1. Since the ANSYS 11.0 commercial Thermal solver was used for simulating those tests, somehow the material properties had to be implemented into the FE model. As received “from the self” the ANSYS package does not account for material properties change both with temperature and time, default ONLY temperature influence is considered. That was a problem for the developed methodology since material constantly changes as time elapses, so a way to tackle this problem and make the ANSYS package adapt to the user defined material model had to be developed.

User defined subroutines for material model, loads and boundary conditions were developed and implemented into the FE model. A brief description of the solution scheme is presented in the next. Simulation time is discretised into predefined time-intervals. The decomposition degree $\alpha$ takes values into the region 0 to 1, this range is divided into bands and for every band a material having the band-average decomposition degree is defined. After the solution of every time-interval, the decomposition degree of all layers for every element is calculated. Depending on the value of the decomposition degree, a material from the previously defined list is assigned to every layer for every element. Boundary conditions are updated, the
solution scheme is restarted and the next time-interval is solved. This procedure continues until all the requested/predefined time–intervals are solved.

Figure 8.7  Single-stage decomposition material model schematic.

8.2.3 Multi-Stage Composite.

The multi-stage decomposition of the polymer matrix has already been described in section 4.2 and the influence of the decomposition on the thermophysical properties of the composite are presented in section 5.2. The tests that were simulated using this multi-stage approach were the ones of the AML-Burner, section 7.6.2. Since the ANSYS 12.0 commercial Thermal solver was used for simulating those tests, the multi-stage material properties had to be implemented into the FE model. As for the single-stage approach (section 8.2.1) the ANSYS package does not account for material properties change both with temperature and time. To tackle this problem and make the ANSYS package adapt to the user defined multi-stage material model user defined subroutines had to be developed.

Similar to the single-stage approach (section 8.2.1), user defined subroutines for material model, loads and boundary conditions were developed and implemented into the FE model. A brief description of the solution scheme is presented in the next. Simulation time is discretised into predefined time-intervals. After the solution of every time interval the molar fractions of the elements-\(A,B,C,D\) for every layer of every element are calculated and stored. The systems of rate equations presented in section 4.2 are solved using a finite difference numerical scheme giving the molar fraction of every constituent. Their molar fractions are then calculated and combined
with the molar fractions $X_i$ the material thermophysical properties (density, thermal conductivity, specific heat capacity) and the char porosity are calculated according to the methodology presented in section 5.2. Finally a different material is assigned to every layer of every element (a total of $m \times n$ materials, where $m$ is the number of layers and $n$ is the number of elements). Boundary conditions are updated, the solution scheme is restarted and the next time-interval is solved. This procedure continues until all the requested/predefined time-intervals are solved. A schematic of the implementation of the multi-stage approach is presented in Figure 8.8

![Figure 8.8 Multi-stage decomposition material model schematic.](image)

### 8.2.4 GLARE.

The Hubrid –GLARE material is a combination of aluminum 2024-T3 layers and glass prepregs. So for this material model a combination of the Aluminum temperature-dependent material (section 8.2.1) and the single-stage composite (section 8.2.2) was used. No further need for other modification was needed.

### 8.2.5 Air properties at elevated temperature.

In all the material model approach, single/multi-stage decomposition and for the rear face cooling boundary condition, the properties of hot air are needed. Those values could be taken as steady values at a specific temperature **BUT** in this work it was selected a more realistic and accurate approach and use the polynomial curve fits
from NASA Reports [8.3] for density, dynamic viscosity, specific heat, thermal conductivity and Prandtl number:

### 8.2.5.A Density $\rho$

$$ T < 500K \rightarrow \rho = \rho_0 $$

$$ T > 500K \rightarrow \rho = \frac{P_0}{Z} \rightarrow \text{gr} / \text{cm}^3 $$

$$ Z = A_Z + B_Z T + C_Z T^2 + D_Z T^3 + E_Z T^4 $$

$$ R = \frac{R_0}{M_0} $$

$$ R_0 = 8314.4720 \text{J} / \text{kmolK} $$

$$ M_0 = 28.964 \text{kg} / \text{kmol} $$

$$ X = \frac{T}{1000}, \ T \rightarrow K $$

### 8.2.5.B Dynamic viscosity $\mu$

$$ T < 500K \rightarrow \mu = 1.4584 \times 10^{-5} \frac{T^{3/2}}{T + 110.33} \quad \mu \rightarrow \text{poise} $$

$$ T > 500K \rightarrow \mu = A_\mu + B_\mu T + C_\mu T^2 + D_\mu T^3 + E_\mu T^4 + F_\mu T^5 $$

$$ X = \frac{T}{1000}, \ T \rightarrow K $$

$$ \mu \rightarrow \text{poise} $$

### 8.2.5.C Kinematic viscosity $\nu$

$$ \nu = \frac{\mu}{\rho} $$

### 8.2.5.D Specific heat $C_p$

$$ T < 500K \rightarrow C_p = 0.24 \text{cal/grK} $$

$$ T > 500K \rightarrow C_p = \text{EXP}(A_{c_p} T^4 + B_{c_p} T^3 + C_{c_p} T^2 + D_{c_p} T + E_{c_p}), \ X = \ln\left(\frac{T}{10000}\right), \ T \rightarrow K $$

$$ C_p \rightarrow \text{cal/grK} $$

### 8.2.5.E Thermal conductivity $k$
Modelling Work.

\[ T < 500K \rightarrow k_e = 5.97776 \times 10^{-6} \frac{T^{3/2}}{T + 194.4}, \]
\[ T > 500K \rightarrow k = \text{EXP}(A_k X^4 + B_k X^3 + C_k X^2 + D_k X + E_k), \quad X = \ln \left( \frac{T}{10000} \right) \rightarrow K \frac{\text{cal}}{cm \cdot sec \cdot K} \]

8.2.5.F Prandtl number \( Pr \)

\[ N_{Pr} = 0.24 \frac{\mu_s}{k_s}, \quad T < 500K, \rightarrow \]
\[ N_{Pr} = A_{Pr} + B_{Pr} X + C_{Pr} X^2 + D_{Pr} X^3 + E_{Pr} X^4 + F_{Pr} X^5, \quad X = \frac{T}{1000}, \quad T \rightarrow K \]

The polynomial coefficients of the curve fits can be found in Table 8.1. These coefficients are for pressure 1atm and temperature <1000K.

<table>
<thead>
<tr>
<th>( k ) ( (\text{cal/cm sec K}) )</th>
<th>( A )</th>
<th>( B )</th>
<th>( C )</th>
<th>( D )</th>
<th>( E )</th>
<th>( F )</th>
</tr>
</thead>
<tbody>
<tr>
<td>Compressibility ( Z )</td>
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<td>-0.465274E-01</td>
<td>0.972123E-02</td>
<td>0.417402E-02</td>
<td>-0.536830E-03</td>
<td>N/A</td>
</tr>
<tr>
<td>Dynamic viscosity ( \mu ) (PaS)</td>
<td>0.5781887E-04</td>
<td>0.4438221E-03</td>
<td>-0.1020840E-03</td>
<td>0.1688754E-04</td>
<td>-0.8622324E-06</td>
<td>-0.2239193E-09</td>
</tr>
<tr>
<td>Specific Heat ( C_p ) (cal/gr K)</td>
<td>0.164992E+00</td>
<td>0.156336E+01</td>
<td>0.552429E+01</td>
<td>0.879873E+01</td>
<td>0.412806E+01</td>
<td>N/A</td>
</tr>
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<td>Thermal conductivity ( k ) (cal/cm sec K)</td>
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<td>Prandtl number ( Pr )</td>
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<td>0.624393E+00</td>
<td>0.1903222E+00</td>
<td>-0.2182155E-01</td>
</tr>
</tbody>
</table>

Table 8.1: Polynomial coefficients of equilibrium air curve fits (at pressure 1atm and temperature <1000K)

8.3 Modelling of Burnthrough Tests.

The burnthrough tests were divided to two campaigns, the first one was using the ISO2685 Propane Burner that were performed in the premises of the Health and Safety Laboratory in the UK and comprised of the big flat panels having size of 400x400mm and the second campaign was using the lab-scale AML butane burner with samples 100x100mm. The first campaign tests were modelled using the single-
stage kinetics while the second campaign utilized the more accurate and realistic multi-stage approach.

In all models the same element type was used, specifically the SHELL131. The SHELL131 [8.7] is a 3-D layered shell element having in-plane and thru-thickness thermal conduction capability. The element has four nodes with up to 32 temperature degrees-of-freedom at each node. The conducting shell element is applicable to a 3-D, steady-state or transient thermal analysis. Also the SHELL131 generates temperatures that can be passed to structural shell elements if thermomechanical analysis is opted. Quadratic variation in temperature through each layer was used for all these transient analyses since all materials are strongly temperature dependent. Since SHELL131 is capable of modelling through thickness physical changes using multiple layers through the thickness. The number of layers used in every simulation matched the number of physical layers used during the samples manufacturing.

8.3.1 ISO2685 Samples Models.

8.3.1.A Aluminum panels

Figure 8.10a shows the back-face temperature distribution of the aluminium 2024-T3 plate at the time of burn-through (t=173sec) as calculated by the FE-model. During the execution of the burn-through tests, it was observed that burn-through occurred between 145 and 210sec depending on the specimen. Figure 8.10b shows a pseudo-colour photo image of the back-face of an experimentally tested Aluminium plate at 210sec, where burn-through has occurred (visible crack) and part of the plate has
melted and bowed away from the flame. Figure 8.11a shows the front and rear face of typical aluminium specimen after the application of the ISO2685 burn-through test.

Figure 8.10  (a) Aluminum plate FE results, burn-through occurred at t=173sec, (b) Pseudo-colour photo of the back face of an aluminium specimen when burn-through occurred, t=210sec.

Figure 8.11  Front and back face of the specimens after the burn-through test, (a) aluminum and (b) CFRP.

8.3.1.B  CFRP panels

Figure 8.12a,b show the calculated back-face temperature of the CFRP plate during the application of the progressive decomposition FE-model on CFRP laminates at the time instance of 30sec and 900sec. The symmetric distribution of temperature across
the vertical axis indicates the symmetric configuration of the burn-through set up, which was analysed. Since the temperature did not exceed the oxidation temperature of carbon reinforcement burn-through did not occur during the analysis.

Figure 8.12c,d present the pseudo-colour images taken during the ISO2685 burn-through test applied on a CFRP laminate at the same time instances. As it was found by the progressive decomposition FE analysis and confirmed during the ISO test, burn-through did not occur after the 900sec exposure period as it is demanded by the ISO2685:1998(E) Standard [8.8]. However, it has to be mentioned that during the actual tests severe flaring and smoking was observed coming from the front face of the specimens that is in “direct contact” with the flame of the burner and which is an indication of the resin decomposition. This flaring and smoking appeared to have an increased rate as exposure time was elapsing, peaked and then subsided and finally stopped. This consists an additional indication that most (if not all) of the polymer resin had decomposed. Figure 8.11b show the front and rear face after the application of the fire burn-through test of one of the CFRP specimens.

Figure 8.12 CFRP plate back face temperature at (a) t=30sec, (b) t=900sec, Pseudo-colour image of the back face of a CFRP specimen at (c) t=30sec and (d) t=900sec.
Figures 8.13 present the through the thickness decomposition of CFRP laminas at $t=300\text{sec}$. The manufactured CFRP panels were quasi-isotropic plates that consisted of ten layers and were all modelled separately in the FE decomposition model. In Figure 8.13 the decomposition degree distribution along every layer is presented, starting from the first one (“hot” face) until the last one (rear face). In the CFRP results it is more clearly evident the insulating mechanism of the resin decomposition as we go “deeper” through the thickness of the material.

![Figure 8.13 Through the thickness decomposition degree of the layers of a CFRP specimen at $t=300\text{sec}$.

In Figure 8.14 the decomposition evolution chronicle of the rear face (last layer) of the CFRP specimen is presented. One can observe the progressive matrix decomposition as time elapses during the simulation of the ISO2685 burnthrough tests.
Figure 8.14  Decomposition degree evolution chronicles of the rear face of the CFRP specimens.

8.3.1.C  GLARE panels

Figure 8.15 shows the back-face temperature of the GLARE plate during the application of the progressive decomposition and aluminium melting FE-model at the time instance of 30sec and 900sec. A symmetric temperature distribution across the vertical axis is also demonstrated. Figure 8.16 presents the pseudo-colour images taken at the same time instances during the ISO2685 burn-through tests applied on a GLARE laminate. As it can be seen in the numerical model, also as during the test, burn-through did not occur after the 900sec exposure period that the ISO Standard [8.8] demanded. It has to be mentioned though that during the tests flaring and smoking was observed both on the front and back face. The Aluminum skin of the front face of the GLARE was cracked some time after the application of the propane burner fire on it. Thus the underlying GFRP layers started to decompose producing decomposition gases.
Modelling Work.

Figure 8.15 GLARE plate back face temperature at: a) $t=30$ sec and the end of the simulation time $t=900$ sec.

Figure 8.16 Pseudo-colour image of the back face of a GLARE specimen at $t=30$ sec and $t=900$ sec respectively.

The back face remained intact during the 900 sec burn-through test. However, the surface treatment that was applied during the preparation of the GLARE laminates vaporized as the back face temperature rose up, and this was the source of the smoking observed.

The flame stream observed in the pseudo-color image at $t=900$ sec in Figure 8.16 is not burnthrough BUT because of size incompatibility between the test-rig and the supplied GLARE samples. This “gap” was filled with ceramic glue that unfortunately failed during the burnthrough test affecting the IR-images and also has affected the quantitative credibility of the GLARE test results. Figure 8.17 presents the front and back face before and after the test of one the GLARE specimens, the ceramic glue and the flame stream that penetrated it are visible.
Figure 8.17  Front (left), back (right) face of a GLARE specimen: a) before and b) after the ISO2685 burn-through test.

Figures 8.18 present the through the thickness decomposition of GLARE samples at a given exposure time, $t=450$sec. The GLARE material that was used was a GLARE-3 5/4 that consisted of four GFRP layers, each layer is made of two GFRP laminates and 5 Aluminum 2024-T3 sheets. In the FE-model each laminate was modelled separately, so in Figure 8.18 the decomposition degree of every laminate of each layer are presented. As it can be observed as we proceed “deeper” through the thickness, the resin decomposition degree decreases. The first two insert images (1a,1b) are the laminates of the first GFRP layer, the other two (2a,2b) are for the next one, and so on until all 4 layers (8 laminates) are completed.
Figure 8.18 Through the thickness decomposition contours of the GFRP layers of a GLARE-3 5/4 specimen at t=450sec.

Table 8.2: GLARE samples details.

| GLARE-3 5/4 | 5 aluminum layers  
Layer thickness: 0.4mm  
4 GFRP layers  
Layer thickness: 0.25mm  
Overall thickness: 3mm | Supplier: Wentzel Engineering |

8.3.1.D Discussion of results

A comparison between results concluded by the progressive decomposition FE model and the experimental results obtained using the ISO2685:1998(E) [8.8] fire burnthrough tests are given in Figures 8.19,8.20 for Aluminium and CFRP laminates respectively. In both the graphs the temperature of the mid point of the rear face of the analyses/tested panel is provided versus the time of exposure to the given propane fire
applied against the front face of the panel. The dashed lines plotted on all graphs correspond to the solidus and liquidus lines of the Aluminium Alloy.

![Graph showing temperature vs. time](image)

Figure 8.19 Response of the Aluminium panels to the thermal load FE melting model and experimental results.

As a first remark one may address the consistency between experimental and numerical results in the temperature behaviour of the materials under investigation. However, a temperature over estimation can be observed for the CFRP material. The “frozen-flux” thermal load applied for the whole duration of the simulation explains this, since it is the worst case thermal scenario. In addition, due to the assumptions made, phenomena like delamination that act insulatively have been neglected. An argument that supports the applicability of this analysis concept is that although overestimated, the back face temperature fields of the CFRP material, remain below the temperature region defined by the Aluminium solidus-liquidus lines. Another supporting argument is that the decomposition process does play a key-role in the phenomena, as presented in Figure 8.20 when decomposition was ignored the temperature results were totally unrealistic.
Finally, one interesting remark coming from the FE analysis is the front-to-rear temperature gradient that these 3 structural materials, Aluminum 2024-T3, CFRP and GLARE, induce. These results are presented in Figure 8.21. The aluminium plate is practically thermally thin so no temperature difference between the front and back face is observed. The GLARE material though offers an encouraging temperature gradient. Although, it must be mentioned here that the around 35K gradient can not be considered as a valid quantitative figure since as it was mentioned earlier the flame stream that broke through the ceramic glue barrier has “flowing” in the path of the spring-loaded contact thermocouple measuring the temperature at the center of the rear face and it definitely affected the temperature profile of the rear face of the samples. Furthermore, the GLARE insulative behaviour is more complex to model since there is delamination between the glass layers and the aluminum sheets feature not considered in this work, that would require thermomechanical models.

The CFRP behaviour although, is interesting. As time elapses, a significant rise in the front-to-back temperature gradient is observed and a constant value is reached. It should be mentioned again here that this 280K gradient is the “product” of a 3mm thick CFRP panel, ISO2685 heat-flux for 900 seconds. This gradient describes clearly the insulating behaviour of the decomposed composite material.
8.3.2 AML Burner.

The selected geometry was a flat panel with dimensions 100x100 mm. The mesh budget consisted of 400 shell elements, while the selected element type was the SHELL131 layered element as already has been mentioned. Detail of the geometry and mesh can be seen in Figure 8.22. The number of layers used in the models were 24 layers, in accordance to the number of layers used in the manufactured samples.
8.3.2.A CFRP samples.

During the tests it was observed that the LY-Ref samples failed after approximately 50sec of exposure to the propane burner. Resin burnt-off completely leaving the carbon fiber reinforcement intact with NO residual load bearing capability since the char residue extent was limited. Thus these samples failed to pass the test. On the other hand, the AP423 and MWCNT ones showed significant fire improvement, NO burnthrough occurred, the porous char formation acted as a promising heat shield and its structure promoted the load bearing capacity of the samples. Conclusively, both AP423 and MWCNT samples passed the test. These were the reasons that the LY-Ref simulations were limited to 100sec and the AP423 and MWCNT for the whole 300sec duration.
In Figures 8.23,8.24 the response of the LY-Ref and AP423 models against the experimental results are presented. Failure of the LY-Ref samples at approximately 50sec is observed whilst the modified samples withstood. In Figure 8.25 contours of the elements-$A$, $B$, $C$, $D$ molar fractions distribution for the LY-Ref sample are presented. These contours refer to the first layer of the sample (flame impinged one) at the average failure time (50sec) of the samples. One can observe that very little char, around 10-20% is present. Finally, in Figure 8.26 one can observe the accuracy of the model compared to the actual test, for the LY-Ref material. The resin burn-off pattern is visible on the failed sample leaving the carbon fibers exposed, the heat-affected zone where the intermediate products (elements $B,C$) is also visible and the unaffected zone also is clearly shown. In comparison, the contour is a screenshot of the transient analysis when failure of the sample occurred (50sec) and presents the resin distribution (element-$A$) molar fraction distribution. The aforementioned three zones are also visible.
Modelling Work.

Figure 8.24  Rear face temperature results for the AP423 composite specimens.

Figure 8.25  Molar fractions contours of the first layer, at 50sec. LY-Ref specimen.

Figure 8.26  Front face contours of remaining resin (element-A) molar fraction at 50sec. Comparison against burnt LY-Ref specimen.
8.3.2.B Discussion of results.

The effect of the ammonium poluphosphate additive Exolit AP423 to the overall fire response of the polymer appeared to be significant whilst the extra effect of the multiwall carbon nanotubes did not have such a drastic contribution. One reason that the AP423 has such a major effect is that it modifies the reaction network of the neat polymer (LY-Ref). Energy is “consumed” as resin and the ammonium polyphosphate break down to lower organic compounds and phosphate esters prior to complete decomposition into carbonaceous char residue. As the AP423 material starts to decompose, char (element-D) is produced in greater amounts and at more stages compared to the LY-Ref unmodified resin. Charring is enhanced by the presence of the CNT’s.

One may wonder, “fire properties are improved and fire resistance enhanced, although, what are the effects on the mechanical properties of the polymer matrix?” Typically fire retardant additives reduce the mechanical performance of the matrix materials, contrary to this, the post-fire load bearing capability of the AP423 and MWCNT carbon fiber composite samples led to perform mechanical tests on NON-degraded resin samples. Compact tension, CT, tests showed significant increase of fracture energy $G_C$ and fracture toughness $K_C$. Specifically, for the MWCNT batch a 708% and 258% increase, in fracture energy and toughness respectively, was observed, Figure 8.27. Another synergic action of the multiwall carbon nanotubes is that they act as “nano-reinforcement” stabilizing the char structure and not promoting exfoliation of the protective char layer.
Figure 8.27 Fracture energy $G_C$ and fracture toughness $K_C$ of resin samples.

### 8.4 References


CHAPTER 9  Full-scale Fuselage Fire Burnthrough Response.

So far the burnthrough response of aerostructures was limited to coupon, samples and medium size flat panels. A more realistic approach was preferable, meaning a mathematical model of a real size test. The certification tests conducted by the FAA are for full size fuselage sector under the fire load of a burning jet-fuel pan pool-fire. A burning jet-fuel pool fire is a completely complex phenomenon on its own, combining it with a decomposing fuselage structure would make the modeling approach even more difficult to simulate if not impossible. Detailed modeling of the burning liquid fuel requires tons of data not always available for the pool-sizes under investigation, so generalized data were used from literature. Also, the main characteristic of a jet-fuel (kerosene) pool fire is that the flames are not clear, on the contrary, great amount of smoke is produced making burning modeling and radiative heat transfer to the fuselage even more of a challenge to model. With these in mind it was decided to try and tackle this full-scale approach by somehow simplifying the modeling approach. Instead of liquid fuel combustion, an equal hot air stream with
mass flow, velocity and temperature properties taken from literature data was performed.

### 9.1 Pan pool-fire

#### 9.1.1 Selected Aerostructure

The possibility of designing, from the beginning, a representative aerostructure for the pool fire burnthrough scenario was viable, but it was decided in terms of time efficiency and to select an already designed and selected aerostructure, Figure 9.1.

![Selected aerostructure. Typical A320 fuselage geometry structure.](image-url)

Figure 9.1  Selected aerostructure. Typical A320 fuselage geometry structure.
9.1.2 Pool-fire scenario

The pool-fire scenario should not be arbitrary. In order to be correct and in accordance to a typical testing procedure, it was decided to follow the DOT/FAA/AR-98/52 full scale burn through test rig, [9.1].

Figure 9.2  DOT/FAA/AR-98/52 full scale burn through test rig layout and basic dimensions.

The dimensions selected for this modelling approach was a blend of the FAA Standard and the selected aerostructure. The fuselage diameter, length, dimensions of the pan, and the vertical and horizontal locations are displayed in Table 9.1. The fuel that was used was kerosene.
Table 9.1: Model dimensions

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>fuselage diameter, $D$</td>
<td>4000 mm</td>
</tr>
<tr>
<td>fuselage length, $L$</td>
<td>6000 mm</td>
</tr>
<tr>
<td>fuselage center ground distance, $H$</td>
<td>2500 mm</td>
</tr>
<tr>
<td>pan length, $x$</td>
<td>3000 mm</td>
</tr>
<tr>
<td>pan width, $b$</td>
<td>2400 mm</td>
</tr>
<tr>
<td>pan vertical distance from the ground, $h$</td>
<td>500 mm</td>
</tr>
<tr>
<td>Pan vertical distance from fuselage center, $t$</td>
<td>2000 mm</td>
</tr>
<tr>
<td>pan horizontal distance from fuselage center, $s$</td>
<td>1000 mm</td>
</tr>
</tbody>
</table>

Figure 9.3 Schematic of the pan pool-fire scenario. a) side-view, b) top view.
9.2  Pool-fire modelling approach

9.2.1  Pool-fire theory

Before performing any modeling work, the phenomenon had to be comprehended so an extensive literature review was performed trying to understand the mass and energy magnitudes that are released during a pool fire. Also the flow characteristics of the pool fire had to be quantified, towards this direction the work performed by Drysdale [9.2] and Babrauskas [9.3] gave realistic and accurate insight.

For the selected dimensions mentioned in section 9.1 the pool-fire surface area is $A_f=7.2\,\text{m}^2$, the equivalent circular pool diameter is $D_{eq}=3.03\,\text{m}$. The energy release rate of a pool fire is given by the expression:

$$\dot{Q}_C = x \cdot \dot{m}^* \cdot A_f \cdot \Delta H_c$$  \hspace{1cm} (9.1)

where:

- $x$: a factor ($<1.0$) included to account for incomplete combustion.
- $\dot{m}^*$: burning rate per unit area (kg/m$^2$sec).
- $A_f$: fuel surface area (m$^2$).
- $\Delta H_c$: volatiles heat of combustion (kJ/kg).
- $\dot{Q}_C$: energy release rate (kW).

According to Drysdale [9.2], circular or square pool-fires feature three distinct regions: a) flame region, b) intermittent, c) plume. These regions are divided by the parameter defined by the expression $z/\dot{Q}_C^{2/5}$, where $z$ is the vertical distance from the fuel surface.
For the aforementioned kerosene pool-fire the energy release rate, $\dot{Q}_c$ is 12.54MW. According to the simplified modeling approach of the pool fire, where no combustion will be modeled and the flame will be replaced by a hot air stream, the average centerline vertical velocity and centerline temperature are needed as input and are expressed by the following expressions Eq.(9.2),(9.3) taken from [9.2],[9.3]:

\[
\frac{u_0}{\dot{Q}_c^{1/5}} = k \cdot \left[ \frac{z}{\dot{Q}_c^{2/5}} \right]^n \tag{9.2}
\]

\[
\frac{2g\Delta T_0}{T_0} = \left( \frac{k}{C} \right)^2 \cdot \left[ \frac{z}{\dot{Q}_c^{2/5}} \right]^{2n-1} \tag{9.3}
\]

where,
- $u_0$: vertical velocity (m/sec).
- $z$: distance from fuel surface (m).
- $\Delta T_0$: temperature excess of ambient (K).
- $T_0$: ambient temperature (K).
The parameters in Eq. (9.2), (9.3) are not the same on every region of the pool-fire, their values vary according to Table 9.2.

<table>
<thead>
<tr>
<th>Region</th>
<th>$k$</th>
<th>$n$</th>
<th>$z/\dot{Q}_c^{2/5}$ (m/kW$^{2/5}$)</th>
<th>$C$</th>
</tr>
</thead>
<tbody>
<tr>
<td>Flame</td>
<td>6.8 m$^{1/2}$/s</td>
<td>1/2</td>
<td>&lt;0.08</td>
<td>0.9</td>
</tr>
<tr>
<td>Intermittent</td>
<td>1.9 m/kW$^{1/5}$s</td>
<td>0</td>
<td>0.08-0.2</td>
<td>0.9</td>
</tr>
<tr>
<td>Plume</td>
<td>1.1 m$^{4/3}$/kW$^{1/3}$s</td>
<td>-1/3</td>
<td>&gt;0.2</td>
<td>0.9</td>
</tr>
</tbody>
</table>

Table 9.2: Pool-fire region parameters [9.2][9.3].

The regions for the pool-fire of interest in the current work are defined as presented in Table 9.3:

<table>
<thead>
<tr>
<th>Region</th>
<th>$z$ (m)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Flame</td>
<td>&lt;3.49</td>
</tr>
<tr>
<td>Intermittent</td>
<td>3.49&lt;z&lt;8.72</td>
</tr>
<tr>
<td>Plume</td>
<td>&gt;8.72</td>
</tr>
</tbody>
</table>

Table 9.3: Pool-fire regions.

After the regions had been defined for the selected pool-fire, then the centreline average temperature and velocity magnitudes had to be estimated. Those values were extracted from nomograms developed by Drysdale [9.2] and Babrauskas [9.3] and are presented in Figure 9.5.
Figure 9.5  Centerline average a) Temperature, b) Velocity nomograms [9.2] [9.3].
9.2.2 Modelling

According to Table 9.3 the whole fuselage panel is inside the Flame region. The fluid mechanics modelling of the liquid fuel pool-fire is essential to determine the thermal load distribution on the fuselage surface to use it as boundary condition in the FE burnthrough thermal models.

Liquid fuel pool-fire modelling is very demanding; phase change calculations (kerosene vaporization) make these models complex. Two simplified approaches can be proposed:

- A first approximation considers a hot air stream and NO combustion.
- Considering gas combustion.

The first approach assumes that the flame is a stream of hot air having properties (average velocity, temperature, flow rate, etc) extracted by the already mentioned methodology from nomograms. Whilst the later one, neglects the phase change (kerosene vaporization] and considers gas mixture combustion. In the current work it was selected to work with the simplest of the two approaches and tackle the pool-fire phenomenon with the hot air stream approach.

In terms of reducing the computational demands the assumption that the pool-fire flame region is symmetric on the vertical axis, half the geometry was modelled.

9.2.2.A Hot-air stream.

According to Drysdale [9.2], based on field tests, above the surface of the liquid kerosene pool surface, there is a layer of vapors burning. The thickness of this layer varies between 25mm and 30mm. Assuming an average thickness of 27.5mm, the fire centerline velocity and temperature for the given distance \(z=0.0275\text{m}\) are calculated as \(u_0=1.13\text{m/sec}\) and \(T_{\text{FLAME}}=1283\text{K}\) for a 300K ambient temperature using Eq.(9.2),(9.3) and nomograms of Figure 9.5.
These were the boundary conditions used in the developed CFD model, velocity inlet of air having the flame temperature mentioned before. Also gravitational effects were considered.

### 9.2.3 Results

In the following figure, Figure 9.6 the simplified geometry of the fuselage structure (consisting only of the aluminium skin and the floor) and the fuel pool are presented.

![Fuselage and kerosene fuel pan mesh.](image)

Figure 9.6 Fuselage and kerosene fuel pan mesh.

In Figure 7 contours of the Total temperature distribution on the fuselage surface and on the symmetry plane are presented. Because there is an opening between the fuel pan and the fuselage, part of the flame is split and flows on the belly of the fuselage. The temperature distribution on the fuselage proves really interesting.
Full-scale Fuselage Fire Burnthrough Response.

(a) Contours of Total Temperature (k) ANSYS FLUENT 12.0 (3d, pbns, lam)

(b) Contours of Total Temperature (k) ANSYS FLUENT 12.0 (3d, pbns, lam)
9.3 FE burnthrough modelling

9.3.1 Building the model.

The thermal conditions on the fuselage surface (temperature, or heat-flux, or heat transfer coefficient distribution) calculated from the CFD model were then extracted and applied on the ANSYS FE burnthrough model. The benefit is that there is NO need for results interpolation to match the two meshes, there is the possibility to extract the CFD fuselage surface mesh and import it in the ANSYS FE environment.

Once the geometry and thermal loads were imported in the FE model, the next step was to define the boundary conditions on the internal face of the fuselage and the appropriate material model to be applied. For the case of investigating the current design trend of metallic fuselages the aluminum model should be used, while for the
composite or hybrid-GLARE ones the progressive degradation methodologies used for the flat panels is applicable.

![Fuselage structure mesh in ANSYS FE environment](image)

Figure 9.8 Fuselage structure mesh inside the ANSYS FE environment.

### 9.3.2 Conclusions

One may say that the assumptions and simplifications made during this full-scale approach make the whole concept non-realistic. The answer is both yes and no. YES, the modeled approach differs to a great extent from the real pool-fire test but also NO, because for such a complicated event of the liquid pool-fire ignition, burn and fuselage burnthrough eventually, one has to start simple and increase complexity gradually.

In the current work a simplified approach was selected and presented since it is the first work on such an event. The possibility of the different numerical codes, FE and CFD, interaction had to be investigated. The pool-fire fuselage burnthrough event is actually a Fluid-Structure-Interaction problem with different time-scales for every phase, vaporization of the liquid kerosene is in the seconds scale, kerosene vapors combustion is in the millisecond scale while the burnthrough is in the minutes scale. All these complicate the solution and as a result the research engineer has to perform a trade-off and balance the importance of each event.
The simplified geometry can provide a good approximation of the real conditions. A more detailed fuselage geometry could be described and this demanding model would provide more detailed results and would account for the “cooling fin” effect of the frames, stringers and floor structure of the fuselage. This “heavy” detailed numerical model could provide estimation of the fuselage structure more prone to fire burnthrough after a crash landing and spilt fuel ignition.

![Detailed fuselage structure](image)

Figure 9.9 Detailed fuselage structure imported for meshing in the GAMBIT environment.

### 9.4 References


In this chapter the benefit analysis of Cherry and Warren [10.1] is presented in short. The objective of their analysis was to assess the potential benefits, in terms of reduction of fatalities and injuries, resulting from improvements in fuselage burnthrough resistance to ground pool fires. The process employed for assessing benefit is considered to give reasonably accurate and consistent results within the limitations imposed by the available data. The methodology gives a reasonable assessment of the tolerance on the predicted levels.

Fire hardening of fuselages will provide benefits in terms of enhanced occupant survival and may be found to be cost beneficial if low-cost solutions can be found. The maximum number of lives saved per year in worldwide transport aircraft accidents, over the period covered by the data, was assessed to be 12.5 for the aircraft in its actual configuration and 10.5 for the aircraft configured to later airworthiness requirements. In detail their work can be found in Appendix A.
10.1 Introduction.
This work contains the method and results of a benefit analysis carried out on fuselage hardening for burnthrough protection against large ground pool fires. A number of past accidents have been identified which are considered to have involved fire penetration of the passenger cabin with a consequential threat to occupant survival. For each of the accidents identified, the assessed benefit in terms of the reduction in number of fatalities and injuries has been derived assuming improvements to the fire hardening properties of the aircraft fuselage.

10.2 Objectives.
The objective of this study was to assess the potential benefits, in terms of reduction of fatalities and injuries, resulting from improvements in fuselage burnthrough resistance to pool fires by:

- Using the International Cabin Safety Research Technical Group’s Survivable Accidents Database to identify and extract detailed data for those aircraft accidents where fuselage burnthrough was an issue in the survivability of the occupants.
- Analyzing each accident in depth to assess the number of lives and injuries that might be saved by a fire-hardened fuselage.
- Assessing the benefits in the context of all survivable aircraft accidents.

10.3 Method.

10.3.1 Selection of accidents.
Survivable or potentially survivable accidents on scheduled or nonscheduled passenger carrying transport aircraft were selected for inclusion in the analysis based on the following definition of a burnthrough accident:

“An aircraft accident where the fuselage skin was penetrated by an external fire while live occupants were on board.”

This definition would exclude instances where the fuselage was consumed by fire after the evacuation period was complete and those accidents where the fuselage burnt through from the inside (burn-out).
Benefit analysis of the postcrash burnthrough hardening.

For the purposes of this analysis, the definition of a survivable accident is:

"An aircraft accident where there were one or more survivors or there was potential for survival."

Only survivable or potentially survivable accidents, in which there were fire injuries, were selected for analysis. Accidents in which there were no fire related injuries were not analyzed since there could be no benefit to be gained from a fire-hardened fuselage. The accidents were selected using the Survivable Accidents Database of the International Cabin Safety Research Technical Group and from information contained in [10.2].

Where accident information did not explicitly state that burnthrough had occurred, it was necessary to make the assumption that a pool fire existing outside an intact portion of fuselage would burnthrough, given the nature of the intensity of the fire.

Seventeen accidents were identified as listed in Table 10.1. The relationship between this subset and all fatal accidents on the database is shown in Figure 10.1.

<table>
<thead>
<tr>
<th>Date</th>
<th>Location</th>
<th>Aircraft</th>
</tr>
</thead>
<tbody>
<tr>
<td>14-Sep-1993</td>
<td>Warsaw</td>
<td>A320</td>
</tr>
<tr>
<td>01-Feb-1991</td>
<td>Los Angeles</td>
<td>B737</td>
</tr>
<tr>
<td>31-Aug-1988</td>
<td>Dallas</td>
<td>B727</td>
</tr>
<tr>
<td>26-Jun-1988</td>
<td>Habsheim</td>
<td>A320</td>
</tr>
<tr>
<td>22-Aug-1985</td>
<td>Manchester</td>
<td>B737</td>
</tr>
<tr>
<td>30-Aug-1984</td>
<td>Douala</td>
<td>B737</td>
</tr>
<tr>
<td>07-Dec-1983</td>
<td>Madrid</td>
<td>B727</td>
</tr>
<tr>
<td>13-Sep-1982</td>
<td>Malaga</td>
<td>DC10</td>
</tr>
<tr>
<td>07-Oct-1979</td>
<td>Athens</td>
<td>DC8</td>
</tr>
<tr>
<td>17-Dec-1978</td>
<td>Hyderabad</td>
<td>B737</td>
</tr>
<tr>
<td>15-Mar-1974</td>
<td>Teheran</td>
<td>Caravelle</td>
</tr>
<tr>
<td>30-Jan-1974</td>
<td>Pago Pago</td>
<td>B707</td>
</tr>
<tr>
<td>22-Jan-1973</td>
<td>Kano</td>
<td>B707</td>
</tr>
<tr>
<td>20-Dec-1972</td>
<td>Chicago</td>
<td>DC9</td>
</tr>
<tr>
<td>18-Apr-1972</td>
<td>Addis Ababa</td>
<td>SVC10</td>
</tr>
<tr>
<td>08-Apr-1968</td>
<td>Heathrow</td>
<td>B707</td>
</tr>
<tr>
<td>16-Feb-1967</td>
<td>Menado</td>
<td>L188</td>
</tr>
</tbody>
</table>

Table 10.1: List of burnthrough accidents identified.
10.3.2 Accident Scenarios.

The severity of hazard in an accident can vary markedly throughout the aircraft. Experience has shown that considering occupant injuries on a “whole” aircraft basis can be misleading when assessing the effects of survivability factors. It is therefore necessary to divide the aircraft into “scenarios”. A scenario is defined as:

“That volume of the aircraft in which the occupants are subjected to a similar level of threat.”

A similar level of threat need not necessarily result in the same level of injury to occupants. The extent of injury sustained can vary with numerous factors including age, gender, adoption of the brace position etc. Furthermore, the threat to occupants can vary over relatively small distances. For example, a passenger may receive fatal injuries because of being impacted by flying debris, and a person in an adjacent seat may survive uninjured. Dividing accidents into scenarios provides a more meaningful basis on which to analyse accidents than considering the whole aircraft due to the marked variation in survival potential with occupant location.

The flight deck and flight attendant areas are generally considered as separate scenarios. The flight deck often has the potential for greater impact damage, and crewmembers usually have full harness restraints. Furthermore, sliding cockpit windows in the area provide a nearby method of egress. The flight attendant areas are normally considered as a separate scenario from the passenger cabin due to the significant differences in seating, restraint systems, and exit availability. For these
Benefit analysis of the postcrash burnthrough hardening.

reasons all analytical work carried out during this study has been based on carrying out assessments for each scenario.

### 10.3.3 Survivability chains.

A mathematical model, known as a Survivability Chain, has been developed such that the overall effect on survivability may be determined from improvements made to survivability factors, taking into account injuries that may be sustained by occupants. A Survivability Chain is derived for each scenario in each accident. A scenario where injuries are sustained due to impact and subsequent fire has been modelled using two levels in the Survivability Chain. An example of the model and the effects of improvement in injuries and fatalities resulting from changes to fuselage fire hardening are shown in Figure 10.2.

![Figure 10.2 Example survivability chain [10.1].](image)

There are therefore:

- 45 uninjured survivors.
- 25 injuries, 10 as a result of the impact, 10 as a result of the fire, and 5 seriously injured as a result of the impact and fire.
- 30 fatalities, 20 as a result of the impact and 10 as a result of the fire (5 of whom sustained nonfatal injuries from the impact).

If improvements were made to fuselage fire hardening, such that it was assessed there were now only 2 fatalities and 6 seriously injured of the 20 impact injured
occupants and only 2 fatalities and 7 seriously injured of the 60 impact survivors, then the survivability chain becomes as shown in Figure 10.3.

![Figure 10.3 Example of survivability chain showing possible improvements in survivability as a result of fuselage fire hardening [10.1].](image)

Hence the improvement to the fuselage fire hardening results in:

- 51 uninjured survivors.
- 25 serious injuries, 12 as a result of the impact, 7 as a result of the fire, and 6 as a result of the impact and fire.
- 24 fatalities, 20 as a result of the impact, and 4 as a result of the fire (2 of whom sustained nonfatal injuries from the impact).

The overall situation is summarized as follows:

<table>
<thead>
<tr>
<th></th>
<th>Survivors</th>
<th>Injuries</th>
<th>Fatalities</th>
</tr>
</thead>
<tbody>
<tr>
<td>Prior to Improvement:</td>
<td>45</td>
<td>25</td>
<td>30</td>
</tr>
<tr>
<td>Post Improvement:</td>
<td>51</td>
<td>25</td>
<td>24</td>
</tr>
</tbody>
</table>

10.3.4 Statistical modeling.

Software has been developed to use this model in a mathematical representation of an accident using Monte Carlo Simulations. This simulation enables an assessment to be made of the change in numbers of survivors, injuries, and fatalities resulting from predictions of the range of improvements that may be possible from changes to a survivability factor. For each scenario, a numerical assessment is made of the effect
Benefit analysis of the postcrash burnthrough hardening.

on the number of fatalities and injuries because of changes to fire hardening of the fuselage. The assessment results in a prediction of the highest, median, and lowest number of fatalities and injuries that could reasonably be expected from the change.

From the example described in Section 10.3.3, the best (or median) assessments were made of the improvement in the number of fatalities and injuries resulting from enhanced fuselage burnthrough protection. When making this determination an assessment would also be made of the maximum and minimum number of fatalities and injuries that are likely to result from a change in the fuselage fire hardening. It is then assumed that there can be 100% confidence that the fatalities and injuries will lie in the range from the maximum to the minimum. The software makes random selections over the range 0% to 100% to arrive at a particular number of fatalities and injuries. From this, a re-evaluation of the number of survivors may be made using the Survivability Chain generated for the accident scenario. This is then compared with the actual number of survivors of the accident. The iterations are then carried out many times to generate a distribution. From this distribution the 2½, 50, and 97½ percentile values are selected to represent a range of the likely improvement in fatality rate for fire-hardened fuselages. This simulation process is described in detail in Appendix D.

It is recognized that the models are not perfect representations of an accident nor are the statistical assessments totally accurate. However, they will provide a better assessment of the likely impact of improvements to fuselage fire hardening than would otherwise be derived from a simple estimate of the resultant change in number of survivors.

10.3.5 Later requirements.

Assessments of the improvements in fatality rate were carried out for the accidents based on the aircraft standard, and operating requirements, at the time of the accident. Each accident was then re-analyzed taking into account the improvements that might have been made to numbers of fatalities and injuries if the aircraft had been configured to the latest requirements. The benefit information, relating to aircraft configured to the latest requirements, was entered onto a separate computer database.
and the statistical analysis described in Section 10.3.4 repeated. The later requirements used to reassess the accidents were:

- Floor proximity lighting/marking
- Seat blocking layers
- Fire hardening of cabin interior materials
- Improved access to type III exits

10.3.6 Variation of benefit with burnthrough time.

The benefits of a fire-hardened fuselage will depend on the extension to burnthrough times provided. Since the Cherry-Warren study [10.1] does not make any assumptions about how the burnthrough protection is achieved, it was necessary to employ a range of burnthrough times and repeat the analysis for a number of different values. It was difficult to assess from the accident rationales exactly when the burnthrough penetration occurred; therefore the assessments were based on incremental burnthrough improvement times.

The process of assessing high, median, and low values for fatalities and injuries was repeated using the following increases in fuselage burnthrough times; 30, 120, 240, and 480 seconds. The statistical model was run for each of the protection times and for both the actual aircraft configuration and the aircraft configured to later certification and operating requirements.

10.3.7 Accident analysis methodology.

The assessments were carried out by two analysts. Each of the accidents was analyzed by analyst-1. After the assessment was complete, there was a discussion between the analysts on each accident. Analyst-1 then made a revised assessment and Analyst-2 made another, independent assessment. The results from these assessments were combined by using the largest extremes of the predicted number of fatal injuries and taking the average of the medians as shown in Figure 10.4. This technique was used to reduce the effect of any bias either analyst might have had in making his assessment.
10.4 Results.

All of the results contained in this section were based on the assumption that the 17 accidents identified were the only fatal burnthrough accidents that have occurred over the period. Subsequent sections of this work discuss the validity of this assumption and the most likely benefit to be achieved from enhancements to fuselage burnthrough protection.

10.4.1 Benefits definitions.

The following definitions are used for the benefits assessed by this analysis. An improvement in fatality rate is defined as:

“The reduction in the number of fatalities divided by the total number of occupants aboard.”

An improvement in injury rate is defined as:

“The reduction in the number of serious and fatal injuries divided by the total number of occupants aboard.”

In order to derive the total number of lives saved in all survivable accidents in which there were fatalities, the fatality rate must be multiplied by the total number of occupants, i.e., 25001. (Refer also to Figure 10.1.) In order to derive the number of lives saved per year, the total number of lives saved must be divided by the number of years the data encompassed, i.e., 28. A similar process is adopted for deriving the total number of injuries from injury rate.
10.4.2 Benefit based on the aircraft standard at the time of the accident.

The high, median, and low prediction of effect on fatality rate was based on 9999 iterations of all burnthrough-related accidents identified on the database using the method described in Section 10.3. These predictions were based on the aircraft configured to the standard at the time of the accident and took no account of the improvements offered by the introduction of later requirements. The values shown in Table 10.2 represent the incremental change in fatality and injury rate predicted for all survivable accidents in which there were fatalities (25001 occupants, refer to Figure 10.1).

<table>
<thead>
<tr>
<th>Additional Burnthrough Protection Time</th>
<th>Median Improvement in Fatality Rate</th>
<th>Median Improvement in Injury Rate*</th>
</tr>
</thead>
<tbody>
<tr>
<td>30 seconds</td>
<td>0.00279</td>
<td>0.00315</td>
</tr>
<tr>
<td>2 minutes</td>
<td>0.00568</td>
<td>0.00484</td>
</tr>
<tr>
<td>4 minutes</td>
<td>0.00751</td>
<td>0.00841</td>
</tr>
<tr>
<td>8 minutes</td>
<td>0.00764</td>
<td>0.00952</td>
</tr>
</tbody>
</table>

Table 10.2: Fatality rate improvement for the seventeen aircraft in actual configuration. *Note that injury rate includes fatal and serious injuries.

These results are summarised in Table 10.3 for the best estimate (median) of number of lives saved over the period 1966 to 1993 and the average per year. They are also displayed graphically in Figures 10.5, 10.6.

<table>
<thead>
<tr>
<th>Additional Burnthrough Protection Time</th>
<th>Median Lives Saved</th>
<th>Median Lives Saved Per Year</th>
</tr>
</thead>
<tbody>
<tr>
<td>30 seconds</td>
<td>69.6</td>
<td>2.5</td>
</tr>
<tr>
<td>2 minutes</td>
<td>142.1</td>
<td>5.1</td>
</tr>
<tr>
<td>4 minutes</td>
<td>187.7</td>
<td>6.7</td>
</tr>
<tr>
<td>8 minutes</td>
<td>191.0</td>
<td>6.8</td>
</tr>
</tbody>
</table>

Table 10.3: Potential life saving for the seventeen aircraft in actual configuration.
Benefit analysis of the postcrash burnthrough hardening.

Figure 10.5 Variation of improvement in fatality rate with additional burnthrough protection time for the seventeen aircraft in actual configuration [10.1].

The graph is shown overlaid with a least squares exponential curve fit for the median line having the formula $0.0077(1-e^{-0.013t})$ where $t$ is the additional burnthrough protection time in seconds. It should be noted that the coefficient 0.0077 is the horizontal asymptote and represents the maximum assessed improvement in fatality rate with a perfectly fire-hardened fuselage.

Figure 10.6 Variation of improvement in injury rate with additional burnthrough protection time for the seventeen aircraft in actual configuration [10.1].

The graph is shown overlaid with a least squares exponential curve fit for the median line having the formula $0.0102(1-e^{-0.0063t})$ where $t$ is the additional burnthrough protection time in seconds. It should be noted that the coefficient 0.0102
is the horizontal asymptote and represents the maximum assessed improvement in injury rate with a perfectly fire-hardened fuselage.

### 10.4.3 Benefit based on an aircraft standard applicable to later requirements.

The high, median, and low prediction of impact on fatality rate was based on 9999 iterations of all accidents on the database using the method described in Section 10.3 assuming that the aircraft were configured to the later requirements standard. The values shown in Table 10.4 represent the incremental change in fatality and injury rate predicted for all survivable accidents in which there were fatalities (25001 occupants, refer to Figure 10.1).

<table>
<thead>
<tr>
<th>Additional Burnthrough Protection Time</th>
<th>Median Improvement in Fatality Rate</th>
<th>Median Improvement in Injury Rate*</th>
</tr>
</thead>
<tbody>
<tr>
<td>30 seconds</td>
<td>0.00266</td>
<td>0.00250</td>
</tr>
<tr>
<td>2 minutes</td>
<td>0.00457</td>
<td>0.00466</td>
</tr>
<tr>
<td>4 minutes</td>
<td>0.00618</td>
<td>0.00782</td>
</tr>
<tr>
<td>8 minutes</td>
<td>0.00636</td>
<td>0.00852</td>
</tr>
</tbody>
</table>

Table 10.4: Fatality rate improvement for the seventeen aircraft configured to later requirements. *Note that injury rate includes fatal and serious injuries.

These results are summarized in Table 10.5 for the best estimate (median) of number of lives saved over the period 1966 to 1993 and the average per year. They are also displayed graphically in Figures 10.7, 10.8.

<table>
<thead>
<tr>
<th>Additional Burnthrough Protection Time</th>
<th>Median Lives Saved</th>
<th>Median Lives Saved Per Year</th>
</tr>
</thead>
<tbody>
<tr>
<td>30 seconds</td>
<td>66.5</td>
<td>2.4</td>
</tr>
<tr>
<td>2 minutes</td>
<td>114.2</td>
<td>4.1</td>
</tr>
<tr>
<td>4 minutes</td>
<td>154.6</td>
<td>5.5</td>
</tr>
<tr>
<td>8 minutes</td>
<td>159.1</td>
<td>5.7</td>
</tr>
</tbody>
</table>

Table 10.5: Potential life saving for the seventeen aircraft configured to later requirements.
Benefit analysis of the postcrash burnthrough hardening.

Figure 10.7  Variation of improvement in fatality rate with additional burnthrough protection time for the seventeen aircraft configured to later requirements [10.1].

The graph is shown overlaid with a least squares exponential curve fit for the median line having the formula $0.0063(1-e^{-0.014t})$ where $t$ is the burnthrough protection time in seconds. It should be noted that the coefficient 0.0063 is the horizontal asymptote and represents the maximum assessed improvement in fatality rate with a perfectly fire-hardened fuselage.

Figure 10.8  Variation of improvement in injury rate with additional burnthrough protection time for the seventeen aircraft configured to later requirements [10.1].

The graph is shown overlaid with a least squares exponential curve fit for the median line having the formula $0.0088(1-e^{-0.0076t})$ where $t$ is the burnthrough protection time in seconds. It should be noted that the coefficient 0.0088 is the
horizontal asymptote and represents the maximum assessed improvement in injury rate with a perfectly fire-hardened fuselage.

10.5 Analysis.

10.5.1 Number of burnthrough accidents.

Seventeen accidents were identified as matching the selection criteria in Section 10.3.1 and were considered appropriate to use for the benefit analysis. It was considered likely that there are other accidents where burnthrough was an issue but because there is little or no data available, they cannot currently be identified. If other burnthrough accidents have occurred, then the derived benefit would increase. This is because the incremental change in fatality rate and injury rate is derived from the total number of occupants in survivable accidents - which is a constant for the period considered. Therefore, the results contained within Section 10.4 can be considered as the minimum level of benefit achievable. From a study carried out of the Survivable Accidents Database, it is assessed that of the worldwide fire related fatal accidents (currently 140 on the Database) only 54% have sufficient data to assess whether burnthrough occurred. If the accidents not having available accident data have a similar benefit potential to those that do, then it is likely that the levels actually realized will be approximately 1.84 times (1/0.54) those contained in Section 10.4. Application of this factor to the benefit derived from the accidents studied results in the assessed median lives saved shown in Table 10.6.

<table>
<thead>
<tr>
<th>Additional Burnthrough Protection Time</th>
<th>Median Lives Saved per Year</th>
</tr>
</thead>
<tbody>
<tr>
<td>30 seconds</td>
<td>4.4</td>
</tr>
<tr>
<td>2 minutes</td>
<td>7.6</td>
</tr>
<tr>
<td>4 minutes</td>
<td>10.1</td>
</tr>
<tr>
<td>8 minutes</td>
<td>10.5</td>
</tr>
</tbody>
</table>

Table 10.6: Best assessment of median lives saved for varying improvements in burnthrough protection time.

The factor of 1.84 may be applied to all of the benefit assessments based on the 17 accidents in order to obtain an assessment applicable to all accidents.
10.5.2 Comparison between fatality rate and injury rate.

Examination of the results in Tables 10.2 and 10.4 shows that the potential improvements to injury rate can be less than the improvement in fatality rate. Injury rate includes serious and fatal injuries and might be expected to improve at a greater rate than fatality rate alone. This apparent anomaly is because the general reduction in injury severity allows for fatally injured occupants to move into the seriously injured category and potentially increase their numbers if insufficient seriously injured move into the minor or no injury category. By way of example, consider the fire hazard presented to the 60 impact survivors shown in Figure 10.2. If the five fire fatalities were reduced to two by the introduction of some enhanced cabin safety feature, then there would be an obvious reduction in the fatality rate. However, if this resulted in the number of seriously injured occupants increasing to 13 (those previously sustaining fatal injuries now being seriously injured), the total number of serious and fatal injuries would remain at 15. There would therefore be no change in injury rate.

10.5.3 Comparison with previous studies.

10.5.3.A Comparison With Cabin Water Sprays.

The benefit analysis that was carried out on cabin water sprays [10.2] in 1991 concluded that

“... today cabin water sprays would save on average 14 lives per year worldwide.”

This benefit should be compared with the assessed 10.5 lives saved per year for enhanced burnthrough protection times of eight minutes derived in this study.

10.5.3.B Comparison with a Representative Set of Survivable Accidents.

As part of a separate study [10.3] a representative set of survivable accidents has been derived. This set of 55 accidents has been selected such that it has similar attributes to the entire population of survivable accidents. Within this set, there are four burnthrough accidents. From this study it is assessed that these four accidents have the potential for 62 median lives to be saved from 1967-1995 an additional eight minutes of protection time for an aircraft configured to the latest requirements. Over the twenty-eight-year period involving a total of 356 survivable accidents this equates to (356/55) x (62/28) = 14 lives per year.
### Table 10.7: Comparison with representative set.

<table>
<thead>
<tr>
<th>Assessment of Lives Saved per Year From This Study</th>
<th>Assessment of Lives Saved per Year Based on the Representative Set of 55 Accidents</th>
</tr>
</thead>
<tbody>
<tr>
<td>10.5</td>
<td>14</td>
</tr>
</tbody>
</table>

Table 10.7: Comparison with representative set.

![Comparison with Representative Set](image)

Figure 10.9  Bar chart showing comparison between the results from this study and that derived from the representative set [10.1].

The similarity of the two values suggests that the prediction of benefit, taking into account the accidents for which data are not available, is reasonably accurate.

#### 10.5.4 Fire entry path.

During the accident analysis, the fire entry path was identified, where possible, in order to gain an understanding of the susceptible areas of the fuselage. A summary of fire burnthrough areas is presented in Table 10.8. The median number of saved lives were included so that an assessment may be made of the contribution of each fire entry area to the overall life saving capability.
Benefit analysis of the postcrash burnthrough hardening.

<table>
<thead>
<tr>
<th>Date</th>
<th>Location</th>
<th>Fuselage Skin</th>
<th>Doors</th>
<th>Windows</th>
<th>Cargo Hold</th>
<th>U/C Bay</th>
<th>Equipment Bay</th>
<th>Median Lives Saved*</th>
</tr>
</thead>
<tbody>
<tr>
<td>01-Feb-91</td>
<td>Los Angeles</td>
<td>Y</td>
<td>N</td>
<td>N</td>
<td>?</td>
<td>?</td>
<td>?</td>
<td>0</td>
</tr>
<tr>
<td>31-Aug-88</td>
<td>Dallas</td>
<td>Y</td>
<td>N</td>
<td>N</td>
<td>N</td>
<td>?</td>
<td>?</td>
<td>1</td>
</tr>
<tr>
<td>26-Jun-88</td>
<td>Habsheim</td>
<td>Y</td>
<td>N</td>
<td>N</td>
<td>?</td>
<td>?</td>
<td>?</td>
<td>3</td>
</tr>
<tr>
<td>22-Aug-85</td>
<td>Manchester</td>
<td>Y</td>
<td>N</td>
<td>Y</td>
<td>N</td>
<td>N</td>
<td>N</td>
<td>40</td>
</tr>
<tr>
<td>07-Dec-83</td>
<td>Manchester</td>
<td>Y</td>
<td>N</td>
<td>N</td>
<td>N</td>
<td>?</td>
<td>?</td>
<td>0</td>
</tr>
<tr>
<td>13-Sep-82</td>
<td>Malaga</td>
<td>Y</td>
<td>N</td>
<td>N</td>
<td>N</td>
<td>N</td>
<td>?</td>
<td>22</td>
</tr>
<tr>
<td>07-Oct-79</td>
<td>Athens</td>
<td>Y</td>
<td>N</td>
<td>N</td>
<td>N</td>
<td>?</td>
<td>?</td>
<td>6</td>
</tr>
<tr>
<td>22-Jan-73</td>
<td>Kano</td>
<td>Y</td>
<td>N</td>
<td>?</td>
<td>N</td>
<td>?</td>
<td>?</td>
<td>52</td>
</tr>
</tbody>
</table>

Table 10.8: Fire entry areas.

* = For 8 minutes additional protection with aircraft configured to later requirements
Y = Conclusive that burnthrough occurred
N = Conclusive that burnthrough did not occur
? = Insufficient information to determine whether or not burnthrough occurred

While conclusive data pertaining to each of the possible burnthrough routes is not available in most cases, it is evident from Table 10.8 that the fuselage skin is the prime entry route for pool fires.

10.5.5 Sensitivity.

10.5.5.A Sensitivity to Burnthrough Protection Time.

The improvement in fatality rate has been shown to vary with burnthrough protection time in an exponential manner. It can be seen that the assessed benefit can increase significantly with burnthrough protection time. An additional burnthrough protection time of 2 minutes approximately doubles the improvement in fatality rate compared with an additional 30 seconds of burnthrough protection time.
10.5.5.B Sensitivity to Number of Burnthrough Accidents.

An assessment has been made of the sensitivity of the number of lives saved per year to the contribution made by any one accident. The accident, which contributed the highest number of lives saved by burnthrough protection at 8 minutes (the longest time assessed), was in Kano on 22 January 1973. This accident contributed a median saving of 52 lives. If this accident is removed from the analysis the median number of lives saved per year reduces by 1.9, i.e., 5.7 reduces to 3.8. While this is a significant reduction, the value still is just on the edge of the range assessed by this analysis prior to applying the correction factor of 1.84 as discussed in Section 10.5.1. If information were available for all burnthrough accidents then the deletion of one accident would have less of an effect.

![Figure 10.10 Reduction in median improvement with highest contributing accident removed [10.1].](image)

10.6 Discussion.

10.6.1 Methodology.

The mathematical and statistical models developed from previous studies for the analysis of accidents is considered an extremely useful tool, especially when analyzing accidents with limited data. The technique of assessing a range of improvements as well as a best estimate provides a prediction of both the most likely benefit to be gained and its potential range.
Benefit analysis of the postcrash burnthrough hardening.

A precise time for burnthrough could not be determined for any of the accidents. Hence, rather than using absolute values for burnthrough time, assessments were made for the potential benefit of increasing burnthrough time, by 30 seconds 2, 4, and 8 minutes. From studying the accidents, it became apparent that improvements in burnthrough time greater than 8 minutes would have no further life saving potential. However, this comparatively high value was chosen in order to get an indication of the maximum achievable benefit providing the technology was available to produce this degree of fire hardening.

Consideration was given to using a standard value for assessing the likely number of lives to be saved for each second in improvement of burnthrough protection time. However, when considering each accident it became apparent that it would be incorrect to adopt this methodology. For example, consider an accident where the pool fire develops to the extent that at some time during the evacuation an exit is rendered unusable due to being engulfed in fire. In this instance, there would be a step change in the egress rate at the time of loss of the exit. The analyses were therefore carried out by making unique assessments for each accident scenario and the number of occupants that might escape for discrete improvements in burnthrough penetration times.

10.6.2 Results.

The potential benefit in terms of reduction in fatality rate and reduction in injury rate was assessed for each of the accidents listed in Table 10.1. As can be seen from Figure 10.1, of the 140 fire-related fatal accidents occurring during the period covered by this study, only 17 were identified as involving burnthrough. It has been assessed that the benefit derived from the accidents studied should be multiplied by a factor of approximately 1.84 in order to obtain the corresponding figure for all accidents. If this factor is applied to the assessed benefit for an additional 8 minutes of burnthrough protection time, then the benefit becomes 10.5 lives saved per year.

Injury rate and fatality rate are defined as “The total number of serious and fatal injuries divided by the total number of occupants aboard” and “The total number of fatalities divided by the total number of occupants aboard”. It should be noted that in
this context, injuries include fatalities and serious injuries. From Table 10.2, a comparison of the injury rates with fatality rate shows that the potential improvement in injury rate is not greatly different from the improvement in fatality rate. Whilst improvements in burnthrough time are likely to reduce the number of fatal injuries, the serious injuries may for some accidents actually increase. This is because the occupants that would be saved from fatal injuries may still sustain serious injuries. Hence, for some accidents there may be no improvement in injury rate (fatal plus serious) but an improvement in fatality rate.

The assessment of fatality rate and injury rate was carried out for varying increases in burnthrough time for each accident. A similar benefit assessment was carried out as if the aircraft had been configured to the standards required by the latest cabin safety requirements. The results of these assessments are presented graphically in Figures 10.5-10.8 for varying increases in burnthrough time. Also shown in these figures is the assessed 95-percentile range in benefit using the methodology previously described. For each of these assessments, a curve of best fit has been derived for the median, or best estimate, of the improvement in fatality rate or injury rate. This curve of best-fit tends to take the form of an exponential relationship between improvement in burnthrough time and improvement in fatality/injury rate

\[ \text{improvement in fatality/injury rate} = A \left( 1 - e^{-Bt} \right). \]

Where \( A \) = the asymptote of the equation representing the maximum possible improvement in fatality/injury rate attainable, and \( B \) = a constant that represents the magnitude of the change in improvement in fatality/injury rate with improvement in burnthrough time.

If the factor of 1.84 is applied to the derived values contained in Section 10.4.2, then for an increase in burnthrough protection time of 8 minutes the assessed number of lives saved per year is 12.5 based on aircraft configured to the standard at the time of the accident. The equivalent reduction in number of injuries is 15.6 per year. For the assessment based on aircraft configured to the latest requirements the derived values for an additional 8 minutes of burnthrough protection time is 10.5 lives saved per year. The equivalent reduction in number of injuries is 14.0 per year.
Benefit analysis of the postcrash burnthrough hardening.

The assessed change in benefit, both in terms of reduction in number of fatalities and injuries, between the actual aircraft configuration and what is predicted for an aircraft configured to later requirements is not as pronounced as might be expected. This may be because major penetration of the aircraft skin is likely to result in rapid progression of fire throughout the passenger cabin. In this respect, the later requirements result in less benefit in protecting from large external pool fires than they do for smaller external fires or for internal cabin fires.

By way of comparison, the assessment carried out for cabin water sprays [10.2] suggested that water sprays would save in the region of 14 lives per year worldwide. Hence, enhanced burnthrough protection has the potential to save in the region of (10.5/14) approximately 75% of what was assessed for cabin water sprays. However it is likely that techniques for improving the fire penetration resistance of fuselages would impose less costly changes to the aircraft than would cabin water spray systems.

A comparison has also been made between the predictions of benefit from this analysis and those derived from a study of a representative set of survivable accidents as described in Section 5.3.2. The results compare reasonably well as shown in Figure 10.9. An inspection of the data relating to median lives saved for an additional 8 minutes protection time shows that certain accidents make a significant contribution to the overall benefit assessment. For example, the Kano accident contributed a saving of 52 lives. In order to assess the significance of accidents with these high levels of potential benefit, the improvements in fatality rate were assessed for all of the accidents excluding Kano. The subsequent derived rate, whilst significantly reduced, approximated to the lower band of the prediction for improvement in fatality rate. On this basis, it is considered that the results of this study are realistic and reasonable.

The structural strength of the aircraft exposed to a pool fire did not appear to have a significant effect on occupant survival. Although there were limited data available, only two accidents (Los Angeles and Manchester) were positively identified as involving structural collapse. Structural failure occurred at 18 minutes for Los Angeles and hence was not a factor in occupant survival. As previously discussed, it is assessed that there is limited benefit to be gained beyond 8 minutes. There is
insufficient data available to ascertain the time that structural collapse occurred for the Manchester accident. Confirmation that structural strength is not a factor in burnthrough accidents is important. If confirmed, changes intended to fire harden the fuselage do not need to take into account the residual structural strength.

Whilst for almost all accidents the data available on the fire penetration route is extremely limited, it is evident that the fuselage skin is by far the most significant path, as might be expected. This is illustrated by the data contained in Table 10.8. Whilst other areas of the fuselage (windows, doors, etc.) may have been penetrated by the fire, it is considered unlikely that they contributed significantly to occupant survival.

There is insufficient data to determine whether the frequency of occurrence of burnthrough accidents has changed significantly during the period covered by this study. However, if there has been any change in rate of occurrence, it is unlikely to be significant when compared to the benefit that may be derived from improving the fire hardening of the aircraft. This is not surprising since the factors contributing to the probability of occurrence of a pool fire have probably not changed significantly. Furthermore, the fire resistance of fuselage skins would be much the same in the current world fleet as it was 30 years ago. On this basis, it is likely that burnthrough accidents will occur at a similar annual rate (per number of flights) for the immediate future.

10.6.3 Cost benefit.

The cost benefit will, of course, relate to the solution adopted for protection of the cabin from external pool fires. Since the benefit has been derived in terms of fire penetration time, an assessment of the cost of each potential solution may be readily calculated. Since it is unlikely that changes in the fuselage skin material will be cost beneficial, then other solutions must be sought. Some work has been carried out by the Federal Aviation Administration and by Faverdale Technology showing the difference between various thermal acoustic insulation materials. If it can be confirmed that structural strength is not a significant factor then solutions such as this are likely to show great potential for being cost-effective. It may be argued that additional benefit may be gained by enhanced flammability standards for cabin floors.
Benefit analysis of the postcrash burnthrough hardening.

However, any enhancement in flammability standards of cabin floors to improve the survivability of occupants in pool fires would require a further study of its effectiveness and feasibility.

10.7 **Conclusions.**

- Fire hardening of fuselages will provide benefits in terms of enhanced occupant survival.
- The assessed reduction in fatalities and injuries from enhanced aircraft fire penetration resistance is shown in Table 10.9

<table>
<thead>
<tr>
<th>Based on Aircraft Standard at Time of Accident</th>
<th>Reduction in Fatalities Per Year</th>
<th>Reduction in Injuries Per Year*</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>12.5</td>
<td>15.6</td>
</tr>
<tr>
<td>Based on Aircraft Configured to the Latest Requirements</td>
<td>10.5</td>
<td>14</td>
</tr>
</tbody>
</table>

Table 10.9: Reduction in fatalities and injuries. *Injuries include fatal and serious injuries.

- The rate of improvement in benefit appears to vary exponentially with minimal improvement beyond the 4 to 8 minute additional protection point.
- The assessed benefit derived from this study is similar in magnitude to that determined from a study of a representative set of survivable accidents, thus providing some confidence in the results.
- The prime fire penetration route is via the fuselage skin and no evidence could be found to suggest that alternate burnthrough routes contribute significantly to occupant survival.
- Aircraft configured to the latest cabin safety requirements are likely to exhibit enhanced, but relatively limited, protection against large external pool fires.
It is not considered likely that the rate of fatalities and injuries per year caused by fuselage burnthrough will change markedly for the near future.

The reduction in the structural strength of the fuselage as a result of a pool fire appears to have a limited effect on occupant survival. If this is confirmed, it will result in a greater opportunity to find cost beneficial solutions to hardening aircraft against pool fires.

Costs have not been assessed to ascertain the cost per life saved for possible methods of enhancing burnthrough protection of aircraft. However, the relationship between benefit and additional burnthrough protection, derived from this study, will assist in carrying out such an analysis.

10.8 References


CHAPTER 11 Summary and Conclusions.

The current dissertation dealt with a crucial subject of the aeronautics industry that is the fire response of composite aerostructures, more specifically the issue of interest in this work is the fuselage fire burnthrough from an external liquid jet-fuel pool fire. Other fire issues that “bother” the aeronautics industry are the fire spread inside the cabin, smoke generation and toxicity of the fumes, but these are not handled in the current dissertation.

Aircraft structures are designed to withstand various loading scenarios during their operational life. These loading scenarios are associated to a great extent with normal aircraft operation (flight manoeuvres, take-off and landing). However there are situations where the aircraft structures are required to assure the safety of the passengers and crew. In the case of an emergency crash landing, the threat of an external jet-fuel fire always exists. Considering that the aircraft structure survives the impact, the survivability of the passengers and crew onboard the aircraft depends solely on the fire resistance of the aircraft structure. A measure of the fire resistance
of an aircraft structure is the time needed for the flames to penetrate the fuselage and spread inside the cabin, the so-called, burn-through time.

So far, the aircraft fire resistance has been extensively studied by conducting lab, medium and full scale tests. The early lab scale tests were performed by the Federal Aviation Administration (FAA) and involved the Bunsen-burner flammability test of coupons for developing fire safe interior materials. As the application of polymer materials on aircrafts kept increasing, the problem of fire burn-through due to external fire emerged. Marker [1.7] was one of the first to perform full-scale fuselage burn-through tests to access the insulating performance of materials. Also a statistical analysis was performed by Cherry and Warren [10.1] that accessed and analyzed data from past accidents and their work resulted in proving the importance of fuselage fire hardening and the passengers’ lives that could be saved using low-cost solutions. These works led the FAA to proposed new fire testing procedures for aircraft materials [1.9].

The scope of this dissertation was to assess the performance of various structural materials in a pool-fire scenario. A simplified approach is made, approximating the pool-fire conditions with a flat panel burn-through test in accordance to the ISO2685:1998(E) Standard [6.1].

The originality of the present work comes from the fact that it incorporates a multistage approach in order to investigate the behaviour and response of composite aircraft structures in the possibility of a fire event. The current approach goes down on material level in order to investigate and model the deterioration (decomposition) of the polymer composite. Thus, it investigates and proposes a methodology of how the thermophysical properties of the composite are deteriorated due to the fire event. It proceeds into developing a progressive-damage material model (material properties varying with the deterioration degree) and finally implementing this custom material model into a commercial FE package and solving the loading scenarios.

Being more specific the current work begins with a quick review of the literature where incidents and work done on the burnthrough event for the past 20-30 years are summarized. It progresses then to presenting the various types of polymers used in the
aircraft industry and their basic decomposition mechanisms, from the unsaturated polyesters to the epoxies and phenolics and in the end reference to the thermoplastics is made. Every organic material, hence, polymers used in aerospace applications, present a set of response characteristics when subjected to fire, specifically the heat release rate, thermal stability index, limiting oxygen index, flammability index, time-to-ignition, surface flame spread, mass loss, smoke density and smoke toxicity. All these characteristics are presented to an extent in chapter 3.

In chapter 4 the backbone of this dissertation is presented, the kinetics modelling. Two approaches are made, one simplified using single stage kinetics where the decomposition degree $a$ is calculated based on the Arrhenius reaction theory and using the kinetic triplets (kinetic parameters) extracted from thermogravimetry, TGA, data using the Friedman multi-curve method. The second approach is more complicated and considers multi-stage decomposition of the polymer composite. Specifically a 3-stage reaction network is considered for every material, the LY-Ref, and the two modified batches, one with ammonium polyphosphate AP423 and the other both with AP423 and multi-wall carbon nanotubes MWCNT. Again the kinetic parameters, activation energy $E_A$, frequency factor $A$, and reaction order $n$, are extracted for every step using the van Krevelen methodology. In the end using the reaction rates equations the reconstruction of the TGA curves is achieved with an error of less than 5% from the test data. Further on to this work and into chapter 5, correlations that consider the material deterioration and affect the thermophysical properties of the materials are proposed. Those expressions are being developed for both of the two kinetic approaches, the single and multi stage.

In chapter 6, another crucial part of this work is presented, the measurement and calibration of the applied fire load. Again two fire load approaches are used, one according to the ISO2685 Standard where a propane burner was manufactured and calibrated according to the Standard for medium scale samples testing and a lab scale butane burner for small samples. The ISO2685 burner was also CFD simulated and the models calibrated against analytical expressions, ISO requirements and real measurements. The CFD simulations were performed so the heat flux or heat transfer coefficient to be extracted and used as input for the later thermal FE burnthrough models. The heat flux distribution of the lab-scale AML burner on the specimen
surface was measured via a water cooled Schmit-Boelter SBG01 heat flux sensor manufactured by Hukseflux.

Manufacturing and materials used details are presented in chapter 7 concerning the samples used for every test campaign. Metallic (AL2024-T3) samples, CFRP neat and modified, and hybrid GLARE ones where manufactured. Also the experimental work performed is described. Cone calorimetry testing data are available, results from thermogravimetry tests, differential scanning calorimetry, and finally the burnthrough tests with both the testing apparatuses, the ISO2685 one and the AML lab-scale burner.

In chapter 8, the modelling work performed during this dissertation is presented. Thermal models were developed into a commercial FE package. It was not part of this work to develop a thermal solver so a commercial one was selected and all the developed methodology was adapted to its requirements and specifications. The boundary conditions on the models are presented both for the ‘hot’ front surface and the rear ‘cooling’ one. For the ‘hot’ one the heat flux distribution is used and for the ‘cooling’ one an equivalent convection is applied that accounts for both convective and radiative cooling. The decomposing material model is implemented into the FE solver via user defined subroutines for the single stage kinetics and the multi-stage approach. Finally the simulations were run and the results and models were compared against the available experimental results.

Since so far the burnthrough response of aerostructures was limited to coupon, samples and medium size flat panels. A more realistic approach is presented in chapter 9, meaning a mathematical model of a real size test. The certification tests conducted by the FAA are for full size fuselage sectors under the fire load of a burning jet-fuel pan pool-fire. A burning jet-fuel pool fire is a complex phenomenon on its own, combining it with a decomposing fuselage structure make the modeling approach even more difficult to simulate if not impossible. Required data for the pool-sizes under investigation were not available, so data for large external hydrocarbon pool fires from literature were used. Also, because the main characteristic of a jet-fuel (kerosene) pool fire is that the flames are not clear, on the contrary, great amount of shoot is produced making combustion modeling and radiative heat transfer to the
Summary and Conclusions.

fuselage even more of a challenge to model, it was decided to try and tackle this full-scale approach by a simplified the modeling approach. Instead of liquid fuel combustion, an equal hot air stream with mass flow, velocity and temperature properties extracted from literature correlation data was performed.

Conclusively, in terms of completeness in chapter 10 the benefit analysis performed by Cherry and Warren [10.1] was presented in brief. The objective of their analysis was to assess the potential benefits, in terms of reduction of fatalities and injuries, resulting from improvements in fuselage burnthrough resistance to ground pool fires. Fire hardening of fuselages will provide benefits in terms of enhanced occupant survival and may be found to be cost beneficial if low-cost solutions can be found. The maximum number of lives saved per year in worldwide transport aircraft accidents, over the period covered by the data, if hardening measures were applied, was assessed to be 12.5 for the aircraft in its actual configuration (when the accidents occurred) and 10.5 for the aircraft configured to later airworthiness requirements.

These figures are completely significant and give an extra confirmation that this work on investigating the fire response of composite aerostructures is on the right track. As the work of Cherry and Warren concluded, the fire hardening measures in order to be applicable need to be cost efficient. The concept under which this whole dissertation stepped on was to investigate the fire response of composite aerostructures and the possibility of hardening the structure itself without the use of extra protective layers that add cost and weight to the overall aircraft and its maintenance. In the end it was concluded that there is the possibility of hardening the fuselage structure by design and by material. Incorporating composites into the structure it is possible to prolong the burnthrough time at least for 4-5 minutes before auto ignition occurs on the inner side of the fuselage. Auto ignition of the inner side fuselage cabin materials is mentioned since in NONE of the burnthrough tests of the CFRP composites and the GLARE samples flame penetration was observed.

So far the best candidate hardening measures is the use of Fiber Metal Laminates, and CFRP composites with resins modified to promote the charring effect, due to its endothermic nature, it consumes part of the impinging heat to pyrolize and form the carbonaceous char. The insulating properties of the carbonaceous chars was first
investigated by the early NASA missions when Thermal Protection Systems, TPS, or heat shields were developed for the ballistic reentries of the Apollo and other missions. The Apollo heat shield used ablation as part of the protective mechanism. The impinging heat flux elevated the temperature of the phenolic TPS which in turn charred creating an insulating layer and also consuming part of the heat. Those protective layers were called phenolic charring ablators. Other materials that could be candidate for fire hardening of fuselage structures with minimum weight penalty might be the very promising aerogels core materials but this is something that needs further and deeper investigation and is proposed for future work since aerogel core materials are still under development.
APPENDIX A  Fuselage Burnthrough Protection for Increased Postcrash Occupant Survivability: Safety Benefit Analysis Based on Past Accidents.
Software has been developed to represent a mathematical simulation of an accident using Monte Carlo Simulations. This enables assessments to be made of the likely range in numbers of survivors resulting from improvements in survivability factors.

Stage 1 of this process is shown diagrammatically in figure A-1. The in-depth analysis of accident details results in the generation of a survivability chain, or series of parallel survivability chains for accidents with several accident scenarios. In this study an assessment has been made of the effect on number of fatalities as a result of improvements to cabin burnthrough times. The assessment results in a prediction of the highest, mean, and lowest number of fatalities that could reasonably be expected from the improvements.

Stages 2 and 3 of the process are shown in figures A-2 and A-3.

Figure A-2 illustrates the principle for assessing the effect on survivability of variations in the effectiveness of improvements to survivability factors. From the rationales, the best (or median) assessment is made of the number of fatalities and injuries that would result in improvements to cabin burnthrough time. However, when making these determinations the analysts will also determine a maximum and minimum number of fatalities and injuries that are likely to result from the improvements.

It is then assumed that there can be 100% confidence that the fatalities will lie in the range from minimum to maximum according to the distribution shown in figure A-2. The software has been developed so that random selections may be made over the range 0% to 100% to arrive at a particular number of fatalities and injuries.

From each random selection a re-evaluation of the number of survivors may be made using the survivability chain generated for the accident scenario as shown in figure A-3. This is then compared with the actual number of survivors of the accident. Thus improvements in survivability, and hence survivability rate may be generated. The formula employed is

\[ S_F = \frac{S - S_B}{T} \]

Where \( S_F \) = the assessment of the increase in survivability rate

\( S \) = the reassessed number of survivors due to the improvements for all accident scenarios

\( S_B \) = the actual number of survivors for all accident scenarios

\( T \) = the total of all occupants for all accident scenarios.

Stages 2 and 3 are repeated a number of times, typically 10,000, which builds up a statistical distribution of values for the improvement in injury rate. Refer to figure A-4.
Stage 4 of the process is simply to determine the 2½, 50, and 97½ percentiles from the resulting distributions to ascertain a mean and likely range for the prediction.

Whilst it is recognised that the models are not perfect representations of an accident nor are the statistical assessments totally accurate, they will provide a better assessment of the likely impact of improvements to survivability factors than would otherwise be derived from a simple estimate of the resultant change in number of survivors.

Figure A-1. Statistical Modelling Process, Stage 1
Figure A-2. Statistical Modelling Process, Stage 2

Repeat stages 2 and 3 many times (e.g., 10,000 iterations)

RANDOM SELECTION
READ OFF VALUE FOR NUMBER OF FATALITIES/INJURIES
Figure A-3. Statistical Modelling Process, Stage 3

Evaluate injury rate improvement using:

$$S_F = \frac{S_B - S}{T}$$

Where:

$S_F$ = the assessment of the decrease in injury rate

$S$ = the reassessed number of uninjured survivors for all accident scenarios

$S_B$ = the actual number of uninjured survivors for all accident scenarios

$T$ = Total number of all occupants for all accident scenarios

A similar technique is used for fatality rate improvement.
From statistical distribution, read off 97.5, 50, and 2.5 percentiles.

Values from circa 10,000 iterations

Figure A-4. Statistical Modelling Process, Stage 4
APPENDIX B—ACCIDENT RATIONALES 1 TO 7

The rationale for each accident that was analysed appears in this appendix. Each accident is presented with the following sections:

- Description of the accident reproduced from the Survivable Accidents Database.
- A description of the fire penetration mechanism using relevant extracts from the database together with any assessments and assumptions made.
- A diagram showing the location of occupants and scenario boundaries.
- Survivability chains for each scenario.
- Effect of later requirements.
- Effect of a fire-hardened fuselage.

The accidents are presented in reverse chronological order.

Key to injury location diagrams:
1. Description of Accident

RESUME
On 16-Feb-67 Lockheed Electra L-188C registered as PK-GLB was on approach to Mapanget Airport, Menado, Indonesia.

The pilot-in-command adopted an awkward approach technique and the aircraft landed heavily 3 ft short of the runway manoeuvring area and some 156 ft short of the runway threshold. The undercarriage collapsed, the aircraft skidded, caught fire and came to rest on the runway.

The aircraft was destroyed by fire.

There were 8 crew and 84 passengers aboard. 22 passengers suffered fatal injuries. 12 passengers suffered serious injuries. 8 crew and 50 passengers escaped with minor or no injuries.

IMPACT
Examination at the site of the accident revealed that the aircraft first struck the ground with its right main landing gear which collapsed rearward and inboard. Marks on the manoeuvring area indicated that the propellers struck the ground in the order 4-3-2-1 and were all torn off from their respective engines. Impact of the left main landing gear was quite severe and the left wing failed, spilling fuel. The aircraft skidded on its belly along the runway and came to stop on the runway.

FIRE
As the aircraft skidded along the runway, fuel spilling from the left wing caught fire and when the aircraft came to a stop it was ablaze. The heat of the engine and also the heat produced by the friction between the aircraft body and the ground, could all have ignited the fuel.

Due to the inadequacy of the fire fighting equipment the aircraft was burnt out.

EVACUATION
The crew consisted of a pilot-in-command, co-pilot, flight engineer, flight radio operator, 3 air hostesses and a steward.

According to the crew, just after the aircraft came to a full stop the co-pilot instructed the flight engineer and the cabin attendants to open the forward main entrance door but unfortunately they did not succeed in opening this door from the inside. Meanwhile smoke and fumes began to penetrate the first class cabin (aft). The cabin attendants advised all passengers to proceed forward in order to escape from the right hand forward emergency exit and cockpit sliding windows. One of the cabin attendants tried to open the overwing emergency exits but was advised by another crew member not to open them because of fire outside the window. During
that same time the flight crew attempted to open the forward main entrance door from the outside; however, they did not succeed either. Survivors evacuated from the aircraft through the right forward emergency exit and cockpit sliding windows.

Passengers who tried to escape from the rear service door, but were unable to open it quickly were trapped in the burning wreckage.

**AIRCRAFT FACTORS**

The aircraft was a Lockheed Electra L-188C registered as PK-GLB. The aircraft was airworthy and maintained in accordance with the approved maintenance specifications. There was no evidence of failure or malfunctioning of the aircraft or its components prior to touchdown that could have led to the accident.

The type of fuel used was not stated in the accident report.

The cabin was fitted with a main entry door at the front on the port side with a service door opposite. There was an overwing exit above each wing. At the rear there was an entry door on the port side with a service door opposite.

**ENVIRONMENTAL CONDITIONS**

The accident occurred at 05:21 hours GMT in daylight. The prevailing weather conditions before and at the time of the accident were reported to be overcast with light showers over the airfield. The 05:00 hours weather report was: wind southerly and about 2 kt, visibility 2 to 3 km, cloud 3/8 at 900 ft and 4/8 at 2000 ft.

**INJURIES TO OCCUPANTS**

There were 8 crew and 84 passengers aboard. 22 passengers suffered fatal injuries. 12 passengers suffered serious injuries. 8 crew and 50 passengers escaped with minor or no injuries.

Casualties were primarily caused by fire and not impact. All those passengers who apparently tried to escape from the rear service door, but were unable to open it quickly were trapped in the burning wreckage.

2. **Fire Penetration Mechanism**

As the aircraft skidded along the runway, fuel spilling from the left wing caught fire, and when the aircraft came to a stop it was ablaze. Further, it was reported that smoke and fumes began to penetrate the cabin from the aft end first.

It is therefore concluded that the ignited, spilt fuel lay predominantly under the rear of the fuselage and burnt through the lower skin in that area. Once burnt through, smoke and then flames would have penetrated the passenger cabin through the floor at the aft end of the fuselage.

Based on the above it is assessed that the prime burnthrough route was through the rear lower fuselage skin. However there was insufficient information to be conclusive about detailed burnthrough areas.
3. Location of Injuries and Scenarios

Due to the lack of detailed information on the location of occupants during the accident, it has been necessary to consider this accident as one scenario containing the whole aircraft.

4. Accident Scenarios and Survivability Chains

This accident is considered as one scenario.

**Scenario 1** contains the whole fuselage. Fire entered at the rear and propagated forward. The scenario contains the 4 flight crew, 4 cabin crew and 84 passengers.

![Diagram of Scenario 1]

5. Effect of Later Requirements

For Scenario 1, it is assessed that later requirements would have improved flammability standards such that the occupants would have had additional time to escape. It is assessed that 11 fatalities would have escaped with non-fatal injuries and further that 11 seriously injured would have escaped with minor or no injuries.

The survivability chain for Scenario 1 therefore becomes:

![Updated Diagram of Scenario 1]

6. Effect of a Fire-Hardened Fuselage

**Scenario 1**

It is concluded that a fire-hardened fuselage would have delayed the entry of fire and provided the occupants with more time to make their way to the forward exits.
The high, median, and low prediction from the use of a fire-hardened fuselage is:

For 30 seconds protection:

\[
\begin{array}{ccc}
S & I & F \\
H & 60 & 12 & 20 \\
M & 63 & 15 & 14 \\
L & 73 & 12 & 7 \\
\end{array}
\]

For 2 minutes protection:

\[
\begin{array}{ccc}
S & I & F \\
H & 62 & 12 & 18 \\
M & 70 & 14 & 8 \\
L & 80 & 12 & 0 \\
\end{array}
\]

For 4 minutes protection:

\[
\begin{array}{ccc}
S & I & F \\
H & 64 & 12 & 16 \\
M & 83 & 9 & 0 \\
L & 92 & 0 & 0 \\
\end{array}
\]

For 8 minutes protection:

\[
\begin{array}{ccc}
S & I & F \\
H & 64 & 12 & 16 \\
M & 84 & 8 & 0 \\
L & 92 & 0 & 0 \\
\end{array}
\]

For an aircraft configured to the standards appropriate to the latest requirements this assessment becomes:

For 30 seconds protection:

\[
\begin{array}{ccc}
S & I & F \\
H & 69 & 13 & 10 \\
M & 75 & 12 & 5 \\
L & 87 & 5 & 0 \\
\end{array}
\]

For 2 minutes protection:

\[
\begin{array}{ccc}
S & I & F \\
H & 69 & 15 & 8 \\
M & 80 & 11 & 1 \\
L & 92 & 0 & 0 \\
\end{array}
\]

For 4 minutes protection:
Summary

The assessed median number of lives saved by a fire-hardened fuselage are:

<table>
<thead>
<tr>
<th>Burnthrough Protection Time</th>
<th>Aircraft in its Actual Configuration</th>
<th>Aircraft Configured to Later Requirements</th>
</tr>
</thead>
<tbody>
<tr>
<td>30 seconds</td>
<td>8</td>
<td>6</td>
</tr>
<tr>
<td>2 minutes</td>
<td>14</td>
<td>10</td>
</tr>
<tr>
<td>4 minutes</td>
<td>22</td>
<td>11</td>
</tr>
<tr>
<td>8 minutes</td>
<td>22</td>
<td>11</td>
</tr>
</tbody>
</table>
1. Description of Accident

RESUME
On 8-Apr-1968 BOAC B707 registered as G-ARWE was taking off from London (Heathrow) airport.

Approximately one minute after take-off the No. 2 engine failed and a few seconds later caught fire. The fire did not go out and the aircraft was manoeuvred for the quickest possible return. During the approach, the No. 2 engine fell away from the aircraft. The aircraft made a successful landing but fuel released on the port side caught fire.

An emergency evacuation was initiated using exits on the starboard side as the fire and smoke spread from the rear forwards. The assist means did not perform well and as a result the crew lost valuable time during the evacuation.

Of the 11 crew and 116 passengers aboard, 1 crew member and 4 passengers suffered fatal injuries. 38 passengers suffered serious injuries. 10 crew and 74 passengers escaped with minor or no injuries.

IMPACT
The aircraft made a smooth touchdown and the Captain brought the aircraft to halt normally. There was no impact.

FIRE
When the aircraft came to a stop, the fire, which had continued to burn near the No. 2 engine position, increased in intensity and the fuel tanks in the port wing exploded. The accident investigation established that the fire continued to burn because of an omission to close the fuel shutoff valve after the engine caught fire. After the aircraft came to rest, the captain ordered a fire drill on the remaining engines. Before this could be carried out, there was an explosion from the port wing which increased the intensity of the fire and blew fragments of the wing to the starboard side of the aircraft. The captain then ordered immediate evacuation of the flight deck. The engine fire shutoff handles were not pulled and the fuel booster pumps and main electrical supply were not switched off. There were more explosions and fuel, which was released from the port tanks, spread underneath the aircraft and greatly enlarged the area of the fire.

EVACUATION
There were 5 flight crew and 6 cabin crew and 116 passengers aboard.

The cabin crew opened the emergency exits as the aircraft came to a stop and started rigging the escape chutes (this involved positioning a bar behind clips on the cabin floor). The passengers commenced evacuation from the two starboard overwing exits, and shortly afterwards, when
the escape chutes had been inflated, from the rear starboard galley door and then the forward starboard galley door.

However, because of the spread of the fire under the rear of the fuselage, the escape chute at the rear galley door soon burst and following the first explosion, the overwing escape route also became unusable.

The starboard overwing exits were the first utilised: 18 passengers escaped by these exits under the direction of the Chief Steward before he stopped their further use because of the smoke and flames which enveloped the starboard wing area following the main explosion.

Nobody left the aircraft by the forward port overwing exit.

The starboard rear galley door's chute was rigged, inflated, and found to be misaligned. One of the stewards climbed down to straighten it. Only 5 passengers and 1 steward escaped down this chute before the sparks and flames spreading from the port side burst it. 5 passengers jumped through this doorway after the chute became unserviceable.

The starboard forward galley door's escape chute was delayed in being put into operation, due to difficulty getting the chute retaining bar into its clips. After this initial delay, the main body of passengers evacuated the aircraft rapidly by this route. The evacuation tended to slow down as passengers, both injured and otherwise, began to collect round the bottom of the chute and in front of the starboard wing. The captain left the aircraft by this exit during a gap between the passengers disembarking. When it appeared that all the passengers had left the aircraft, the remaining cabin crew members also used this escape route.

The port forward main door was also used. The chute did not inflate at first, after it as deployed, and the flight engineer climbed down and straightened it out at the bottom; it almost immediately caught fire and burst. 1 passenger escaped jumping from this doorway after the chute collapsed. 3 flight crew members egressed through the cockpit windows.

The evacuation took place in an orderly manner, but when the rear galley door and starboard overwing overwing exits became unusable, some momentary confusion resulted among those passengers who had to revise their escape routes. Conditions in the cabin were quite good in the early stages. But they deteriorated rapidly when the explosion occurred. As the evacuation progressed, dense black smoke advanced forward up the cabin from the rear as the fire took deeper and deeper hold. Smoke eventually reduced visibility to zero in the forward galley area. The captain stated it was completely overpowering. There was some difficulty in helping passengers at the rear of the aircraft, which was the first part of the fuselage to be overwhelmed by the fire. it was in this area that the stewardess was last seen alive attending to the passengers who ultimately succumbed.

The evacuation of passengers had been largely completed by the time airport Fire and Rescue Service began to provide assistance. The fire service prevented the fuel in the starboard tanks from catching fire but the rear fuselage and port wing was burned out.
AIRCRAFT FACTORS
The aircraft was a B707-465 registered as G-ARWE, operated by BOAC.

The cabin was fitted with a main entry door at the front with a service door opposite. There were 2 overwing exits above each wing. There was an entry door at the rear port side and a service door opposite.

ENVIRONMENTAL CONDITIONS
The weather conditions were clear and fine at the time of the accident.

INJURIES TO OCCUPANTS
Of the 11 crew and 116 passengers aboard, 1 crew member and 4 passengers suffered fatal injuries. 38 passengers suffered serious injuries. 10 crew and 74 passengers escaped with minor or no injuries.

4 of the passengers and one stewardess were overcome by heat and smoke in the rear of the aircraft and did not escape.

38 passengers sustained injuries during the evacuation.

2. Fire Penetration Mechanism

After the aircraft had come to a stop on the runway, there was an explosion from the port wing which increased the intensity of the fire and blew fragments of the wing to the starboard side of the aircraft. This would have spread the external fire over the outer surface of the fuselage.

There were more explosions and fuel, which was released from the port tanks, spread underneath the aircraft at the rear and greatly enlarged the area of the fire. Internally smoke was observed as coming from the rear of the cabin and spreading forward.

The spread of fire under the rear of the fuselage combined with the outer surface fire would have rapidly burnt through the fuselage skin, initially at the rear of the cabin.

Based on the above it is assessed that the prime burnthrough route was through the fuselage skin. However there was insufficient information to be conclusive about other burnthrough areas.

3. Location of Injuries and Scenarios

Due to lack of detailed information on the location of the occupants during the evacuation, this accident has been taken as one scenario containing the whole aircraft.

4. Accident Scenarios and Survivability Chains

This accident is considered as one scenario.
**Scenario 1** contains the whole fuselage. It was not subjected to any impact, but the fire entered at the rear and propagated forward. The scenario contains the 5 flight crew, 6 cabin crew, and 116 passengers.

5. **Effect of Later Requirements**

For Scenario 1, it is assessed that later requirements would have improved flammability standards such that the occupants would have had additional time to escape. It is assessed that two of the fatalities would have escaped with non-fatal injuries.

The survivability chain for Scenario 1 therefore becomes:

6. **Effect of a Fire-Hardened Fuselage**

**Scenario 1**

It is concluded that a fire-hardened fuselage would have delayed the entry of fire and provided the occupants with additional time to make their way to the available exits.
The high, median, and low prediction from the use of a fire-hardened fuselage is:

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For an aircraft configured to the standards appropriate to the latest requirements this assessment becomes:

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Summary

The assessed median number of lives saved by a fire-hardened fuselage are:

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<td>8 minutes</td>
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1. Description of Accident

RESUME
A Super VC10 was taking off from Addis Ababa on 18-Apr-1972. Just prior to decision speed the nosewheel tyre hit a jacking pad that has fallen from a Cessna 185 a few hours earlier. The tyre burst and the crew initiated an aborted take-off but were unable to stop the aircraft before the end of the runway.

The aircraft slid down an embankment at the end of the runway and struck a steel lattice tower which ruptured the fuel tank. The spilling fuel ignited and the aircraft broke into three sections as it came to rest.

Survivors evacuated the aircraft through the fuselage breaks as the left emergency exits were jammed by impact damage and the right side exits were blocked by fire. 10 people had to walk away from the aircraft between streams of fuel which subsequently caught fire and trapped them.

Of the 11 crew and 96 passengers aboard, 8 crew and 35 passengers suffered fatal injuries.

IMPACT
After the nosewheel tyre burst the aircraft continued down the runway, veering slightly to the right as it did so. A few seconds later the No.1 rear main tyre failed. After crossing a storm drain located at the end of the runway both undercarriage bogies were damaged and the No.2 rear and No.3 front tyres burst. The aircraft became momentarily airborne as it left the lip of the embankment on which the runway was laid. As it did so, the left outer wing of the aircraft struck a steel lattice tower which ruptured the No.1A fuel tank and severed electrical cable looms in the leading edge of the wing. The aircraft fell heavily on to the ground below the runway level and broke up immediately on impact into three major portions, namely the tail empennage with the engines attached; the centre section and wings; and the forward part of the fuselage. After sliding a short distance the aircraft came to rest.

FIRE
Fire initially broke out when the left outer wing struck the lattice tower and the No.1A fuel tank was ruptured. The released fuel was ignited either by sparks generated as a result of the wing hitting the tower or by arcing due to disruption of electrical cable looms. There was evidence of burning on the ground from the point of impact with the tower to the final resting position, indicating that the fire trailed behind the aircraft for the whole distance.

According to the statements of survivors, fire appears to have started almost immediately after impact towards the rear and underside of the main cabin. There the heat was described as being intense at floor level.
Fire also broke out immediately after impact on the right side by the wing root. This prevented the emergency exits on that side being used. Fire eventually consumed the main cabin area, the forward fuselage, the left wing and the right wing root. The tail unit together with the engines were unburnt though extensively scorched.

**EVACUATION**
The crew consisted of a Captain, First Officer, Navigating Officer, Flight Engineer and 7 cabin crew. There were 96 passengers aboard.

In the main, the evacuation of the aircraft by the passengers and crew was self effected. Considerable selfless assistance was rendered by members of the cabin staff and also some of the passengers, some of whom died as a result of their efforts in this respect when they would have otherwise survived. The evacuation was facilitated considerably by the fortuitous fracture of the left forward fuselage, allowing relatively easy egress. Had it not been for this fracture casualties may well have been greater, as the left emergency exits were jammed by impact damage and the right side exits were blocked by fire.

Those who managed to get clear of the aircraft to the left side found their way blocked by a barbed wire fence. This forced most passengers and surviving members of the crew to walk down the slope alongside two main streams of fuel flowing from the aircraft. This fuel subsequently caught fire, trapping a number of people, believed to be about 10 in number.

**AIRCRAFT FACTORS**
The aircraft was a Super VC10 registered as 5X-UVA, operated by East African Airways and had been maintained in accordance with an approved maintenance schedule.

The aircraft was constructed in 1966 and went into service with East African Airways Corporation in that same year, having been issued with both a United Kingdom and an East African Certificate of Airworthiness.

[This series aircraft can be configured for up to 187 passenger seats.]

The fuselage was fitted with 2 doors at the front and rear and 2 overwing emergency exits above each wing.

**ENVIRONMENTAL CONDITIONS**
A weather observation made shortly after the accident gave the following information: wind 9 knots, temperature 21C, visibility 7-10km.

**INJURIES TO OCCUPANTS**
It appears that all those on board survived the impact, but some subsequently succumbed to the effects of the fire.

33 occupants were fatally injured by fire before evacuating. 10 cabin occupants succumbed to fire following evacuation.
2. **Fire Penetration Mechanism**

According to the statements of survivors, the fire appears to have started after impact towards the rear and underside of the main cabin. There the heat was described as being intense at floor level. This would be characterised by a burnthrough of the lower fuselage skin allowing the fire to enter under the cabin floor and propagate into the passenger cabin.

Fire was also able to enter the cabin directly through fuselage breaks at either end of the wing root.

Based on the above it is assessed that the prime burnthrough route was through the rear underside of the fuselage skin. However there was insufficient information to be conclusive about detailed burnthrough areas. The fire also entered through fuselage breaks.

3. **Location of Injuries and Scenarios**

![Diagram showing scenarios]

Scenario 1

Note: Survivors escaped through fuselage breaks.

Scenario 2
4. Accident Scenarios and Survivability Chains

This accident is divided into two separate scenarios.

**Scenario 1** contains the main cabin area. Evacuation was carried out through a fuselage break because the left exits were jammed and the right exits were blocked by fire. The scenario contains the 97 occupants including crew and passengers.

![Diagram of Scenario 1](image1)

**Scenario 2** contains 10 occupants who evacuated the aircraft but became trapped between two streams of burning fuel.

![Diagram of Scenario 2](image2)

5. Effect of Later Requirements

For Scenario 1, it is assessed that later requirements would have improved flammability standards such that the occupants would have had additional time to escape. It is assessed that an additional eight occupants would have successfully evacuated.

The survivability chain for Scenario 1 therefore becomes:

![Diagram of Improved Scenario 1](image3)
It is assumed that later requirements would not have affected the situation in Scenario 2 in which all fatalities occurred outside the cabin environment.

6. Effect of a Fire-Hardened Fuselage

Scenario 1

It is concluded that a fire-hardened fuselage would have delayed the entry of fire and provided the occupants with more time to make their way to the fuselage break. Fire would also have entered through the breaks in the fuselage which would not be affected by a fire-hardened fuselage. It is therefore assessed that ten occupants would still have succumbed to fire even with perfect fuselage fire hardening.

Due to insufficient detail on serious injuries, they have been assigned to zero throughout this accident.

The high, median, and low prediction from the use of a fire-hardened fuselage is:

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Scenario 2

It is assessed that a fire-hardened fuselage would not have affected the situation in Scenario 2 in which all fatalities occurred outside the cabin environment.

Summary

The assessed median number of lives saved by a fire-hardened fuselage are:

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1. Description of Accident

RESUME
A North Central Airlines DC-9-31 was taking off under poor visibility in fog. A Delta Airlines Convair CV-880 was taxying across the runway at the same time due to poor air traffic control. The DC-9 collided with the tail of the CV-880 and as a result was unable to climb and landed back on the runway. The undercarriage collapsed and the aircraft caught fire.

There were 4 crew and 41 passengers aboard. 9 passengers failed to escape the cabin fire because they could not locate the exits in dense smoke and poor lighting conditions.

IMPACT
After the collision, the captain decided that his aircraft could not maintain flight, at which time he took control and flew the aircraft back onto the runway.

The nose and main undercarriage collapsed and the aircraft came to a stop upright with the fuselage resting on the runway.

Passengers described the collision as being a slight bump. The subsequent touch down of the aircraft and the crash slide were described as being comparable to normal landing. Deceleration forces were described as being very slight with some side-to-side motion. None of the passengers reported being propelled into the seat in front of them.

FIRE
Shortly after impact, the DC-9 was engulfed in flames around the aft section of the aircraft and the fuselage was subsequently gutted by fire.

Smoke developed very rapidly which reduced the ability of the occupants to find emergency exits routes and made a co-ordinated crew response extremely important.

The control tower saw a flash but could not see the aircraft burning.

The first fire trucks were reported at the scene within 2 minutes. At the time of their arrival all survivors were already out of the aircraft and had moved away from the wreckage.

Firefighters extinguished the fire in 16 minutes.

EVACUATION
The crew consisted of a captain, first officer and 2 stewardesses. There were 41 passengers aboard.
The accident occurred at night. The cabin lights went out as the aircraft came to a stop and the emergency cabin standby lights were powered by rechargeable 2.5 volt nickel-cadmium batteries which produced a low light intensity when combined with the dense smoke. The extreme darkness reportedly made the location of emergency exits very difficult.

After the aircraft came to a stop smoke began to enter the cabin almost immediately. Some passengers stated that initially there was some pushing and shoving, but generally there was very little panic. Passengers reported having to get lower and lower toward the floor in order to breathe. The smoke was very dense, according to survivor accounts.

One stewardess was seated in the forward jump seat and the other stewardess was seated in seat 15B. After impact and during the slide, the rear stewardess opened the left forward overwing exit at row 12. After the aircraft had stopped, she exited and called to the passengers to follow. 4 passengers followed her out through the exit.

The forward stewardess opened the main entry door after the aircraft stopped. The escape slide deployed but did not inflate because the lanyard was wrapped around the neck of the inflation bottle. The stewardess stated that she was then pushed out of the aircraft. From the outside she called out to the passengers and assisted them down to the ground.

The first officer escaped from the aircraft through the sliding window on the right side of the cockpit. He went around the nose of the aircraft to the main entry door and from the ground he assisted passengers escaping through that door.

The captain entered the cabin through the cockpit door and called to the passengers to come forward. He then went outside through the main entry door. From a position outside the aircraft, he assisted passengers down to the ground. Then, re-entering the aircraft, the captain assisted other passengers through the main entry door. The rest of the survivors deplaned through the main entry door.

Most of the passengers indicated that their biggest obstacles in evacuating the aircraft were smoke and the lack of emergency lighting. Also the supervision of the evacuation by the flight and cabin crew members from a position outside the aircraft delayed egress of some of the passengers.

A passenger opened the right forward overwing exit through which he made his escape.

The rear tail cone emergency exit was not used. The galley exit door and the two aft overwing exits were not opened.

The 9 passengers remaining were found in the following locations by firemen:
- 1 male passenger in the tail cone aft of the pressure bulkhead
- 1 male passenger in the rear rest room
- 1 female passenger in the cockpit
- 1 male passenger halfway in the cockpit
- 5 female passengers (including 1 invalid) seated along the left side of the cabin area.

The 4 passengers who left their seats apparently attempted to find an exit but were unable to do so.

**AIRCRAFT FACTORS**

Aircraft N954N was a McDonnell Douglas DC-9-31, owned and operated by North Central Airlines, Inc. The aircraft was certificated, maintained and equipped in accordance with approved company procedures and FAA regulations. The aircraft had 22,000 lbs of Jet A aviation kerosene on board.

The aircraft was manufactured as serial number 47159 on 27-Dec-1967.

The fuselage was fitted with a main entry door at the front port side with a service door opposite. There were two overwing exits above each wing. The rear tail cone was also fitted with an emergency exit.

**ENVIRONMENTAL CONDITIONS**

The accident occurred in fog at night with a visibility of 1/4 mile.

**INJURIES TO OCCUPANTS**

9 of the 10 fatally injured passengers failed to escape from the aircraft. They received no traumatic injuries but succumbed instead to the effects of smoke inhalation or burns or both. Of the passengers who had left their seats, 2 were found in the cockpit area and 2 were found in the aft section. They apparently attempted to find an exit but were unable to do so in dense smoke and poor lighting conditions. 5 others remained in their seats.

The 10th fatally injured passenger succumbed 5 days later.

13 passengers and 2 crew were injured.

**2. Fire Penetration Mechanism**

The subsequent touch down of the aircraft and the crash slide were described as being comparable to normal landing. Deceleration forces were described as being very slight with some side-to-side motion. None of the passengers reported being propelled into the seat in front of them. It is therefore assumed that the fuselage was not ruptured by the impact forces.

Shortly after impact, the DC-9 was engulfed in flames around the aft section of the aircraft and after the aircraft came to a stop smoke began to enter the cabin. It was the smoke that dominated the evacuation time rather than flames. It is therefore assumed that the lower surface of the fuselage had burnt through rapidly and that smoke from under the cabin floor propagated into the passenger cabin and hindered attempts to locate the available exits.
Based on the above it is assessed that the prime burnthrough route was through the lower aft fuselage skin. However there was insufficient information to be conclusive about detail burnthrough areas.

3. Location of Injuries and Scenarios

Two passengers were initially in the main cabin but were found in the cockpit area having failed to locate an exit in dense smoke.

4. Accident Scenarios and Survivability Chains

This accident is divided into two separate scenarios.
Scenario 1 contains the flight deck area and two flight crew. It was subjected to the fire but the occupants escaped with minor or no injuries.

Scenario 2 contains the main passenger cabin, which suffered internal fire and had only three of the seven exits available for egress. It includes all passengers and flight attendants. Smoke was very dense and hampered attempts to locate open exits.

5. Effect of Later Requirements

It is assumed that later requirements would not have affected the situation in Scenario 1 in which there were no injuries sustained.

For Scenario 2, it is assessed that floor proximity lighting would have enabled two of the nine fatalities to find an available exit, i.e., the two passengers found in the cockpit who walked past the open main entry door.

The survivability chain for Scenario 2 therefore becomes:
6. **Effect of a Fire-Hardened Fuselage**

**Scenario 1**

It is concluded that a fire-hardened fuselage would not have affected the situation in Scenario 1 as no occupants sustained injuries.

**Scenario 2**

It is concluded that a fire-hardened fuselage would have delayed the entry of fire and provided the occupants with more time to locate the available exits.

The high, median, and low prediction from the use of a fire-hardened fuselage is:

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For an aircraft configured to the standards appropriate to the latest requirements this assessment becomes:

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Summary

The assessed median number of lives saved by a fire-hardened fuselage are:

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<tr>
<th>Burnthrough Protection Time</th>
<th>Aircraft in its Actual Configuration</th>
<th>Aircraft Configured to Later Requirements</th>
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<td>8 minutes</td>
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RESUME
On 22-Jan-1973 an Aila B707 registered as JY-ADO was landing in poor visibility at Kano airport, Nigeria.

The aircraft touched nosewheel first steady and heavy with main wheels barely in contact with the runway. The nosewheel entrenched itself then collapsed. The main gear contacted later in rapid deceleration, pierced the main wings and the aircraft collapsed along the runway centreline. A fire broke out and after a prolonged pause the passengers and crew evacuated.

Of the 202 occupants aboard, 176 suffered fatal injuries as a result of the fire.

IMPACT
As the undercarriage collapsed, indications are that the right hand main landing gear truck beam separated shortly after touchdown and the right hand main landing gear oleo dug through the tarmac surface. All four right hand main landing gear wheel rims were bent and broken, in varying degrees, indicating an unusually high loading during initial touchdown on the right hand main landing gear.

Inspection of the right hand main landing gear trunnion and wing box structure during the investigation indicated that the right hand main landing gear separated in an aft direction. There was no evidence to indicate that the right hand wing tanks opened up during the landing gear separation sequence.

At approximately 1100 feet from the runway threshold there is evidence that No. 1 and 2 engines dug heavily into the runway. Evidence indicates that the left hand outboard wing separated shortly after engine contact. There are indications that the No. 3 and No. 4 engines, the right hand outboard flap, aft fuselage and right hand horizontal stabiliser also contacted the runway.

The aeroplane proceeded down the runway in a shallow swerve to the right crossing the runway right hand edge approximately 2300 feet from runway threshold. The aeroplane continued on across a grassy strip coming to rest across a drainage ditch opposite the 3500 foot mark and 500 feet to the right of the runway centreline. The aeroplane had turned approximately 140 degrees to the right as it came to rest.

FIRE
The left hand outboard wing separation occurred at a point just inboard of the No, 1 engine strut. Shortly thereafter a severe fire occurred on the left hand wing which continued as the aircraft slid, until it came to rest on a grassy strip over a drainage ditch.
The fuselage forward of the aft pressure bulkhead and the wings were almost completely destroyed by fire.

**EVACUATION**
There were 3 flight crew, 8 cabin crew and 191 passengers aboard.

Once the aircraft came to a stop, the cockpit windows were opened and the crew exited to assist passengers out of the cabin.

There was a prolonged pause when nothing seemed to be happening to evacuating passengers, then an overwing exit was activated and a mass of passengers came through, in doing so they crushed a steward to death. This movement of a bulk of passengers from post-impact lethargy to safety coincided with the spread of fire.

Interviews with the surviving flight stewards indicated that a mass of people in the galley area prevented opening the galley door. Further statements verified the fact that four of the stewards were near the forward end of the passenger cabin and one steward was near the aft entry door at the time the aircraft impacted the runway. The steward who was stationed in the aft cabin section stated that he was unable to open the aft left hand entry door and escaped from the right hand aft galley exit. The stewards at the forward end of the cabin indicated that fire entered the cabin as the left hand forward entry door was opened and they escaped through the fire. Investigation at the scene revealed that both station 990 emergency exits were still in place after the fire had been extinguished.

None of the cabin crew spoke the language of the passengers.

**AIRCRAFT FACTORS**
The aircraft was a Boeing 707 registered as JY-ADO and operated by Aila Royal Jordanian Airlines.

The cabin was fitted with a passenger entry door at the front on the port side and a service door opposite. There were 2 overwing emergency exits above each wing and 1 each side near the wing trailing edge (Stn 990). There was an entry door at the rear on the port side and a service door opposite.

**ENVIRONMENTAL CONDITIONS**
The visibility along the runway was around 200 to 400 metres.

**INJURIES TO OCCUPANTS**
There has been no report available to the accident investigators to indicate that autopsies were performed. Therefore, the official cause for passenger deaths is not known. It appears that fire was the major cause of fatalities.

1 crew member and 175 passengers suffered fatal injuries.

10 crew members and 16 passengers suffered serious injuries.
2. **Fire Penetration Mechanism**

The stewards at the forward end of the cabin indicated that fire entered the cabin as the left-hand forward entry door was opened.

In addition there was extensive fire on the port wing, which can be assumed to have propagated into the cabin by burning through the fuselage side in that area. None of the port overwing exits had been opened.

Based on the above it is assessed that the prime burnthrough route was through the fuselage skin in the port wing root area. However there was insufficient information to be conclusive about detailed burnthrough areas. Fire also entered through the opened port forward entry door.

3. **Location of Injuries and Scenarios**

![Diagram showing Scenario 1 and Scenario 2]

4. **Accident Scenarios and Survivability Chains**

This accident is divided into two separate scenarios.

B-28
**Scenario 1** contains the flight deck area and the three flight crew. The occupants were able to escape through the cockpit windows.

(Fire scenario diagram)

**Scenario 2** contains the main passenger cabin, which suffered internal fire and had only three of the ten exits available for egress. It includes all passengers and flight attendants. Many fatalities were found crowded in the galley area and overcome by smoke.

(Fire scenario diagram)

5. **Effect of Later Requirements**

It is assumed that later requirements would not have affected the situation in Scenario 1, which contained only the flight deck area.

For Scenario 2, it is assessed that later requirements would have improved flammability standards such that the occupants would have had additional time to escape. It is assessed that an additional 41 occupants would have evacuated and 41 of the seriously injured would have escaped with minor or no injuries.

The survivability chain for Scenario 2 therefore becomes:

(Fire scenario diagram)
6. Effect of a Fire-Hardened Fuselage

Scenario 1

It is concluded that a fire-hardened fuselage would not have affected the situation in Scenario 1 as all occupants already had access to the cockpit windows for evacuation and were only injured when they re-entered the cabin in order to assist passengers.

Scenario 2

It is concluded that a fire-hardened fuselage would have delayed the entry of fire and provided the occupants with more time to use the available exits. Twenty-three occupants evacuated in 60 seconds, i.e., say 11 in 30 seconds. Assuming a constant evacuation rate, it is assessed that an additional maximum of 11 occupants would have escaped for each additional 30 seconds of protection. Further, 1/10 of the occupants who were seriously injured would escape with minor or no injuries for each additional 30 seconds of protection. Because fire also entered through the front main entry door no additional benefit was assigned after 4 minutes of burnthrough protection.

The high, median, and low prediction from the use of a fire-hardened fuselage is:

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<td>37</td>
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<td>M</td>
<td>46</td>
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<td>89</td>
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<td>88</td>
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</table>
For an aircraft configured to the standards appropriate to the latest requirements this assessment becomes:

For 30 seconds protection:

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H & 47 & 19 & 133 \\
M & 49 & 22 & 128 \\
L & 50 & 25 & 124 \\
\end{array}
\]

For 2 minutes protection:

\[
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H & 48 & 24 & 127 \\
M & 66 & 24 & 109 \\
L & 84 & 24 & 91 \\
\end{array}
\]

For 4 minutes protection:

\[
\begin{array}{ccc}
S & I & F \\
H & 49 & 31 & 119 \\
M & 91 & 25 & 83 \\
L & 132 & 20 & 47 \\
\end{array}
\]

For 8 minutes protection:

\[
\begin{array}{ccc}
S & I & F \\
H & 49 & 31 & 119 \\
M & 91 & 25 & 83 \\
L & 132 & 20 & 47 \\
\end{array}
\]

**Summary**

The assessed median number of lives saved by a fire-hardened fuselage are:

<table>
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<th>Aircraft in its Actual Configuration</th>
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<tr>
<td>8 minutes</td>
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1. Description of Accident

**RESUME**
On 30-Jan-74, B707-321B was making an ILS approach at night to Pago Pago International Airport in American Samoa. The aircraft encountered destabilising wind changes which resulted in an excessive descent rate and as a result the aircraft crashed short of the runway.

A fire broke out and the occupants could not open all the emergency exits. Of the 101 occupants, 97 suffered fatal injuries.

The aircraft was destroyed by the impact and subsequent fire.

**IMPACT**
Due to destabilising wind changes which resulted in an excessive descent rate the aircraft crashed into trees 3,865 feet short of the runway and then struck the ground.

The undercarriage landing gear was extended at the time of impact.

There was progressive destruction of the aircraft during its travel through the vegetation and as it slid over the ground. The fuselage remained intact except for the forward nose fuselage structure.

Passengers who survived the accident said that the impact forces were slightly more severe than a normal landing. No damage to the cabin interior was reported.

**FIRE**
Fire was evident during the last 350 feet of the wreckage pattern. The fuselage from the aft pressure bulkhead forward through the cockpit area was gutted by fire and the passenger cabin floor and contents were consumed. From the wing trailing edge forward, the top of the fuselage and the fuselage sidewalls were consumed by fire down to a point about 4 feet above the window line. The interior of the rear fuselage aft of the rear pressure bulkhead was not damaged by fire.

The response of the fire department was hampered by the weather, obstacles across the response route and the uncertainty of whether the fire was from an aircraft or a house. However it is doubtful that any of the occupants remaining in the aircraft were still alive when the fire and rescue personnel arrived at the scene.

**EVACUATION**
There were 10 crew and 91 passengers aboard. The crew consisted of a captain, first officer, third officer, flight engineer and 6 cabin crew.
Large fires were seen outside the right side of the aircraft. One person opened an overwing exit on the right side of the aircraft; flames came in through the exit, and he closed it. Other survivors opened the left overwing exits, and all the survivors except the first officer escaped through those exits. The wing was described as very hot and several survivors fell into flaming fuel at the trailing edge.

The first officer was assisted in his escape by two other cockpit crewmembers and left the aircraft through a hole in the cockpit wall.

The surviving passengers reported that some passengers rushed toward the front and rear of the cabin before the aircraft stopped. The survivors did not hear instructions regarding escape from the aircraft after the accident. Most of the survivors suffered burns and other injuries after they escaped from the cabin.

Post accident investigation revealed that the forward and the rear entry doors were not opened or used for escape. The forward door was opened about 2 to 3 inches, but the aft door was closed.

The forward galley service door could not be identified in the wreckage. The rear galley service door was found in place and locked.

It is possible that the flight attendants were overcome by smoke or that they tried to open the exits and did not redirect passengers to alternate exits. It is also possible that the passengers crowded around doors and for that reason the flight attendants were unable to open the exits.

It is unlikely that all of the passengers could have escaped from the aircraft through the 2 left overwing exits. However it is possible that there would have been more survivors had the passengers acted according to preflight instructions and proceeded to the nearest exit, instead of moving toward the main exits through which they had originally entered.

The movement of most of the passengers to the front and rear exits indicates that they either did not comprehend the pretakeoff briefing or they reacted to the emergency without thinking.

**AIRCRAFT FACTORS**

The aircraft was a B707 registered as N454PA, operated by Pan American World Airways. The aircraft was certificated, equipped and maintained in accordance with FAA regulations.

The aircraft had 117,000 lbs of Jet A-1 fuel aboard.

There were two floor level exits located in the front of the cabin and two floor level exits located in the rear of the cabin. Each door was a plug type that had to be opened inward and then rotated outboard. Each floor level exit was fitted with an automatically deployable and inflatable emergency evacuation slide. Two emergency exits were located over each wing. These exits were not fitted with evacuation slides.

The aircraft was configured as a 146 passenger capacity aircraft with some cargo on board as well.
The first class section contained four rows of two double seat units for a total capacity of 16 passengers. Located forward of the first class section were two galley units and two lavatories on the right side of the cabin and a galley unit, console bar, and two flight attendant jump seats on the left side of the cabin. Each of the jump seats were double occupancy, rear-facing seats fitted with seat belts but no shoulder harnesses. A cabin divider was installed between the first class and economy class sections of the cabin. The economy class section contained twenty rows of two triple seat units one row of two double seat units and two rows of single triple seat units for a total capacity of 130 passengers. At the rear of the economy class cabin there were three galley units, a carry-on baggage compartment, three lavatories and a double forward-facing flight attendant jumpseat. The jumpseat was fitted with seat belts and shoulder harnesses. None of the jumpseat locations afforded a view of the passenger compartment.

ENVIRONMENTAL CONDITIONS
The accident occurred at night, below clouds, in rain. After the crash the rain became heavier.

INJURIES TO OCCUPANTS
Of the 101 occupants, 97 suffered fatal injuries. 9 passengers and 1 crew member survived the crash and fire. 1 passenger died the next day, the crew member and 3 passengers died 3 days after the accident. 1 passenger died 9 days after the accident but at that time 'fatalities' had to occur within 7 days of the accident so it was counted as a survivor. [However for consistency, the current definition of a fatality has been used and hence this passenger has been determined to have sustained fatal injuries.]

Except for the Third Officer, all fatally injured persons died of smoke inhalation, massive 1st, 2nd and 3rd degree burns and complications from those massive burns. The Third Officer survived the crash but died later of traumatic leg and arm injuries and severe burns.

Toxicological examinations of the casualties revealed significant levels of carbon monoxide and hydrogen cyanide which are normal byproducts of aircraft fires.

2. Fire Penetration Mechanism

The fuselage remained intact as it came to rest. A fuel-fed fire broke out along the whole of the right-hand side and it can be assumed that this pool of fire burnt through the underside of the fuselage and then entered the passenger cabin from below. Indeed large parts of the right fuselage sidewall were eventually consumed by the flames as was the cabin roof.

The fire services were delayed in their efforts to reach the site and as a result the fire became quite severe. However it was unlikely that there were any further survivors to be rescued.

Based on the above it is assessed that the prime burnthrough route was through the fuselage skin lower surface. However there was insufficient information to be conclusive about other burnthrough areas.
4. Accident Scenarios and Survivability Chains

This accident is divided into two separate scenarios.
Scenario 1 contains the flight deck area. It was subjected to the initial impact which was not severe but did create a hole in the forward skin that the third officer used to crawl out from and escape. The scenario contains the four flight crew.

Scenario 2 contains the main passenger cabin, which suffered internal fire and had only the two left overwing exits available for egress. It includes all passengers and flight attendants. Survivor statements indicate that the cabin occupants were moving up and down the cabin trying to locate and open exits. Most were found overcome by smoke crowded near the exits.

5. Effect of Later Requirements

It is assumed that later requirements would not have affected the situation in Scenario 1, which contained only the flight deck area.

For Scenario 2, it is assessed that many were injured outside the aircraft in the external fire and hence later requirements would have only had a modest effect.
The survivability chain for Scenario 2 therefore becomes:

![Diagram](image)

6. **Effect of a Fire-Hardened Fuselage**

**Scenario 1**

It is concluded that a fire-hardened fuselage would have delayed the entry of fire and provided the flight crew with more time to either help open the forward main passenger door or follow the third officer out of the hole in the cockpit wall. The high, median, and low prediction from the use of a fire-hardened fuselage is:

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For 8 minutes protection:

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\begin{array}{ccc}
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H & 0 & 0 & 4 \\
M & 2 & 2 & 0 \\
L & 2 & 2 & 0 \\
\end{array}
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**Scenario 2**

It is concluded that a fire-hardened fuselage would have delayed the entry of fire and smoke and provided the occupants with more time to use the available overwing exits or to open the forward entry door. However, little benefit has been assigned due to the panic behaviour of the occupants and because those evacuating had to exit close to the external fire and risked injury outside the cabin environment.

The high, median, and low prediction from the use of a fire-hardened fuselage is:

For 30 seconds protection:

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\begin{array}{ccc}
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M & 1 & 8 & 88 \\
L & 2 & 12 & 83 \\
\end{array}
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For 2 minutes protection:

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\begin{array}{ccc}
S & I & F \\
H & 0 & 4 & 93 \\
M & 1 & 8 & 88 \\
L & 2 & 12 & 83 \\
\end{array}
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For 4 minutes protection:

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S & I & F \\
H & 0 & 4 & 93 \\
M & 1 & 8 & 88 \\
L & 2 & 12 & 83 \\
\end{array}
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For 8 minutes protection:

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\begin{array}{ccc}
S & I & F \\
H & 0 & 4 & 93 \\
M & 1 & 8 & 88 \\
L & 2 & 12 & 83 \\
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For an aircraft configured to the standards appropriate to the latest requirements this assessment becomes:
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Summary

The assessed median number of lives saved by a fire-hardened fuselage are:

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<tr>
<th>Burnthrough Protection Time</th>
<th>Aircraft in its Actual Configuration</th>
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1. Description of Accident

RESUME

On 15-Mar-74, a Stirling Airways Caravelle registered as OY-STK was taxying at Teheran/ Mehrabad International Airport, Teheran, Iran.

Shortly before the aircraft was going to initiate a left turn towards the run-up area two loud noises were heard in the aircraft and the right wing dropped to the ground and struck the runway. The aircraft came to a stop 90 m further on and a heavy fire developed.

Evacuation of the aircraft was carried out through exits on the left side of the aircraft.

Preliminary investigation revealed that the right landing gear collapsed due to structural failure of a fitting (lower 'candelabra'). A rupture of the fuel tank resulted and the JP 1 fuel escaping from the tank ignited before the aircraft came to a stop. The source of the ignition was not determined.

There were 4 crew and 92 passengers aboard. 15 passengers suffered fatal injuries.

IMPACT

The aircraft was taxying at the time of the accident and there was no impact.

FIRE

After refuelling at Teheran/Mehrabad International Airport, the aircraft was cleared to taxi for take-off. Due to some construction work it was cleared to back track on the runway to the take-off position. Whilst the aircraft was taxying on the runway, it was requested by the ATC to expedite taxying and to clear the runway on the run-up area close to the runway threshold because another aircraft was on final approach.

Shortly before the aircraft was going to initiate a left turn towards the run-up area 2 loud noises were heard in the aircraft and the right wing dropped to the ground and struck the runway.

The aircraft came to a stop 90m further on and a heavy fire developed.

Preliminary investigation revealed that the right landing gear collapsed due to structural failure of a fitting (lower 'candelabra'). A rupture of the fuel tank resulted and the JP 1 fuel escaping from the tank ignited before the aircraft came to a stop. The source of the ignition was not determined.

EVACUATION

There were 4 crew and 92 passengers aboard.
Evacuation of the aircraft was carried out through exits on the left side of the aircraft.

Evacuation time was thought to be approximately 2 minutes.

**AIRCRAFT FACTORS**
The aircraft was a Caravelle 10B3 registered as OY-STK and operated by Stirling Airways.

The aircraft was carrying JP 1 fuel.

There was an up-and-over main entry door at the front port side with a service door opposite. Two emergency exits were located over each wing.

**ENVIRONMENTAL CONDITIONS**
The accident occurred during daylight with little or no prevailing wind.

**INJURIES TO OCCUPANTS**
There were 4 crew and 92 passengers aboard. 15 passengers suffered fatal injuries.

2. Fire Penetration Mechanism

As the aircraft came to a stop, leaking fuel formed a pool next to the fuselage on the starboard side and ignited.

It can be concluded that with a pool fire on the starboard side of the fuselage, the skin would have been burnt through from underneath after approximately 30 seconds, causing smoke and flames in the cabin. This can be assumed to be during the evacuation period as it was estimated that evacuation took 2 minutes.

With this type of fire, penetration would have been initially through the lower fuselage skin and later into the passenger cabin through the floor. Once smoke was present in the cabin the occupants would have become disorientated and unable to locate the open exits.

Based on the above it is assessed that the prime burnthrough route was through the lower fuselage skin. However there was insufficient information to be conclusive about detailed burnthrough areas.

3. Location of Injuries and Scenarios

Because of the absence of documentary evidence on details of the evacuation, the whole fuselage volume has been taken as a single scenario which includes all occupants.

4. Accident Scenarios and Survivability Chains

This accident is taken as one scenario.
**Scenario 1** contains the whole aircraft and encompasses 4 crew and 92 passengers.

5. **Effect of Later Requirements**

It is assumed that later requirements would have provided additional time for evacuation such that an additional two occupants would have located open exits and evacuated successfully.

It is assessed that this would result in the survivability chain becoming:

![Diagram showing Scenario 1](image)

6. **Effect of a Fire-Hardened Fuselage**

**Scenario 1**

It is concluded that a fire-hardened fuselage would have delayed the entry of fire and provided the passengers with additional time to locate and use the available exits.

In the absence of detail information on serious injuries, these have been assigned to zero throughout this accident.
The high, median, and low prediction from the use of a fire-hardened fuselage is:

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For an aircraft configured to the standards appropriate to the latest requirements this assessment becomes:

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**For 4 minutes protection:**
Summary

The assessed median number of lives saved by a fire-hardened fuselage are:

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<th>Burnthrough Protection Time</th>
<th>Aircraft in its Actual Configuration</th>
<th>Aircraft Configured to Later Requirements</th>
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<td>8 minutes</td>
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APPENDIX C—ACCIDENT RATIONALES 8 TO 17
The rationale for each accident that was analysed appears in this appendix. Each accident is presented with the following sections:

- Description of the accident reproduced from the Survivable Accidents Database.
- A description of the fire penetration mechanism using relevant extracts from the database together with any assessments and assumptions made.
- A diagram showing the location of occupants and scenario boundaries.
- Survivability chains for each scenario.
- Effect of later requirements.
- Effect of a fire-hardened fuselage.

The accidents are presented in reverse chronological order.

Key to injury location diagrams:

- **FI**: FATAL IMPACT
- **FM**: FATAL MECH ASPHYX
- **FB**: FATAL BURN
- **FA**: FATAL ASPHYX/TOXICITY
- **FB**: FATAL BURN IMPACT INJ
- **FA**: FATAL ASPHYX/TOXICITY IMPACT INJ
- **FW**: FATAL WATER
- **FWI**: FATAL WATER IMPACT INJURY
- **FU**: FATAL UNDETERMINED
- **SI**: SERIOUS IMPACT
- **SF**: SERIOUS FIRE
- **SW**: SERIOUS WATER
- **SWF**: SERIOUS IMPACT/FIRE
- **SWI**: SERIOUS IMPACT/WATER
- **MN**: MINOR/NONE
- **U**: UNOCCUPIED
- **LU**: SURVIVED INJURIES UNKNOWN

Exit
Exit used for evacuation
1. Description of Accident

RESUME
Indian Airlines B737 registered as VT-EAL was taking off from Hyderabad Airport on 17-Dec-78. The leading edge devices did not deploy and as a result the aircraft became aerodynamically unstable. The take-off was aborted and the aircraft was flared for a belly landing with undercarriage retracted. The aircraft belly landed in nose up, left wing low attitude, on the centre line of the runway. It slid for 3080 feet, hit a boundary fence, crossed a drain and ploughed in rough terrain negotiating with small boulders and came to rest. Fire broke out on impact.

The aircraft was completely destroyed by fire.

There were 6 crew and 126 passengers aboard, of which 1 passenger suffered fatal burn injuries, 1 cabin attendant and 3 passengers were seriously injured.

3 persons cutting grass near the boundary fence of the airport were killed.

IMPACT
The leading edge devices did not deploy and as a result the aircraft experienced a severe shudder and became aerodynamically unstable. The aircraft did not gain any height and started to sink.

The take-off was aborted and the aircraft was flared for a belly landing with undercarriage retracted. The aircraft belly landed in nose up, left wing low attitude, on the centre line of the runway.

The aircraft slid for 3080 feet, hit a boundary fence, crossed a drain and ploughed in rough terrain negotiating with small boulders and came to rest.

As the aircraft slid into rough terrain it began breaking up, shed the port engine, starboard engine and engine accessories. The rest of the aircraft remained intact.

The ground markings included drag marks at a distance of 7185 ft from the beginning of the runway, indicating first contact of the aircraft. On the right hand side a second drag mark was found at a distance of 7293 ft. A third drag mark was observed at a distance of 7385 ft. It was concluded that the first and second were caused by the port engine and the third by the starboard engine. A centre traverse mark was intermittent and was caused by the fuselage. Debris was found littered alongside the tracks.

ATF spillage could be inferred from the soakage in the overrun area.

The undercarriage was found in the retracted position and appeared to be locked. The main wheels were lying fore and aft instead of one above the other.
**FIRE**
The fire appeared initially at the trailing edge of the port wing and spread to the starboard side of the aircraft. Fuel from tanks leaked and collected underneath the starboard side and due to wind the fire intensified. At first the right wing became engulfed in fire and later the middle and forward portions of the fuselage. The upholstery of the aircraft was burnt in 2 hours. Witnesses stated that at times the fire subsided and again flared due to bursting of fuel tanks.

The interior of the passenger cabin and cockpit areas were completely burnt out.

Fire fighting operations were not successful because the fire crew could not get close to the wreckage quickly enough. This was partly due to their perception that the aircraft was at the end of the runway where, in fact, it was significantly further into the rough terrain and the crew were not acquainted with the terrain in that area. The fire engines could not get past a drain and had to fight the fire from some distance.

**EVACUATION**
The crew consisted of a Captain, Co-pilot and 4 cabin crew. There was also a pilot who occupied the observer's seat.

There were 126 passengers aboard.

The Captain said he was unable, due to preoccupancy, to announce emergency and order passenger evacuation. The emergency drill was not adopted and the control tower was not alerted. The fire crew, however, observed the crash landing and proceeded at once to the site.

[The accident report did not state which doors were used in the evacuation.]

**AIRCRAFT FACTORS**
The aircraft was a basic series B737-200 registered as VT-EAL and operated by Indian Airlines. It was maintained and operated in accordance with airworthiness requirements.

The aircraft was manufactured in 1970 as serial number 20485. Airworthiness Certificate number 1576 was issued on 9-Mar-1971 on the strength of FAA Export Certificate of Airworthiness E9894.

The aircraft was carrying Jet-A fuel.

The aircraft was fitted with a main entry door in the cabin at the front on the port side with a service door opposite. There was an entry door at the rear on the port side with a service door opposite. There was a single overwing emergency exit above each wing.

**ENVIRONMENTAL CONDITIONS**
On the day of the accident the weather was reported to be: wind 120 deg, 10 knots, visibility 10 km, temperature 23 C.
INJURIES TO OCCUPANTS
There were 6 crew and 126 passengers aboard, of which 1 passenger suffered fatal burn injuries, 1 cabin attendant and 3 passengers were seriously injured.

As a result of the fire, one passenger suffered serious burn injuries all over his body. He was admitted to hospital but died 3 days later.

Other serious and minor injuries were attributed to the impact:

1 passenger received a fracture injury on her tibia and her fibula was injured.
1 passenger suffered a fracture of the bone below his left knee.
1 passenger received a fracture of her left hand.
1 passenger received a fracture of his left leg.
1 passenger sustained a fracture of his left heel bone.
1 stewardess received injury narrowing the space between her 11th and 12th vertebrae.
1 stewardess received incised injury on her forehead.

There were 14 passengers (all male) who received minor injuries.

2. Fire Penetration Mechanism

The fire appeared initially at the trailing edge of the port wing and spread to the starboard side of the aircraft. Fuel from tanks leaked and collected underneath the starboard side and due to wind the fire intensified. At first the right wing became engulfed in fire and later the middle and forward portions of the fuselage.

It can be reasonably assumed that the ignited fuel that collected either side of the fuselage would have burnt through the fuselage under surface in the area of the wing root/fuselage junction. There were no breaks in the fuselage due to impact forces.

Once the skin was penetrated, smoke and flames would have entered the passenger cabin through the cabin floor.

As only one passenger was reported to have suffered (and later died of) burn injuries, it can be assumed that the evacuation was nearly completed before the entry of smoke and fire became a threat to the occupants. Details of the evacuation process did not appear in the official accident report.

Based on the above it is assessed that the prime burnthrough route was through the fuselage skin. However there was insufficient information to be conclusive about other burnthrough areas.
3. Location of Injuries and Scenarios

Seriously injured flight attendant was arbitrarily assigned to this position. Her location has no effect on this analysis.

Note: The accident report did not state which doors were used in the evacuation.

Pilot who occupied the observer's seat has been counted as a passenger for the purpose of this analysis.

4. Accident Scenarios and Survivability Chains

This accident is divided into four separate scenarios.
**Scenario 1** contains the flight deck area, which was subjected to the initial impact. The scenario contains two flight crew and a pilot in the observer's seat.

**Scenario 2** contains the forward flight attendant area and encompasses two flight attendants.

**Scenario 3** contains the main passenger cabin, which suffered internal fire after the initial impact. The scenario contains 125 passengers.
Scenario 4 contains the rear flight attendant area and encompasses two flight attendants.

5. Effect of Later Requirements

It is assumed that later requirements would not have affected the situation in scenarios 1, 2, and 4 in which all injuries were related to the impact.

For Scenario 3, it is assessed that later requirements would have provided additional time to escape for the only occupant injured by the fire. In this event it is assessed that the injuries would have been less severe and therefore not fatal.

The survivability chain for Scenario 3 therefore becomes:

* The seriously injured flight attendant was arbitrarily assigned a seat position. Her location has no effect on this analysis.
6. Effect of a Fire-Hardened Fuselage

Scenario 1

It is concluded that a fire-hardened fuselage would not have affected the situation in Scenario 1 as there were no injuries.

Scenario 2

It is concluded that a fire-hardened fuselage would not have affected the situation in Scenario 2 as there were no injuries.

Scenario 3

It is concluded that a fire-hardened fuselage would have provided sufficient time for the passenger with burn injuries to escape with lesser injuries.

The high, median, and low prediction from the use of a fire-hardened fuselage is:

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L & 122 & 3 & 0 \\
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M & 122 & 3 & 0 \\
L & 122 & 3 & 0 \\
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**Scenario 4**

It is concluded that a fire-hardened fuselage would not have affected the situation in Scenario 4 as the only injury was related to the impact.

**Summary**

The assessed median number of lives saved by a fire-hardened fuselage are:

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AIRCRAFT: DC8  DATE OF ACCIDENT: 7th October '79
REGISTRATION: HB-IDE  LOCATION OF ACCIDENT: ATHENS

1. Description of Accident

RESUME
On 7-Oct-79, a DC-8-62 was landing at night on a wet runway at Athens Airport, Greece.

After a late and fast touchdown and after ineffective breaking, the aircraft overshot the runway end and the overrun area, fell down a slope of 4 m and caught fire.

Of the 154 occupants, 14 suffered fatal injuries as a result of the fire.

The aircraft was destroyed by impact and fire.

IMPACT
Touchdown was at approximately 500m from the displaced runway threshold. There was a very short flare phase with the effect that the nose wheel touched down almost simultaneously with the main landing gear. Immediately after touch down the captain selected idle reverse on all 4 engines and took control of the aircraft from the co-pilot. The co-pilot set normal reverse. A few seconds later brake application was initiated which gave an impression of almost normal braking conditions.

At approximately 500-600m before the end of the runway braking efficiency dropped and full braking was applied. In spite of this action braking efficiency dropped to almost nil within seconds. Still the impression prevailed that with reverse deceleration it would be possible to stop the aircraft before the runway end. At approximately 300m before the runway end a first doubt came up about this fact. The captain tried to test directional steering capability by turning the nose wheel slightly to the left. No rotation of the aircraft was felt. In this condition the aircraft was skidding the remaining distance of the runway and with a speed of approximately 20-25 knots overran the runway end.

After rolling over the asphalt overrun area of approximately 65m length, the aircraft fell approximately 4m onto a bitumen road which crossed the runway axis almost rectangularly.

The first impact occurred with the nose wheel within reach of the road. The second impact must have occurred with the afterbody/tail on the end of the overrun area. Approximately at the same time the nose wheel was bent backwards due to the terrain which rises by about 0.5 m. The main landing gear struck the ground between a bank and the road. The aircraft skidded further before coming to a halt, the tail being approximately 76.5m from the end of the runway.

The aircraft was destroyed by the hard impact in the vicinity of the road and by the ensuing fire. The left wing was broken in front of the No. 1 engine. The fuselage was broken in front of the vertical stabiliser. The nose wheel was bent backwards. The right and left main landing gear
were torn off the wing structure with parts of the rear spar bent backwards and twisted by approximately 90° for the left and by approximately 180° for the right.

The aircraft was carrying 40 packages of radio active material for medical purposes. Experts found that the packages were for the most part destroyed due to the ensuing fire. However no leakage of radio active materials occurred into the atmosphere.

FIRE
As soon as the aircraft overshot the runway and stopped outside the airport's boundary, fire broke out at the right main part of the fuselage. The impact forced the right main landing gear to bend backwards by approximately 180° and tore open the fuel tanks of the right wing. Fuel from these tanks started to flow and the fire was extended not only to the aircraft, but to the road at the right side of the aircraft. This meant that access to the aircraft's right side was impossible.

The fire fighting service was in action in approximately 3 minutes and 3 of the fire fighting cars which had turrets began action without any delay combating the fire from the edge of the slope. 3 cars from the US base fire brigade fought the fire at the tail and the right side of the aircraft. Another fire service took over the left side from which passengers were still evacuating.

While the fire had begun to be under control some small explosions were heard, probably from exploding oxygen bottles and fire covered the aircraft completely.

One rescuer equipped with fire proof equipment and smoke mask did not enter the aircraft because he was waiting for a specific order. This was not given because the rescue crew were out of the protective range of the turrets.

The fire was under control within about 25 minutes and extinguished after about 2 hours from the initial crash. A total of 20 fire fighting cars and 75 fire crew took part in the operation.

EVACUATION
There were 3 flight crew, 7 cabin crew and 144 passengers aboard.

As soon as the aircraft came to rest, the captain left the aircraft through the cockpit window in order to assist passengers as they came out of the forward exit. The crew opened the left front and rear doors. The front door opened normally, and the slide worked quickly. The rear door had some difficulty in opening, probably because of deformation, it was opened with some delay. The slide did not deploy.

The crew was giving the appropriate instructions for quick evacuation and passengers were leaving the aircraft normally at the front. However the loss of the PA system meant that no information regarding the use of the exits could be transmitted. As a result confusion was created to the passengers in the central area of the aircraft who could not use the overwing exits due to the external fire.
According to crew and passenger statements, approximately 120-130 passengers left the aircraft through the left front exit. The exit doors and emergency exits at the right side of the aircraft were not used. Overwing exits at the left side were not used either because, according to the flight attendants' statements, there was external fire in that area. The delay in opening the rear door resulted in passengers moving back and forth. The rear of the cabin had more smoke than the front part due to the lack of draught.

The passengers were finally directed to the front door. The front slide failed after it had been used by 40-50 passengers and the passengers were then jumping from a height of 1.70 metres. 11 passengers sustained minor injuries due not only to the height but also to the fact that they were jumping on each other. The evacuation lasted approximately 3.5 to 4.5 minutes. The co-pilot re-entered the aircraft after the last passenger was out to search for passengers. He couldn't see anything because of smoke.

According to witness statement as well as the Swissair boarding cards, the 14 dead persons were seated at the rear part of the aircraft between the 21st and 26th row. It seems that these passengers hadn't tried to leave the aircraft, considering the evacuation had been completed from the rear door, as stated by the flight attendant. 5 of the dead were seated in row 25. Many passengers walked through that area and no one had reported any difficulties in passing through.

It was calculated that the evacuation lasted approximately 3.5 to 4.5 minutes.

**AIRCRAFT FACTORS**
The aircraft was a DC-8-62 registered as HB-IDE, operated by Swissair.

The aircraft was manufactured as serial number 45919 and delivered on 22-Nov-1967.

The aircraft had 4 exit doors, 2 at the front part and 2 at the rear part of the cabin, 4 overwing emergency exits and 2 sliding windows in the cockpit.

**ENVIRONMENTAL CONDITIONS**
The accident occurred at night on a wet runway. Wind was 090°, 17 knots. Visibility was 7 km in light rain. The temperature was 18C.

**INJURIES TO OCCUPANTS**
Of the 10 crew and 144 passengers aboard, 14 passengers suffered fatal injuries.

Fourteen fatalities were found sitting in rows 21-26. The forensic medical postmortem reports testify that the death of 14 passengers was caused by burns of third degree on the whole body.

11 passengers suffered minor injuries during evacuation.

2. **Fire Penetration Mechanism**

As the aircraft came to a stop, leaking fuel ignited and extended along the entire starboard side of the fuselage. In addition the port overwing exit doors were not opened because the cabin crew saw fire in that area.
It can be concluded that with fire on both sides of the fuselage, the skin would have burnt through after approximately 30 seconds, causing smoke and flames in the cabin. This can be assumed to be during the evacuation period as it was estimated that evacuation took 3.5 to 4.5 minutes. Indeed, fire services were on scene in 3 minutes and saw passengers still evacuating.

With this typical pool fire environment, fire penetration would have been initially through the lower fuselage skin, baggage bay, or undercarriage bay and later into the passenger cabin through the floor. Once smoke was present in the cabin, the occupants would have become disorientated and unable to locate the open exits. It was considered that occupants were not rendered immobile by the impact forces as movement was reported of passengers back and forth during the delay in opening the rear port door.

Based on the above it is assessed that the prime burnthrough route was through the fuselage skin. However there was insufficient information to be conclusive about other burnthrough areas.

3. **Location of Injuries and Scenarios**

![Diagram of scenarios]

Scenario 6

Scenario 5

Scenario 4

Scenario 3

Scenario 2

Scenario 1
4. Accident Scenarios and Survivability Chains

This accident is divided into six separate scenarios:

**Scenario 1** contains the flight deck area encompassing the three flight crew who were not injured.

**Scenario 2** contains the forward flight attendant area with four flight attendants, all of who escaped uninjured.

**Scenario 3** contains the front section of the passenger cabin from seat row 1 to seat row 20 inclusive. A uniform distribution of passengers was assumed; therefore this scenario contains an estimated 92 passengers, all of whom evacuated without injury.
**Scenario 4** contains the midsection of the passenger cabin from seat row 21 to seat row 26 inclusive. A uniform distribution of passengers was assumed; therefore this scenario contains an estimated 27 passengers, 14 of whom succumbed to the fire.

![Scenario 4 Diagram]

**Scenario 5** contains the rear section of the passenger cabin from seat row 27 to the end (taken as seat row 32 with 3 abreast in the tapered fuselage) inclusive. A uniform distribution of passengers was assumed; therefore this scenario contains an estimated 25 passengers, all of whom evacuated without injury.

![Scenario 5 Diagram]

**Scenario 6** contains the rear flight attendant area encompassing three flight attendants, all of whom evacuated without injury.

![Scenario 6 Diagram]
5. **Effect of Later Requirements**

It is assumed that later requirements would not have affected the situation in scenarios 1, 2, 3, 5, and 6 which already had sufficient time for successful evacuation.

For Scenario 4, it is assessed that later requirements would have improved passenger orientation and an additional eight passengers would have been able to locate the useable exits. The survivability chain for Scenario 4 becomes:

6. **Effect of a Fire-Hardened Fuselage**

**Scenario 1**

It is concluded that a fire-hardened fuselage would not have affected the situation in Scenario 1 as there was already sufficient time for a successful evacuation.

**Scenario 2**

It is concluded that a fire-hardened fuselage would not have affected the situation in Scenario 2 as there was already sufficient time for a successful evacuation.

**Scenario 3**

It is concluded that a fire-hardened fuselage would not have affected the situation in Scenario 3 as there was already sufficient time for a successful evacuation.

**Scenario 4**

It is concluded that a fire-hardened fuselage would have delayed the entry of fire and provided the occupants with additional time to use the available exits.

The high, median, and low prediction from the use of a fire-hardened fuselage is:
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**Scenario 5**

It is concluded that a fire-hardened fuselage would not have affected the situation in Scenario 5 as there was already sufficient time for a successful evacuation.

**Scenario 6**

It is concluded that a fire-hardened fuselage would not have affected the situation in Scenario 5 as there was already sufficient time for a successful evacuation.

**Summary**

The assessed median number of lives saved by a fire-hardened fuselage are:

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<tr>
<th>Burnthrough Protection Time</th>
<th>Aircraft in its Actual Configuration</th>
<th>Aircraft Configured to Later Requirements</th>
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<td>8 minutes</td>
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</table>
1. Description of Accident

RESUME
On 13-Sep-82 a Spantax DC-10-30F registered as EC-DEG was taking off from Malaga Airport in Spain.

The pilot aborted the takeoff because of 'vibrations' of unknown origin. The aircraft proceeded off the end of the runway and struck a number of objects, creating sufficient damage to cause fuel spillage, but no fire, as it decelerated.

Approximately 700 feet from the end of the runway, the aircraft crossed a road and struck a house. This impact was quite severe, ripping off the right wing and creating a fireball. The fireball 'followed' the aircraft until it came to rest approximately 1000-1200 feet beyond the runway.

Of the 13 crew and 381 passengers aboard, 3 crew and 47 passengers suffered fatal injuries. 40 passengers were seriously injured.

IMPACT
The aircraft proceeded off the end of the runway and struck a number of objects, creating sufficient damage to cause fuel spillage, as it decelerated.

Approximately 700 feet from the end of the runway, the aircraft crossed a road and struck a house. This impact was quite severe, ripping off the right wing. The fuselage was believed to be entirely intact at this point, resting on its belly.

FIRE
A large external fuel fire developed on the right hand side of the aircraft aft of the wing area. The fire size was estimated 25 feet long (fuselage direction) and 50 feet wide. Flames extended 2-21/2 times the height of the fuselage. A much smaller fuel fire formed on the left hand side. The wind speed was 14 knots. The wind vector was forward to aft and at a slight angle with the fuselage centre line, tending to bend the flames away from the fuselage.

There are witness accounts that fire broke in through the tail and dense smoke seeped in probably through a tear in the upper part of the passenger cabin at the height of door 4R.

The aircraft was gutted from the inside-out by fire (the external fuel fire was extinguished by the fire department).

EVACUATION
There were 2 flight crew, 11 cabin crew and 381 passengers aboard.
Slides were deployed for the front emergency exits immediately: L1, L2, and R1. Door R2 was opened later by a passenger (intense fire on that side). After 3 or 4 passengers exited, the slide that served R2 was disabled by fire. Because that landing gear was sheared off, the slides formed a shallow angle and occupants were able to ‘walk down the slides’.

Door R3 was not opened due to the intense fire on that side. The stewardess in charge of opening door L3 saw fire on the left side of the plane but decided to open it anyway because she noted the fire was more intense on the right side. The three stewardesses located toward the plane's tail tried to open doors L4 and R4 without success according to witness accounts.

The evacuation took place slowly because the passengers picked up their carried luggage before evacuating. In the third cabin, besides the problems brought about by hand-carried luggage, a bottleneck resulted due to the number of passengers, most of them on the left aisle, that were trying to reach door L3. On top of that, evacuation was carried on with difficulty due to the fire having destroyed the L3 slide.

The lack of visibility, due to the fire and smoke, and the cabin dividers made it impossible to have a view of the plane as a whole, and consequently, three different evacuations were carried out. One from each cabin.

The 91 passengers in the first cabin left the plane through doors L1, R1 and L2. The 122 in the second cabin left through doors L2, L3 and some through R2. The third cabin was occupied by 167 passengers. On these, the 117 that evacuated the plane did so by using door L3 which was affected by the fire through most of the process. The L3 slide was rendered useless. The 47 passengers and three crew members that died occupied the third cabin.

**AIRCRAFT FACTORS**
The aircraft was a DC-10-30F registered as EC-DEG and operated by Spantax.

The cabin was fitted with 4 passenger doors down each side.

**ENVIRONMENTAL CONDITIONS**
The accident occurred in daylight. The prevailing weather conditions at the time of the accident were dry, no cloud and wind at 14 kt.

**INJURIES TO OCCUPANTS**
Of the 13 crew and 381 passengers aboard, 3 crew and 47 passengers suffered fatal fire injuries. 40 passengers were seriously injured.

2. **Fire Penetration Mechanism**

When the aircraft came to a stop, a large external fuel fire developed on the right-hand side of the fuselage aft of the wing area and a much smaller fuel fire formed on the left-hand side.

The wind direction was forward to aft and at a slight angle with the fuselage centre line, tending to bend the flames of the large fire away from the fuselage side. However, it can be assumed
that flames from the smaller fire were being directed onto the fuselage side at the rear of the cabin and would have caused a burnthrough from the left side.

In addition, smoke was seeping in through a tear in the upper part of the passenger cabin at the height of door 4R. It is assessed, however, that there was no immediate burnthrough in this area.

The wreckage was described as being gutted from the inside out by fire. It is assumed that this would have occurred well after the evacuation was complete.

Based on the above it is assessed that the prime burnthrough route was through the fuselage skin at the rear on the left side. However there was insufficient information to be conclusive about detail burnthrough areas.

3. Location of Injuries and Scenarios
4. **Accident Scenarios and Survivability Chains**

This accident is divided into four separate scenarios.

**Scenario 1** contains the flight deck area where all occupants successfully evacuated. The scenario contains the two flight crew.

**Scenario 2** contains the forward passenger cabin area where all occupants successfully evacuated. The scenario contains 91 passengers and 4 flight attendants.

**Scenario 3** contains the mid passenger cabin area where all occupants successfully evacuated. To account for all passengers it has been assumed that there were 123 passengers (instead of the 122 quoted in the accident report) and 4 flight attendants in this scenario.
Scenario 4 contains the rear passenger cabin area where 50 occupants did not have sufficient time to make their way to the open L3 exit. The scenario contains 167 passengers and 3 flight attendants.

5. Effect of Later Requirements

It is assumed that later requirements would not have affected the situation in scenarios 1, 2, and 3 which already had sufficient time for successful evacuation.

For Scenario 4, it is assumed that later requirements would have provided an additional 30 seconds of evacuation time to those in the rear passenger cabin area who only had L3 as an available exit. The evacuation rate on this exit was such that an additional 10 occupants would have been able to evacuate given an additional 30 seconds.

The survivability chain for Scenario 4 therefore becomes:

6. Effect of a Fire-Hardened Fuselage

Scenario 1

It is concluded that a fire-hardened fuselage would not have affected the situation in Scenario 1 as no injuries were sustained.
Scenario 2

It is concluded that a fire-hardened fuselage would not have affected the situation in Scenario 2 as no injuries were sustained.

Scenario 3

It is concluded that a fire-hardened fuselage would not have affected the situation in Scenario 3 as no injuries were sustained.

Scenario 4

It is concluded that a fire-hardened fuselage would have delayed the entry of fire and provided the passengers with additional time to make their way to the open L3 exit. It is further assessed that smoke was entering through the tear above door L4 and therefore approximately 10 occupants would still have succumbed even with perfect fire hardening.

The high, median, and low prediction from the use of a fire-hardened fuselage is:

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For an aircraft configured to the standards appropriate to the latest requirements this assessment becomes:

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M & 94 & 42 & 34 \\
L & 110 & 40 & 20 \\
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M & 102 & 42 & 26 \\
L & 118 & 42 & 10 \\
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For 4 minutes protection:

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L & 146 & 14 & 10 \\
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For 8 minutes protection:

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\begin{array}{ccc}
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M & 115 & 37 & 18 \\
L & 146 & 14 & 10 \\
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Summary

The assessed median number of lives saved by a fire-hardened fuselage are:

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<td>4 minutes</td>
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<td>8 minutes</td>
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1. Description of Accident

RESUME
On 07-Dec-1983 an Iberia B727-200 registered as EC-CFJ was taking off from Madrid-Barajas Airport, Spain in fog conditions.

As the aircraft reached V1 speed, it collided with a DC-9 which had taxied onto the runway in error due to the poor visibility.

The aircraft spun round, caught fire and was destroyed.

Of the 9 crew and 84 passengers aboard, 1 crew member and 50 passengers suffered fatal injuries. 4 crew and 26 passengers suffered serious injuries. 4 crew and 8 passengers escaped with minor or no injuries.

IMPACT
The DC-9 aircraft was on the flight runway, because the visibility conditions due to fog in the zone through which the aircraft had taxied prevented the crew from obtaining sufficient visual references in order to determine that it was not the correct route which they should have been taking to reach the head of the runway.

At the moment of impact the B727 had just reached V1 and was travelling substantially on the runway axis.

The port side of the aircraft's fuselage on a level with the partitioning bulkhead between the crew and passenger cabins collided with the port wingtip of the DC-9 which had invaded the flight runway.

The effect of this initial impact was to instantly cause the other aircraft to swing round, leaving it practically parallel with, but in the opposite direction to, the B727, which lost its port wing and landing gear. The B727 continued along the runway, swinging round and moving over until it came to a halt at 460 m from the point of impact on the left hand edge of the runway, facing the opposite way to the direction of take-off.

FIRE
Fire broke out on the initial impact due to the breaking off of the port wing and the consequent leakage of fuel, which subsequently destroyed the whole aircraft.

The fire and the smoke generated affected the passengers of the B727 throughout the seconds which passed from when the collision occurred until the aircraft came to a complete stop and its evacuation.
EVACUATION
There were 9 crew and 84 passengers aboard.

The instantaneous fire which broke out on the port side of the aircraft and the plane's subsequent violent swing round incapacitated the victims who were unable to reach the exits.

AIRCRAFT FACTORS
The aircraft was a B727-200 registered as EC-CFJ and operated by the Iberia Company.

The cabin was fitted with a passenger entry door at the front port side, a service door on the starboard side, 2 overwing emergency exits above each wing and service door at either side at the rear. The cockpit had sliding windows on each side.

ENVIRONMENTAL CONDITIONS
The accident occurred during the hours of daylight but in fog.

INJURIES TO OCCUPANTS
Of the 9 crew and 84 passengers aboard, 1 crew member and 50 passengers suffered fatal injuries. 4 crew and 26 passengers suffered serious injuries. 4 crew and 8 passengers escaped with minor or no injuries.

5 people died instantly as a result of the impact of the DC-9's port wing with the B727's fuselage.

The traumatic injuries both of the survivors and of the fatalities were produced as a result of:

(a) The initial impact of the port wing tip of the DC-9 with the part of the B727's fuselage level with the bulkhead separating the crew compartment from the passenger cabin.

(b) The dynamic forces generated by the accelerations at the commencement and end of the aircraft's turn through 180 deg.

The effects of the smoke and fire which broke out on collision, plus the traumatic injuries, incapacitated a large number of victims, preventing them from evacuating the aircraft.

2. Fire Penetration Mechanism

Fire broke out on the initial impact due to the breaking off of the port wing and the consequent leakage of fuel. The fire was instantaneous and on the port side of the aircraft. The fire and the smoke generated affected the passengers throughout the seconds which passed from when the collision occurred until the aircraft came to a complete stop and its evacuation.

It is assessed that some fire and smoke could have entered the cabin through ruptures in the fuselage side caused by the impact. In addition, there was a pool fire around the aircraft as it came to a stop and it is assumed that this fire would have burnt through the fuselage during the evacuation.

The pool of ignited fuel subsequently destroyed the whole aircraft.
Based on the above it is assessed that the prime burnthrough route was through the fuselage skin. However there was insufficient information to be conclusive about detail burnthrough areas. It is additionally assessed that fire could have also entered the fuselage through ruptures.

3. **Location of Injuries and Scenarios**

Note: The accident report did not indicate which of the exits were used for evacuation.
4. Accident Scenarios and Survivability Chains

Due to insufficient documentary detail, this accident is considered as one scenario containing the whole aircraft.

Scenario 1 contains the whole aircraft.

5. Effect of Later Requirements

It is assumed that later requirements would not have affected the situation because the occupants who did not evacuate were incapacitated during the violent swing through 180 degrees and were unable to attempt an evacuation.

6. Effect of a Fire-Hardened Fuselage

It is assumed that a fire-hardened fuselage would not have affected the situation because the occupants who did not evacuate were incapacitated during the violent swing through 180 degrees and were unable to attempt an evacuation.
1. **Description of Accident**

**RESUME**
Before take-off the right engine failed with significant damage to the engine and the right wing between the fuselage and the engine pod. Fragments from a high pressure compressor disc perforated the right integral wing tank.

Fuel leaked onto the ground and ignited. Fire destroyed the aircraft.

There was no impact damage.

There were 118 occupants aboard, of which 2 suffered fatal injuries due to fire outside the cabin environment.

2. **Fire Penetration Mechanism**

There is very little data available on this particular accident, but it may be assumed that once the leaked fuel under the fuselage ignited, the fuselage would have burnt through along the lower surface.

Once the fuselage was penetrated, fire and smoke would have propagated up through the floor and then into the passenger cabin.

Based on the above it is assessed that the prime burnthrough route was through the fuselage skin. However there was insufficient information about other burnthrough areas.

3. **Location of Injuries and Scenarios**

There is insufficient data to identify the location of injuries except for the fact that the two fire fatalities occurred outside the cabin environment.

Therefore the accident is considered as two scenarios, one containing the two external fire fatalities and one containing the remainder of the occupants.

4. **Accident Scenarios and Survivability Chains**

This accident is considered as two separate scenarios.
Scenario 1 contains the two occupants fatally injured outside the cabin environment.

Scenario 2 contains the remainder of the occupants.

5. Effect of Later Requirements

Due to lack of data, no assumptions can be made about the effect of later requirements.

6. Effect of a Fire-Hardened Fuselage

As the only fire fatalities occurred outside the cabin environment, it is concluded that a fire-hardened fuselage would have had no benefit in this accident.
AIRCRAFT: B737  DATE OF ACCIDENT: 22nd August '85
REGISTRATION: G-BGJL  LOCATION OF ACCIDENT: MANCHESTER

1. Description of Accident

RESUME
At 0612 hrs G-BGJL, carrying 131 passengers and 6 crew on a charter flight to Corfu, began its take-off from runway 24 at Manchester with the co-pilot handling. About thirty six seconds later, as the airspeed passed 125 knots, the left engine suffered an uncontained failure, which punctured a wing fuel tank access panel. Fuel leaking from the wing ignited and burnt as a large plume of fire trailing directly behind the engine. The crew heard a 'thud', and believing that they had suffered a tyre-burst or bird-strike, abandoned the take-off immediately, intending to clear the runway to the right. They had no indication of fire until 9 seconds later, when the left engine fire warning occurred. After an exchange with Air Traffic Control, during which the fire was confirmed, the commander warned his crew of an evacuation from the right side of the aircraft, by making a broadcast over the cabin address system, and brought the aircraft to a halt in the entrance to link Delta.

As the aircraft turned off, a wind of 7 knots from 250° carried the fire onto and around the rear fuselage. After the aircraft stopped the hull was penetrated rapidly and smoke, possibly with some flame transients, entered the cabin through the aft right door which was opened shortly before the aircraft came to a halt. Subsequently fire developed within the cabin. Despite the prompt attendance of the airport fire service, the aircraft was destroyed and 55 persons on board lost their lives.

The cause of the accident was an uncontained failure of the left engine, initiated by a failure of the No 9 combustor can which had been the subject of a repair. A section of the combustor can, which was ejected forcibly from the engine, struck and fractured an underwing fuel tank access panel. The fire which resulted developed catastrophically, primarily because of adverse orientation of the parked aircraft relative to the wind, even though the wind was light.

IMPACT
There was no disruption to the passenger cabin as a result of impact in this accident.

FIRE
G-BGJL began its take-off from runway 24 at Manchester with the co-pilot handling. About thirty six seconds later, as the airspeed passed 125 knots, the left engine suffered an uncontained failure, which punctured a wing fuel tank access panel. Fuel leaking from the wing ignited and burnt as a large plume of fire trailing directly behind the engine. The crew heard a 'thud', and believing that they had suffered a tyre-burst or bird-strike, abandoned the take-off immediately.

After the aircraft stopped the hull was penetrated rapidly and smoke, possibly with some flame transients, entered the cabin through the aft right door which was opened shortly before the aircraft came to a halt. Subsequently fire developed within the cabin.
Fire was first spotted 10 seconds after the bang. The port engine and wing caught fire. Fuel leaking from the wing ignited and burnt as a large plume of fire trailing directly behind the engine. Upon turning, the fire spread under the fuselage because of wind and totally engulfed the aft fuselage. The aircraft was extensively damaged by fire. Most of the light alloy components in the aft region of the left engine nacelle were melted or burnt away. The left wing lower aft surfaces, large sections of the trailing edge flaps inboard of the engine and the lower surfaces of the flaps outboard of the engine were melted, and the remaining regions of the left inner wing and the main landing gear bay were superficially fire-damaged.

The rear fuselage was extensively burnt between the wing trailing edge and the rear doors; a large part of the left fuselage side between frames 787 and 887 (approximately seat rows 17 to 21) was completely burnt away. The whole of the fuselage aft of the rear cargo door and the tail section had collapsed onto the ground.

Analysis of the wreckage has shown that the fire initially penetrated the skins on the left side in the vicinity of seat rows 17 to 19, below the level of the cabin floor. Having breached the outer skin, the only barrier which prevented the fire gaining access to the cavity formed between the outer skin and the cargo bay side-liner panels, which communicated directly with the cabin interior above via floor level air-conditioning grills, was a 1 inch thick fibreglass wool acoustic insulation blanket contained in a thin plastic bag.

Most of the passenger cabin ceiling and crown skins were burnt and all of the overhead luggage bins were destroyed. The support beams which carried the cabin floor above the rear cargo hold were burnt away in the central aisle area and on the right side of the cabin, allowing most of the cabin floor above the hold to collapse down onto the baggage. Most of the cabin interior fittings and seats in this section of the cabin were destroyed completely or were very extensively damaged. The interior fittings in the centre and forward sections of the cabin were generally less severely affected by the fire. However, there was considerable local variability, particularly in the severity of seat damage. Notably, seats 8C and 9C (left aisle seats just forward of the overwing exits) were completely destroyed, whereas the adjoining seats were relatively intact.

The survivors statement indicated that the smoke suddenly emanated from the rear of the cabin just before the aircraft stopped. Many passengers reported holding their breath as smoke hit them and that one breath of the thick black smoke caused breathing problems and lung pain. A passenger from seat 6A saw a sheet of flame inside the cabin. It seemed to be near the centre of the aircraft and separated the front half from the back. Another passenger from 6B, after seeing foam being sprayed over the fire on the left side of the aircraft, tried to move into the aisle but it was jammed with people and it was difficult to move. On turning he saw flames shooting in through the side windows and up through the floor area. The flames were several feet in length and continual.

The fire station crash alarm was initiated by ATC immediately the fire was observed from the tower. However, many fire crew personnel heard the bang, saw the fire and started to respond before the alarm had sounded.
RIV2 arrived at the scene approximately 25 seconds after the aircraft had stopped. It was positioned on the left side of the aircraft and foam was applied initially onto the left side of the fuselage and then onto the left engine. RIV1 arrived shortly after RIV2, positioned off the nose slightly on the left side, and discharged the whole of its foam along the left side of the fuselage with the intention of protecting passengers, who by then were evacuating from the L1 chute, and cooling the left side of the fuselage.

Approximately 7 minutes into the incident, after it became clear that no more passengers were likely to emerge unaided, a team with breathing apparatus made an entry via the R1 door. Conditions inside the cabin at that time were very bad, with thick smoke and a serious fire in progress at the rear of the cabin. Shortly after entering, an explosion occurred which blew one of the firemen out of the door onto the tarmac. The officer in charge was by that time becoming increasingly concerned about the reducing water supplies, especially with regard to the potential loss of water supplying sidelines deployed within the cabin, and directed that there would be no more attempts to gain entry until there was a reliable supply of water. In the interim, sidelines were used on the exterior only. At about this time a fire was seen to flash briefly along the cabin.

By approximately 11 minutes into the incident, the internal fire appeared to have spread forward throughout the cabin, where breaches in the roof could be seen. J1 was dispatched to replenish with water from the hydrant system: the vehicle was positioned at three hydrants in succession, but no water could be obtained from any of them. This resulted in a delay of about 10 minutes, after which J1 returned to the scene empty. It was then dispatched to the hydrant behind the fire station, where replenishment was successful.

The GMC fire appliances arrived at the aircraft approximately 13 minutes into the incident. Initially, the Station Officer (SO) in charge experienced some difficulty in identifying the officer commanding the airport fire service, resulting in some delay before the water requirements were identified and the transfer of the 1600 gallons of water from the GMC appliances to J2 could begin. Using a sideline from the newly replenished J2 tender, a two man team with breathing apparatus was then able to make an entry via the R1 door using a short ladder, and, for the first time, were in a position to begin addressing the internal fire.

**EVACUATION**

During the take-off run the commander made the routine call of "eighty knots" which was confirmed by the co-pilot, and 12 seconds later a 'thump' or 'thud' was heard. Believing that they had suffered a tyre-burst or bird-strike, the crew abandoned the take-off immediately, intending to clear the runway. They had no indication of fire until 9 seconds later, when the left engine fire warning occurred. After an exchange with Air Traffic Control, during which the fire was confirmed, the commander warned his crew of an evacuation from the right side of the aircraft, by making a broadcast over the cabin address system; "Evacuate on the starboard side please." As the aircraft's groundspeed reduced through 17 Kt., 10 seconds before it stopped, the purser opened the flight deck door and said, "Say again", seeking confirmation of the evacuation order. The commander repeated, "Evacuate on the starboard side", 8 seconds before the aircraft came to a halt.
The passenger evacuation drill, a non-memory drill was called for by the commander and was read from the Quick Reference Handbook by the co-pilot. Before they were able to complete the drill the commander saw fuel and fire spreading forward on the left side of the aircraft, opened the co-pilot's sliding window on the right side of the flight deck and ordered him to evacuate the aircraft. This the co-pilot did by means of a fabric escape strap secured above the sliding window and he was followed down to the ground by the commander.

The purser and stewardess seated in the left of the forward galley area during the take-off run heard a 'thud' which they too thought was a tyre burst. They were aware that the take-off had been abandoned. There were sounds of distress in the cabin and the purser leaned inboard in an attempt to improve his view and saw passengers standing up. He made a Public Address announcement for passengers "to sit down and to remain strapped in", released his harness and went into the forward part of the cabin. He saw fire outside the aircraft on the left side coming up over the leading edge of the wing and flowing back over the wing's top surface. There was no smoke or fire apparent to him in the cabin at that time.

Passengers in rows 1-3 appear to have been initially oblivious of the fire which issued from the engine after the 'thud'. However, most of those seated aft of row 5, and in particular those aft of row 14 on the left side, were immediately aware of an intense fire. The flames were seen to cause some 'cracking and melting' of the windows, with some associated smoke in the aft cabin before the aircraft stopped. These effects, with the accompanying radiant heat, caused some passengers to stand up in alarm. A male passenger shouted "sit down, stay calm". Similar calls were then made by others seated mainly on the right side of the aircraft. Many sat down, but some found the pressure to move into the aisle irresistible.

After the purser had confirmed the evacuation with the commander he repeated the evacuation call a number of times over the PA system. Then, as the aircraft was coming to a halt, he went to the right front (R1) door to open it and release the inflatable escape slide. The door unlocked normally but as it was moving out through the aperture the slide container lid jammed on the doorframe preventing further movement of the door. After spending a short time trying to clear the restriction he postponed further effort and crossed to the L1 door. He cracked it open, ascertained that the forward spread of the fire was slow enough to allow evacuation from that door, opened it fully and confirmed the inflation of the slide manually. This was achieved about 25 seconds after the aircraft had stopped and coincident with the initiation of foam discharge from the first fire vehicle to arrive. Evacuation began on the left side under the supervision of the No 4 stewardess, who had to pull free some passengers who had become jammed together between the forward galley bulkheads in order to start the flow.

The purser returned to the R1 door, lifted the slide pack in order to close the slide container lid, and cleared the obstruction. He succeeded in opening the door about 1 minute 10 seconds after the aircraft stopped and again confirmed the automatic inflation of the slide by pulling the manual inflation handle. Evacuation was carried out from this exit supervised by the purser. Smoke emanating from the cabin quickly reached the galley area and became rapidly more dense and acrid. When the smoke began to threaten severe incapacitation, the forward cabin crew vacated the aircraft by the slides at their respective doors.
As the aircraft came to a halt and at the instigation of other passengers, a young woman sitting in seat 10F, beside the right overwing exit, attempted to open it by pulling on her right hand armrest which was mounted on the exit hatch. Her companion in seat 10E, the centre seat of a row of three, stood up and reached across to pull the handle located at the top of the hatch marked "Emergency Pull". The hatch, weighing 48 lb., fell into the aircraft to lay across the passenger in 10F, trapping her in her seat. With the assistance of a man in row 11 behind the women, the hatch was removed and placed on vacant seat 11D. The passengers in 10F and 10E then left the aircraft cabin through the overwing exit onto the wing followed by other survivors. This exit was open about 45 seconds after the aircraft stopped.

During the latter stages of the abandoned take-off, and just as the aircraft turned towards taxiway link Delta, the right rear door was seen by external witnesses to be open, with the slide deployed and inflated. A stewardess was initially visible in the doorway but the door and slide were obscured by thick black smoke as the aircraft stopped. No one escaped through this door. Two passengers remember seeing one of the two stewardesses from the rear of the aircraft struggling to direct passengers in the rear aisle. Neither rear stewardess survived.

As the aircraft stopped, the aft cabin was suddenly filled with thick black smoke which induced panic amongst passengers in that area, with a consequent rapid forward movement down the aisle. Many passengers stumbled and collapsed in the aisle, forcing others to go over the seatbacks towards the centre cabin area, which was clear up until the time the right overwing exit was opened. A passenger from the front row of seats looked back as he waited to exit the aircraft, and was aware of a mass of people tangled together and struggling in the centre section, apparently incapable of moving forward, he stated "people were howling and screaming".

The smoke quickly moved forward and passengers from seats 6A and 6B, waiting to get into the aisle, said they couldn't see and it became very hot as they entered the aisle to get to the forward exits. Passengers behind them either climbed over seats to get forward, or felt their way down the aisle by way of the seat. People seated in seats 7A and B could not see the exit when they reached it, but had to feel the walls to find a gap. A female passenger from seat 15A who climbed over seats to move towards the front exit was pushed past the exit by a mass of bodies. People all around her were collapsing on the floor. She fell to the ground unconscious just outside the doorway. She revived and pulled herself out.

At least 15 passengers reported going over seats or climbing over other collapsing passengers in the aisles. Of these 15 passenger survivors, at least 11 used the starboard overwing exit to escape, which represents some 44 percent of those who used the congested aisle. Many passengers who ultimately got out the starboard overwing exit, collapsed temporarily within or adjacent to this exit due to incapacitation; e.g. passengers from seats 12D, 14F, 15A and 8B.

With regard to the exits used during the evacuation, some 15-17 used the left forward exit, 34-36 used the right forward exit. Some 27 passengers (including two infants) used the right overwing exit. Survivors indicated hesitation by some to exit the left front on the side of the fire, which slowed evacuation out of the doors.
The first people were out the left front door at 30 seconds from stopping and, out the right front door at 90 seconds from stopping. The overwing exit was opened 45 seconds from stopping. Passengers stopped using the left front door when the right front door was opened due to the presence of fire on the left side. In 3.5 to 4 minutes after stopping, all survivors (except one 14 year old boy from seat 12D found by the fireman 5.5 minutes after their arrival) were off the aircraft. From the statements of the survivors, it is evident that the effects of the fire on the left side of the aircraft rapidly instilled fear and alarm in many passengers, particularly those in the aft/left cabin - i.e. row 14 aft. These effects appear to have been marked heat radiation through the windows together with "cracking, melting and smoking" of the window transparency panels, which motivated some passengers from the aft cabin to enter the aisle and move forward before the purser's 'sit down' announcement on the PA, and therefore before the evacuation call 14 seconds prior to the aircraft stopping.

The left rear door was opened by firemen some time after the fire had been extinguished.

The air and ground movements controllers in the tower had seen the fire and smoke trailing behind the aircraft and had initiated 'full emergency' action. The air controller activated the alarm siren connected directly to the aerodrome fire service station (Manchester International Airport Fire Service - MIAFS), and gave brief details of the emergency to the MIAFS watchroom over the direct telephone link.

Members of the MIAFS heard a bang and saw an aircraft decelerating on runway 24. Black smoke and flames were trailing from the left side of the aircraft and the firemen had already initiated their response when the crash alarm siren sounded.

Two Rapid Intervention Vehicles (RIVs) attended first, one arriving at the aircraft coincident with, the other just after the L1 door had opened and its slide deployed, as passengers were about to start to evacuate. About 30 to 40 seconds later, as two major foam tenders took up position, the R1 door was opened fully and its slide deployed.

Many survivors from the front six rows of seats described a roll of thick black smoke clinging to the ceiling and moving rapidly forwards along the cabin. On reaching the forward bulkheads it curled down, began moving aft, lowering and filling the cabin. Some of these passengers became engulfed in the smoke despite their close proximity to the forward exits. All described a single breath as burning and painful, immediately causing choking. Some used clothing or hands over their mouths in an attempt to filter the smoke; others attempted to hold their breath. They experienced drowsiness and disorientation, and were forced to feel their way along the seat rows towards the exits, whilst being jostled and pushed. Many, even in the forward cabin, resorted to going over the seat backs in order to avoid the congested aisle.

At the start of evacuation from the L1 door, the stewardess stated that passengers seemed to be jammed in the cabin aisle and entrance to the galley (i.e. between the twin forward bulkheads). She cleared the jam by pulling one young passenger forwards and the flow then started. Later she saw a young girl lying on the floor of the forward aisle. She pushed another youth back, pulled the girl forward by her collar and pushed her down the slide. As the passengers came forward through the bulkhead aperture so the smoke built up in the forward galley area. She
recalled feeling a body slump against her legs, bent down and, due to improved visibility near the floor, saw that it was another girl passenger. Her face was black with soot, eyes fixed and dilated with no signs of breathing. The stewardess considered giving her the kiss of life when a fireman down below shouted for her to throw the girl down to him. With great difficulty she lifted her by the waist and threw her onto the chute. After being forced down by the smoke onto her hands and knees, the stewardess felt around for other passengers back as far as the galley cabin entrance. She was considering getting her smokehood when a fireman shouted at her to jump, concerned that she would perish if she delayed. Having been unable to locate any further passengers, she went down the slide.

The Purser stated that, after getting the R1 door open at his second attempt and initiating evacuation from this exit, the smoke began entering the galley area.

Very rapidly the area around the overwing exit became a mass of bodies pushing forward to the exit. People all around were falling and collapsing to the floor. Many passengers who ultimately got out of the right overwing exit, nevertheless collapsed temporarily within, or adjacent to it. The exit was blocked with "people's bodies lying half-in and half-out of the aircraft". A male passenger, from 16C, died after becoming lodged in this right overwing exit. A young boy, from 12D, was pulled out over this man's body by a fireman about 5 minutes after the aircraft stopped. Several of the survivors who used the overwing exit were impeded by becoming entangled in the ditching strap. However, one passenger recalled catching hold of it as she collapsed, to recover consciousness with her head outside the exit.

Of the 24 passengers who escaped from the right overwing (not including the 2 young children and the young boy pulled clear) some 11 passengers (46%) went over the seats as opposed to using the congested aisle to get there.

AIRCRAFT
The aircraft was fitted with 130 passenger seats, two double and one single cabin crew seats. One of the double crew seats was forward of door L1 facing rearwards and the other double aft of door L3 facing forwards. In the forward passenger cabin a pair of full height galley bulkheads were positioned just aft of the two doors, L1 and R1. In the aft end of the cabin a full height stowage unit was located just forward of door R3 with a single crew seat mounted on the rear of it, facing aft.

This configuration was in compliance with British Airways Configuration Modification No 25C211, Drawing No 1-54378 certified by the British Airways authorised engineer as being in compliance with the appropriate regulations on the 20 November 1981.

This drawing specifies a seating pitch of:

<table>
<thead>
<tr>
<th>Rows</th>
<th>Pitch</th>
</tr>
</thead>
<tbody>
<tr>
<td>1-9</td>
<td>30 ins</td>
</tr>
<tr>
<td>9-10</td>
<td>31 ins</td>
</tr>
<tr>
<td>10-22</td>
<td>29 ins</td>
</tr>
</tbody>
</table>
In addition, this drawing specified that seats 10A and 10F, should be of a type modified to prevent the seat-backs from hinging forward and row 9 seats should have no recline, in order that access to both overwing exits should not be impeded. The seat backs of row 9, in common with the majority of seats, could be folded forwards to create more room for the upper body of any person moving between rows 9 and 10 to the overwing exits.

The aisle aperture between the twin forward bulkheads in this configuration was 22 inches wide.

The aircraft was equipped with four main cabin doors ('Type I'), two overwing emergency exits ('Type III') and two sliding-window emergency exits on the Flight Deck. Each main door incorporated a slide pack.

The overwing emergency exits were located at either side of row 10 and were intended for ground evacuation of centre cabin passengers, or as the primary exits for use after a sea-ditching. For the latter purpose, these exits were each equipped with a webbing-type escape rope/lifeline, anchored to the upper/forward corner of the aperture, with a snap-hook on the other end for attachment to a lug located on the upper surface of each wing near the trailing edge. These lifelines were some 17 feet in length and designed to provide evacuees with a means of stabilising themselves while on the wing upper surface prior to boarding the rafts. For ground evacuation, arrows painted on the upper surface of each wing were intended to lead evacuees to the trailing edge and down the extended flaps.

On pulling the overwing exit hatch release handle the hatch, weighing 48 lb., pivots inboard about its lower edge and requires lifting to remove it from the aperture to make the exit available.

The Flight Deck had two sliding-window emergency exits for use by the pilots, with two associated webbing-type escape ropes stored in the overhead above the windows.

The cabin crew stations at the forward and aft passenger doors (i.e. left) were each equipped with an interphone and passenger address microphone. The forward cabin crew were also provided with two 'Scott' smokehoods, located in a cupboard stowage facing their bench-seat. One 1.5 Kg capacity Bromochlorodifluoromethane (BCF) fire extinguisher bottle (discharge duration 15 seconds) was also located in a stowage locker facing this seat. The other three smokehoods, for use by the cabin crew, were stored in the overhead 'bin' at row 18 (right). One 1.5 lb. capacity water fire extinguisher was stored in this area of the cabin within the right overhead at row 20. A further two, 1.5 Kg BCF extinguishers were located on the aft wall of the rear right bulkhead. Two megaphones were available for cabin crew use, one stored in the forward left overhead bin at row 2 and the other in the aft right overhead at row 18.

Ten portable oxygen bottles were stored in the cabin overheads; two (for crew use) were located at row 2 right, two units either side of the aisle at row 10 (for passengers) and four units within the overhead at rows 20-21 right, of which three were designated for crew use.
ENVIRONMENTAL CONDITIONS
The accident happened during daylight. The weather recorded at Manchester Airport at 0550 hrs was:

Surface Wind: 270°/5 Kt Visibility: 25 km Cloud :1 okta at 1,400 feet Temperature: + 13 C  
QNH *:1014 millibars*(Corrected mean sea level pressure setting)

The weather recorded at 0620 hrs was:

Surface Wind: 260°/6 Kt Visibility :1,000 metres in smoke Cloud: 1 okta at 1,400 feet  
Temperature: + 13 C QNH: 1015 millibars

The Manchester Automatic Terminal Information Service (ATIS), information 'C' was received by the crew prior to starting engines. This gave the surface wind as 280°/6 Kt., variable 240°-320°. When ATC cleared the aircraft for take-off, they passed a surface wind of 250° at 7 Kt. The runway was dry.

INJURIES TO OCCUPANTS
The major cause of the fatalities was rapid incapacitation due to the inhalation of the dense toxic/irritant smoke atmosphere within the cabin.

A marked deposition of carbon particles was found within the trachea of all victims, with some congestion of the mucosa (mucus lining) in 17 cases ("marked congestion" in the case of one passenger) with many instances of "excess mucus". The lungs of all fatalities showed marked general congestion and oedema (fluid), with carbon particles in the air passages, consistent with the inhalation of smoke. There was no evidence of organic disease which could have caused the death of any of the victims.

Blood samples were analysed to determine carboxyhaemoglobin and cyanide levels. In addition, hydrocarbon absorption was measured, including benzene and toluene, these two being the most prevalent volatiles found in all fatalities. Many other minor trace volatiles were found, including acetaldehyde.

Of the 54 occupants who expired on the aircraft, 43 (80%) had cyanide levels in excess of 135 micrograms/100 ml which would have led to incapacitation. Of these, 21 had levels above 270 micrograms/100 ml, the fatal threshold. Forty passengers (74%) had levels of carboxyhaemoglobin in excess of 30% saturation which would also be expected to cause incapacitation. Of these, 13 passengers had levels in excess of 50%, which is generally accepted as the fatal threshold. From information in the accident report and from the AAIB, it was determined that 9 passengers had absorbed less than the incapacitating levels of carbon monoxide and hydrogen cyanide stated above, and died from direct thermal assault. The remaining 48 passengers who died on board did so as a result of smoke/toxic gas inhalation.

One eventual fatality, a 31 year old man from seat 8B had superficial burns over 24 percent of his body, was taken off the aircraft alive 33 minutes after the aircraft stopped. He died after 6 days in hospital because of severe pulmonary (lung) damage and associated pneumonia.
2. **Fire Penetration Mechanism**

As the aircraft came to a stop, the spilling, ignited fuel collected in a pool under the wing on the left side, directly behind the port engine. The wind direction was such that flames were directed onto the fuselage side and as a result the rear fuselage was extensively burnt. A large part of the left fuselage side approximately between seat rows 17 and 21 was burnt through and allowed fire and smoke to enter the passenger cabin. One passenger reportedly saw flames shooting in through the side windows and up through the floor area.

Analysis of the wreckage has shown that the fire initially penetrated the skins on the left side below the level of the cabin floor. Having breached the outer skin, the only barrier to the fire gaining access to the cavity, formed between the outer skin and the cargo bay side liner panels, was a 1-inch-thick fibreglass wool acoustic insulation blanket contained in a thin plastic bag. This cavity communicated directly with the cabin interior above via floor level air conditioning grills.

The aft right door was opened shortly before the aircraft came to a halt and this allowed some flame transients to enter the cabin. No occupants were able to use this exit as a means to escape.

As fire and smoke entered the cabin at the rear, it rapidly propagated forward. Many survivors described a roll of thick, black smoke clinging to the ceiling and moving rapidly forward along the cabin. On reaching the forward bulkheads it curled down, began moving aft, lowering and filling the cabin. Some passengers became engulfed in the smoke despite their close proximity to the forward exits and were forced to feel their way out. Others succumbed to the smoke and died of asphyxiation.

Based on the above, it is assessed that the prime burnthrough route was through the fuselage skin and windows on the left side.
3. Location of Injuries and Scenarios

Scenario 1

Scenario 2

Scenario 3

Scenario 4

Scenario 5

Scenario 6
4. Accident Scenarios and Survivability Chains

This accident is divided into six separate scenarios.
**Scenario 1** contains the flight deck area. It was the area furthest from the point of entry of the fire and therefore had the most time available for evacuation. The scenario contains the two flight crew who exited through the right cockpit window.

**Scenario 2** contains the forward flight attendant area. The flight attendants were initially seated rearward facing and stayed close to their respective exits throughout the whole evacuation period as they directed passengers out. Indeed, they each finally evacuated through their assigned exits when they considered it unsafe to remain. As a result they both survived uninjured.

**Scenario 3** contains the front section of the passenger cabin from seat row 1 to seat row 5 inclusive and the left side of row 6. This area had both the L1 and R1 exits open and useable. The occupants executed a successful evacuation uninjured. It is assumed that the unknown injuries were minor.
**Scenario 4** contains the front section of the passenger cabin from seat row 7 to seat row 8 inclusive and the right side of row 6. Occupants in this area made their way to the L1 and R1 exits. Those further from the exits had a longer exposure to the fire and smoke and were either injured or asphyxiated. It is assumed that the unknown injuries were minor.

![Diagram of Scenario 4](image)

**Scenario 5** contains the midsection of the passenger cabin from seat row 9 to seat row 15 inclusive. This area had only the R2 overwing exit open and usable. Those occupants closest to the exits executed a successful evacuation. Those further away had a longer exposure to the fire smoke and were either injured by fire or overcome by smoke and toxic gas. It is assumed that the unknown injuries were minor. The numbers include two infants who were carried out of the aircraft uninjured.

![Diagram of Scenario 5](image)

**Scenario 6** contains the rear section of the passenger cabin from seat row 15 to seat row 22 inclusive as well as the two rear flight attendants. This area had only the R2 overwing exit open and usable and was the area in which the fire burnt through and entered the cabin. All occupants from this section had a significant distance to travel before reaching the overwing exit. Further, visibility was severely restricted by dense black smoke and location of exits was almost impossible. As a result, most of the occupants either died as a result of burns or were overcome by the smoke and toxic gas. It is assumed that the three unknown injuries next to the point of fire entry were serious and all other unknown injuries were minor. The numbers include an infant who was asphyxiated in the cabin.
5. **Effect of Later Requirements**

It is assumed that later requirements would not have affected the situation in scenarios 1, 2, and 3 which already had sufficient time for successful evacuation.

For scenarios 4, 5, and 6 it is assessed that later requirements would have improved flammability standards such that the occupants would have had an additional 30 seconds of time to escape. It is assessed that this would result in the survivability chains for Scenarios 4, 5, and 6 becoming:
6. Effect of a Fire-Hardened Fuselage

Scenario 1

It is concluded that a fire-hardened fuselage would not have affected the situation in Scenario 1 as there was already sufficient time for a successful evacuation.

Scenario 2

It is concluded that a fire-hardened fuselage would not have affected the situation in Scenario 2 as there was already sufficient time for the flight attendants to direct passengers and then evacuate themselves successfully.

Scenario 3

It is concluded that a fire-hardened fuselage would not have affected the situation in Scenario 3 as there was already sufficient time for a successful evacuation.

Scenario 4

It is concluded that a fire-hardened fuselage would have delayed the entry of fire and provided the passengers with additional time to locate and use the two forward exits.

The high, median, and low prediction from the use of a fire-hardened fuselage is:

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For an aircraft configured to the standards appropriate to the latest requirements this assessment becomes:

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Scenario 5

It is concluded that a fire-hardened fuselage would have delayed the entry of fire and provided the occupants with additional time to use the available overwing exit.

The high, median, and low prediction from the use of a fire-hardened fuselage is:

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For an aircraft configured to the standards appropriate to the latest requirements this assessment becomes:
Scenario 6

It is concluded that a fire-hardened fuselage would have delayed the entry of fire and provided the occupants with additional time to move forward and use the available overwing exit.

The high, median, and low prediction from the use of a fire-hardened fuselage is:

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For an aircraft configured to the standards appropriate to the latest requirements this assessment becomes:

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**Summary**

The assessed median number of lives saved by a fire-hardened fuselage are:

<table>
<thead>
<tr>
<th>Burnthrough Protection Time</th>
<th>Aircraft in its Actual Configuration</th>
<th>Aircraft Configured to Later Requirements</th>
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</thead>
<tbody>
<tr>
<td>30 seconds</td>
<td>12</td>
<td>17</td>
</tr>
<tr>
<td>2 minutes</td>
<td>37</td>
<td>30</td>
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<tr>
<td>4 minutes</td>
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<tr>
<td>8 minutes</td>
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1. Description of Accident

RESUME
During an airshow on 26-Jun-88, A320 F-GKFC made a level pass at about 30 ft, gear and flaps out, engines almost at idle and 130 passengers on board. After the go-around, the aircraft hit trees at the end of the runway. The right wing broke up and a fire erupted.

All passengers evacuated the aircraft except 3 who suffered fatal injuries as a result of the fire.

IMPACT
The aircraft touched the trees shortly after the end of the runway at 12:45.40; at this time, engine speed was around 83% N1, the pitch attitude of the aircraft was 14 deg.

The first contact between the aircraft and the trees was made by the rear section of the fuselage, the tailplane, then the engine nacelles and the main landing gear.

The aircraft sank slowly into the forest. The tip of the right wing then the right wing itself broke, finally the aircraft came to rest.

FIRE
As the right wing broke fuel was projected forward and fire immediately broke out and penetrated the cabin as soon as the aircraft came to rest. Fuel flowed under the cabin and as a result fire also raged around the left wing.

EVACUATION
There were 2 flight crew, 4 cabin crew and 130 passengers on board.

As soon as the aircraft came to rest, the stewards stationed at the forward and aft doors, seeing the fire on the right of the aircraft, opened the left doors.

The normal lighting went off and the emergency lighting did not come on due to a design error in its automatic illumination program.

The captain declared that he made several attempts to trigger the evacuation signal; but it seems that this signal did not work, as nobody in the cabin heard it.

The purser wished to broadcast a message via the public address but could not find the handset, which apparently, had been torn from its support, maybe due to the fragility of this support and of the attachment of the electrical lead.

A passenger stated that he had wanted to open the left wing emergency exit but could not reach it. The opening of this exit would have been very dangerous, as fire was raging around the left wing level.
The left forward door was blocked by branches after it had started to open correctly. The escape slide started to deploy partly outside and partly inside the cabin.

The purser, with the help of at least one passenger and an air hostess from another airline (who was in the cockpit as a passenger during the flight), pushed the door. The door opened suddenly and the purser and the passenger fell out of the aircraft and were covered by the escape slide.

The passengers started to panic and push in the front of the cabin. The passenger hostess then started to evacuate the passengers, but the first ones were blocked by branches in the escape slide. The other passengers jumped out beside the escape slide but they very quickly piled up on top of each other again due to the branches.

The hostess then stopped evacuation for a short while so that the passengers who had already jumped had time to extricate themselves. Meanwhile, on the outside, the purser, with the help of another air hostess, tore away the branches from across the escape slide to free the blocked passengers. After a certain time, the hostess herself left the aircraft suffering from the effects of the smoke.

The hostess stationed in the centre of the aircraft at 12D was pushed into the aisle by the passenger in 12F, who was seriously burnt, was then carried forward while at the same time helping a passenger whose clothes were on fire.

She arrived at the left forward door (L1), no doubt after the evacuation of the previous hostess, and took her place.

After the evacuation of the last persons to arrive at this door, she called into the cabin to find out if anyone was still there but did not get any reply; by this time, the smoke, very thick, and flames made any visual inspection impossible. On the order of the captain, who had just evacuated the injured first officer, she then left the aircraft.

The captain then returned to the cockpit to get an antismoke mask with the intention of going through the cabin, but in turn had to leave, before he could don the equipment, suffering from the effects of the fumes.

Note that the cabin attendants either were not able to use the antismoke masks during the evacuation or did not think of doing so. They were, in fact, more occupied in opening the doors as quickly as possible so as to speed up passenger evacuation.

The left aft door was opened without any problems and the escape slide was correctly deployed. The steward ordered the passengers to come toward him to evacuate and at the same time reassured them verbally. This, combined with the absence of smoke at the rear of the cabin, certainly contributed toward the good evacuation via this door.

After several passengers had used the escape slide it was punctured on the branches. The steward asked his colleague to descend to help to receive the passengers. Evacuation continued without panic with spontaneous help from one passenger, especially for the evacuation of an elderly person who had difficulty in moving.
After the last passengers had left the aircraft, the hostess returned to go through the cabin but the flames and smoke had reached the galley; she was able only to shout out in the direction of the cabin.

During the evacuation, the stewards and hostesses could not see what was happening at the other end of the aircraft due to the fire around the left wing. Therefore, after the end of evacuation via their door, they went round the wing toward the front or the rear to see if they could be of any help at the other door.

It has not been possible to determine the time it took to evacuate the survivors.

All passengers were able to leave the aircraft, except three who succumbed to the fire.

**AIRCRAFT FACTORS**
The aircraft was an A320-100, registered as F-GFKC and operated by Air Charter International.

The aircraft obtained its Certificate of Airworthiness on 22-Jun-1988 and was delivered on 24-Jun-1988.

The aircraft was fitted with two doors at the front and two doors at the rear of the passenger cabin. In addition there were 2 overwing emergency exits over each wing.

**ENVIRONMENTAL CONDITIONS**
The accident happened in daylight during an airshow. Visibility was 8km and wind was 010 deg at 6 kts.

**INJURIES TO OCCUPANTS**
All passengers were able to leave the aircraft, except three who succumbed to the fire:

A young handicapped boy in seat 4F who seems to have remained in his seat.

A little girl located at 5C who, according to her young brother (carried away by the flow of the other passengers), had not been able to open her seat belt and was blocked by the back of her seat, which was tilted over onto her.

A woman travelling in seat 10B who, according to her husband, had reached the left forward door; as her body was found near that of the little girl, we can reasonably assume that she went back into the cabin to help the little girl and was overcome by the fumes.

2. **Fire Penetration Mechanism**

As the right wing broke, fuel was projected forward and fire immediately broke out and penetrated the cabin as soon as the aircraft came to rest. Burnthrough is therefore assumed to be initially from the right side.
Fuel flowed under the cabin and as a result fire also raged around the left wing. It can be assumed that the pool fire under the cabin centre section and left wing root would have added to the initial burnthrough on the right side.

The fuselage was found completely burnt out and this is taken to mean that the fuselage skin had been consumed by the fire mainly after the evacuation had taken place.

Based on the above it is assessed that the prime burnthrough route was through the fuselage skin on the right side. However there was insufficient information to be conclusive about detailed burnthrough areas.

3. Location of Injuries and Scenarios
4. Accident Scenarios and Survivability Chains

This accident is divided into four separate scenarios:

**Scenario 1** contains the flight deck area. It was the area furthest from the point of entry of the fire and the only injury was to the first officer during the impact. The scenario contains the two flight crew and an observer air hostess.

**Scenario 2** contains the forward flight attendant area. The flight attendants were seated rearward facing and they both survived uninjured.

**Scenario 3** contains the main passenger cabin in which there were 129 passengers and a flight attendant in 12D.
Scenario 4 contains the rear flight attendant area where there was one flight attendant who escaped uninjured.

5. Effect of Later Requirements

It is assumed that seat blocking would have been fitted on an aircraft of this age. Further there was no mention of passenger disorientation so no benefit was assumed for floor proximity lighting. It was, however, assumed that later requirements on material heat release would have saved some serious injuries in Scenario 3.

It is therefore assessed that the survivability chain for Scenario 3 becomes:

6. Effect of a Fire-Hardened Fuselage

Scenario 1

It is concluded that a fire-hardened fuselage would not have affected the situation in Scenario 1 as injuries were related to the impact.
**Scenario 2**

It is concluded that a fire-hardened fuselage would not have affected the situation in Scenario 2 as there were no injuries.

**Scenario 3**

It is concluded that a fire-hardened fuselage would have delayed the entry of fire and provided occupants with additional time to go back and assist some of the remaining passengers to safety.

The high, median, and low prediction from the use of a fire-hardened fuselage is:

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Scenario 4

It is concluded that a fire-hardened fuselage would not have affected the situation in Scenario 4 as there were no injuries.

Summary

The assessed median number of lives saved by a fire-hardened fuselage are:

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<tr>
<th>Burnthrough Protection Time</th>
<th>Aircraft in its Actual Configuration</th>
<th>Aircraft Configured to Later Requirements</th>
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1. Description of Accident

RESUME
On 31-Aug-88, B727-232 registered as N473DA was taking off from Dallas-Fort Worth International Airport, Texas. The slats and flaps were not properly configured and as a result the aircraft did not gain altitude after rotation.

The aeroplane struck an instrument landing system (ILS) localiser antenna array approximately 1000 feet beyond the end of the runway and came to rest about 3200 feet beyond the departure end of the runway. The flight was airborne approximately 22 seconds from lift-off to the first ground impact near the ILS localiser antenna. The aeroplane was destroyed by impact forces and the postcrash fire.

The combination of jammed left aft service door and the intense impenetrable fire trapped 12 occupants.

Of the 108 occupants aboard, 12 passengers and 2 crew members were killed by fire, 21 passengers and 5 crew members were seriously injured and 68 passengers sustained minor or no injuries.

IMPACT
The aeroplane struck the instrument landing system (ILS) localiser antenna array approximately 1000 feet beyond the end of runway 18L, and came to rest about 3200 feet beyond the departure end of the runway. The flight was airborne approximately 22 seconds from lift-off to the first ground impact near the ILS localiser antenna.

The fuselage had separated into three major sections: (1) the forward section consisted of the nose forward of fuselage station (FS)-420 [just aft of seat row 1]; (2) the centre fuselage section included the body structure between FS-420 and FS-950c [between seat rows 27 & 28]; and (3) the aft fuselage section extended from FS-950c aft to the end of the No. 2 engine tailpipe.

The forward fuselage section had rotated to the left about 45 deg. The entire lower fuselage structure sustained various degrees of tearing, buckling and general overall distortion. Some sooting was noted in the area between the first officer's side window and the separation.

The centre fuselage section came to rest right side up and was supported by the left wing and right wing centre section.
**FIRE**
The external fire was initiated when the right wing and tail struck the localiser antenna array. The fire intensified when the aircraft struck the lip of a depression in the terrain. Flames not only impinged on the right side of the fuselage but extended around the fuselage, heating the inboard wing area on the left side. The forward door of the aft cargo compartment was torn off at its hinges, causing a large opening in that area through which fire entered the cargo area before the aircraft came to rest. The fire burned through the floor.

Fire also entered the cabin through the aft break in the fuselage and later through a burnthrough in the centre wing box area.

The left side forward of FS-720e [between seat rows 17 & 18] showed no evidence of fire damage and all passenger windows were intact. However, the left side between FS-720e and FS-950c had sustained varying degrees of fire damage. In the general area of FS-950c, some fuselage structure had been consumed by fire.

The right side of the centre fuselage section between FS-420 and FS-950c also contained fire damage with some fuselage areas totally consumed by fire. All passenger windows on the right side were missing or melted.

The fuselage crown between FS-450 [between seat rows 2 & 3] and FS-680 [between seat rows 13 & 14] from the top of the passenger window line on the right side to the top of the Delta logo on the left side was consumed by fire. Another area of the top fuselage, between FS-720c [between seat rows 16 & 17] and FS-840 [between seat rows 21 & 22], was consumed by fire from the top of the right forward overwing emergency exit door to the fuselage centreline.

The aft fuselage section had rolled to the left and rotated counterclockwise about 45 deg. The aft fuselage skin structure between FS-950c and FS-950e [between seat rows 28 & 29] on the left side was consumed by fire. The aft fuselage skin structure on the right side between FS-950e and FS-1030 [between seat rows 31 & 32] was found laying on the ground alongside the fuselage. The centre aft fuselage section generally sustained various degrees of breakup and fire damage.

The aft cabin floor was completely consumed by fire from approximately station 950d [seat row 28] to station 1130 [just aft of seat row 36] with the exception of the passageway that led to the left-rear door at the galley. Fire damage to other components was also extensive in this area with most major interior components destroyed.

The area forward of station 950d had less severe fire damage to the floor and sidewall areas (continuing through station 740 [between seat rows 18 & 19]). As noted previously, there was a large area of burnthrough on the cabin floor from approximately station 740 to approximately station 720d [seat row 17] on the right side. From this point forward to approximately station 400 [just forward of seat row 1], fire damage to the right side of the interior was significantly more extensive than that of the left side. From station 400 forward, the cabin was free of fire damage.
Damage to ceilings, sidewalls, overhead stowage compartments, closets, etc., was closely correlated to areas of floor and fuselage burnthrough. From station 400 aft, no ceiling panels were in place including areas where the fuselage crown was present as well as the areas that were burned through.

All floor exit level door liners were found. The aft right exit had only the lower portion of the liner present from approximately the top of the escape slide container downward. It was heavily sooted and showed some signs of melting. The aft left door liner was essentially intact with heavy charring and melting over the upper third of the surface. Both of the left and right forward door liners were free of fire damage. The liner on the ventral door was destroyed by fire.

Sidewalls were intact on the left side of the forward cabin from station 380 [forward edge of starboard service door] to the area of the overwing exit. Many of these panels were melted at the upper portion above the window, but were in place and otherwise intact. Sidewall panels on the right side of the cabin in this zone were completely destroyed. Small portions of other right side wall panels were present near the floor toward the aft part of the cabin. Two sidewall panels on the left side at approximately stations 950b and c (immediately forward of the separation in the aft fuselage) were relatively free of fire damage but were heavily sooted.

From station 380 to station 760 [between seat rows 19 & 20] on the left side, there were some large remnants of overhead stowage units hanging from the structure. These were extensively burned and melted, however, they were recognisable as overhead stowage units. No other overhead stowage units were present.

In most areas of the cabin wherever the sidewall and/or ceiling panels were destroyed, the thermal insulation was also destroyed. The following exceptions were noted:

In the aft right corner of the cabin in the sidewall area just forward of and adjacent to the last row of seats.

Some small areas over the overhead stowage bin area along the left side of the cabin between station 380 and 950.

A few sidewall areas on the right side of the cabin near station 720.

The last row triple seats (right side) showed frame burn through and residual cushion fire blocking layer. Up to and including row 28, all seats were missing, with the exception of the triple seat in row 30 left side, which had some cushion fire blocking layer remaining, along with some seat frame structure burnthrough on the seat back cushions. Rows 23 through 26 (right side) were heavily fire damaged, but some cushion fire blocking layer remained. The seat back cushions on 23E, 24E, and 26D were burned through. Seats 27D, E, and F were severely damaged and its common frame was twisted and displaced several feet rearward from the proper position. Rows 26 and 27 (left side) had fire blocking layer present (frame intact), with the exception of seat back 26C which was totally destroyed by fire. Row 25 (left side) was missing (possibly falling down into the hole of the floor area). Rows 23 and 24 (left side) had fire blocking layer remaining, except for row 24A, B, and C, in which the seat back frame were
totally destroyed by fire. Seats 22B and C sustained severe damage to the seat bottoms. The remaining seats in rows 20 through 22 (left side) sustained fire damage to the seat backs, burning through the seat back frame, but the seat bottom cushions in this area had some fire blocking remaining. Rows 20 through 22 (right side) sustained fire damage to the seat back frame with some fire blocking layer remaining, except for row 20, which was pitched forward and bent up on the frame bottom.

Rows 15 through 19 (right side) sustained severe fire damage, with no fire blocking layer remaining. Rows 10 through 14 (right side) were destroyed by fire. Rows 10 through 19 (left side) had some fire blocking layer remaining on the seat back and bottom cushions, with the exception of rows 15 and 17, which sustained lesser amounts of fire damage.

In the first class section, seat row 1 (left and right sides) was intact with little fire damage. Row 2 (left side) had some fire damage to the seat back and bottom cushions with some fire blocking layer remaining. Seats 3A and B were twisted with some fire damage. Seats 4A and B were fire damaged with some cushion fire blocking layer remaining on the seat bottom. Row 3 (right side) was totally destroyed by fire. Row 4 (right side) was missing.

A number of lives were saved by the use of the fireblocking layer on the passenger seats. An exact number of additional survivors could not be determined.

The fire services arrived within 5 minutes of notification, the fire was knocked down within 5 minutes and extinguished in about 40 minutes.

**EVACUATION**

There were 3 flight crew, 4 cabin crew and 101 passengers aboard.

The investigation found that although the fuselage had separated in several places, the occupiable volume of the cabin was not substantially compromised. Passengers generally stated that impact forces were not severe. Further, the cause of deaths of the passengers in the aft section of the cabin were attributed to smoke inhalation and fire rather than impact injuries. Exit from the aft cabin was hampered by the fire that impinged on the right side of the aeroplane. Exit from the mid and forward cabin was through breaks in the fuselage and through the left side exits, except for the left aft service door which was not opened.

The right forward exit door was lodged in the wreckage. The main entry door (left forward) was open and wedged under the forward fuselage section.

Both overwing exit hatches on the left side were open. The hatch from the forward exit was found inside the cabin and the hatch from the rear exit was found outside the aircraft.

The right side rear overwing exit hatch was not opened, the hatch from the forward right side was found in the cabin.

The rear floor level exit on the right hand side was found not opened.
The left aft service door could not be opened due to deformation of the door frame which resulted from the aeroplane's repeated impacts with the ground.

A flight attendant, while attempting to open the left aft service door, stowed the girt bar on the door as per Delta's flight attendant training procedures which address the difficulty in opening a door following a gear-up landing.

It would have been unlikely for any one person of average strength to open the left aft service door under the circumstances existing at the time of the attempted evacuation.

The aft airstair was not usable because the aircraft was resting on its fuselage and the airstairs could not be lowered. Also the rear pressure bulkhead door to the tailcone was jammed closed due to impact damage.

**AIRCRAFT FACTORS**
The B727-232 was configured for a three person flightcrew and 149 passengers. The passenger cabin was configured with 12 first class passenger seats and 137 tourist class seats. A double occupancy aft facing flight attendant seat was on the aft left side of the cockpit rear bulkhead; a double occupancy forward facing flight attendant seat was located on the central airstairs door. A single flight attendant seat was in row 32.

The aircraft was delivered to Delta Air Lines in November 1973 in a passenger configuration. It was serial number 20750, line number 992. Fire blocking layers were fitted to passenger seats. This was the first survivable accident following the implementation of floor proximity and fire blocking rules. It can therefore be assumed that floor proximity lighting was also fitted.

The aircraft had a total of four floor level exits and four overwing emergency exits and a ventral stairway exit.

**ENVIRONMENTAL CONDITIONS**
The weather conditions at the time of the accident were: visibility 10 miles, wind 100 deg at 8 knots and temperature 22C.

**INJURIES TO OCCUPANTS**
Of the 108 persons on board, 12 passengers and two crewmembers were killed, 21 passengers and five crewmembers were seriously injured, and 68 passengers sustained minor or no injuries.

The cause of death of the 11 passengers and the two flight attendants was determined to be smoke inhalation. Levels of carboxyhemoglobin (COHb) ranged from 15 to 81 percent. Tests for drugs and ethanol were negative in all 13 persons. A 14th fatality was a passenger who had successfully evacuated but later attempted to re-enter the burning aeroplane. This passenger died of severe burns, 11 days after the accident.
2. **Fire Penetration Mechanism**

The fuselage broke into three main sections; the front, mid, and rear with significant breaks between them. These inter-section breaks were large enough to facilitate passenger egress from the wreckage during the evacuation.

For the front section, fire entered through the inter-section break.

For the midsection, fire was present along the whole of the right side and also extended round, under the fuselage and heated the inboard wing area on the left side. Fire entered through the inter-section breaks at either end and later burnt through in the centre wing box area, presumably as a result of the pool fire underneath.

For the rear section, fire entered the rear cargo area when the external door was torn off at its hinges. This fire propagated into the cabin by burning through the floor.

The accident report stated that a number of lives were considered as being saved by the use of the fire-blocking layer on passenger seats. An exact number of additional survivors could not be determined.

The fire services arrived within 5 minutes of notification; the fire was knocked down within 5 minutes and extinguished in about 40 minutes.

Based on the above it is assessed that the prime burnthrough route was in the midsection lower fuselage skin in the wing box area. However there was insufficient information to be conclusive about detail burnthrough areas.
3. Location of Injuries and Scenarios

Scenario 5

Scenario 4

Scenario 3

Scenario 2

Scenario 1
4. **Accident Scenarios and Survivability Chains**

This accident is divided into five separate scenarios.

**Scenario 1** contains the flight deck area. It was subjected to the initial impact, which was not severe, and was not fire damaged. The scenario contains the three flight crew.

**Scenario 2** contains the forward flight attendant vestibule area. It was subjected to the initial impact, which was not severe but was not fire damaged. The scenario contains two flight attendants who both left the aircraft through the L1 exit.

**Scenario 3** contains the front part of passenger cabin from the galleys to the inter-section break just aft of row 1. All injuries were sustained in the postcrash fire. Both occupants exited through the break in the wreckage.
**Scenario 4** contains the passenger cabin from the front fuselage break just in front of seat row 2 to the rear fuselage break at seat row 27 inclusive. All injuries were sustained from the external fire which entered the cabin through the breaks in each end and by way of a burnthrough from underneath the centre wing box area. The two overwing exits L2 and L3 were available for egress along with fuselage breaks.

![Diagram of Scenario 4]

**Scenario 5** contains the passenger cabin from the rear fuselage break at seat row 28 to the rear of the cabin. All injuries were sustained from the postcrash fire which entered the cabin by burning through the cabin floor from the cargo area where the cargo door had been torn off. All survivors exited through fuselage breaks.

![Diagram of Scenario 5]

5. **Effect of Later Requirements**

The aircraft seats were fitted with fire-blocking layers.

It is assumed that later requirements would not have affected the situation in Scenarios 1 and 2 in which all injuries were related to impact forces.

It is assessed that later requirements would have improved flammability standards such that the occupants would have had additional time to escape. Bearing in mind that fire was also entering directly through fuselage breaks, the additional time would have been modest.

In Scenario 3, the fire injuries were seated immediately next to the break (and fire) and so improved flammability standards would not have prevented injury from the direct assault.
The survivability chain for Scenarios 4 and 5 therefore become:

6. **Effect of a Fire-Hardened Fuselage**

**Scenario 1**

It is concluded that a fire-hardened fuselage would not have affected injuries in this scenario because the cause of injury was related to impact.

**Scenario 2**

It is concluded that a fire-hardened fuselage would not have affected injuries in this scenario because the cause of injury was related to impact.

**Scenario 3**

It is concluded that a fire-hardened fuselage would not have affected injuries in this scenario because fire entered through fuselage breaks and not by burning through the fuselage skin.

**Scenario 4**

It is concluded that a fire-hardened fuselage would have delayed the entry of fire from the centre wing box area and provided the occupants with more time to use the available overwing exits.
However, fire also entered through breaks at either end, and it is therefore assessed that the additional time would have been modest and would not have varied with additional burnthrough protection time.

The high, median, and low prediction from the use of a fire-hardened fuselage is:

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For an aircraft configured to the standards appropriate to the latest requirements this assessment becomes:

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**Scenario 5**

It is concluded that a fire-hardened fuselage would not have affected injuries in this scenario because fire entered through fuselage breaks and not by burning through the fuselage skin.

**Summary**

The assessed median number of lives saved by a fire-hardened fuselage are:

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<th>Burnthrough Protection Time</th>
<th>Aircraft in its Actual Configuration</th>
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<td>8 minutes</td>
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1. Description of Accident

RESUME
On 1-Feb-91 a Boeing 737-300 registered as N388US was landing at Los Angeles International Airport. It collided with a Fairchild Metroliner that was positioned on the same runway awaiting clearance for take-off.

The aircraft slid to the left side of the runway and into an unoccupied fire station whereupon a fire broke out. An evacuation took place but 20 passengers and 2 crewmembers were fatally injured.

IMPACT
The B-737 remained largely intact as a result of the collision with the Metroliner. The aircraft veered off the runway and into an unoccupied fire station building.

The only parts of the B-737 that separated from the aircraft were the nose cone, nose gear doors and left pitot tube. The impact with the building destroyed the cockpit and damaged the left engine and an area of the left wing leading edge. The top and left sides of the cockpit were crushed inward, and the forward section of the cockpit on the captain's side was crushed in, down, and to the right. Both forward cockpit windshields were cracked. Several propeller slashes were on the lower right side of the B-737 fuselage skin in the area of the forward galley door.

No fuel tank rupture or leakage from the wing or centre tanks of the B-737 was observed. The fuel which was on fire came from the ruptured fuel cells of the Metroliner which was found crushed underneath the B-737.

Several passengers noted that the landing appeared to be routine; however, within a few seconds of touchdown they recalled feeling the aeroplane, move up and down, consistent with heavy brake applications. They noticed 'an orange glow' through the cabin windows on both sides of the aeroplane; flight attendants were heard yelling repeated commands 'get down, stay down'. After the impact with the building, the flight attendants commanded the passengers to release their seatbelts. The two rear flight attendants and several passengers had unbuckled their seatbelts after the first impact and were thrown forward when the aeroplane struck the building.

The R1 flight attendant stated that the 'touchdown felt normal' and that shortly thereafter 'I heard a big metal scrape, and felt like they slammed the brakes real hard'. Within 2 or 3 seconds, the emergency lights came on and he began to shout commands, 'grab ankles, heads down, stay down'. He saw the floor in front of him moving up and down about knee high.
FIRE
After the first impact, and while the aeroplane was still moving, the R1 flight attendant noted that the cabin became 'really warm', and he observed smoke coming from underneath the floor in front of him. He also remembered seeing smoke and fire on top of the valet closet in front of him. He described the smoke as 'very thick'.

On the right side of the fuselage there were a number of small holes burnt through in the lower quadrant and a large hole had burnt through in the lower belly area.

In the forward cargo compartment, the cargo liner on the left side had very little heat damage whereas the right forward corner had heavy fire damage. There was slight fire damage to the cargo liners, the cabin floor, cargo compartment floor and some structure in the area. This area also housed the crew oxygen cylinder, which was found loose. The cylinder contained heavy amounts of soot, except for the area of an attaching strap. The pressure gauge and regulator were extensively fire damaged, and the overpressure and supply lines were broken.

The fire fighters were able to control the fire under the aircraft but fire continued in the cabin.

An area of the top of the B-737's fuselage burnt through in the forward cabin between the first class and coach sections. This burnthrough was from the inside out. Interior fire damage in this area was extensive.

The top of the fuselage was also burned away from just aft of the wing to the aft doors. The fuselage along the floor beams was still attached aft of the wing so eventually the entire tail section drooped to the ground. At this time the fire fighters were able to advance into the cabin and extinguish all remaining fires.

The forward passenger door (L1) was jammed shut, and the lower half of the door was displaced inward approximately 6 inches. There was no fire damage to the exterior of the door. The forward service door (R1) was open. The door was structurally intact, but its interior had sustained significant fire and heat damage. The exterior of the door contained soot near its bottom forward side. The aft passenger door (L3) was open, and both sides of the door were fire damaged. The aft service door (R3) was open. There was no soot on the interior surface of the door, and minor amounts of soot were evident on the exterior.

The most extensive interior fire damage was in the rear of the cabin. All interior materials located at 4 feet or higher were destroyed by the interior fire. The carpet was intact throughout the cabin with the exception of the first class section. The cabin seats contained fire blocking material with Poly-Benizol Iomedizal (PBI). Floor proximity emergency lights were installed in the cabin and based on survivor's statements they functioned during the evacuation. All of the overhead baggage compartments were found detached and melted onto the seats.

EVACUATION
There were 2 flight crew, 4 cabin crew and 83 passengers aboard. The cabin layout had 128 passenger seats.
As the aeroplane struck the abandoned fire station and stopped, the R1 flight attendant departed his jumpseat and went to his exit door. After assessing the area outside the door for fire, he rotated the handle to the open position and attempted to open the door. During this time he said that the smoke got so bad that he could no longer see anything. After forcing the door, he was able to open it about 12 inches and shortly thereafter he was able to open it fully. At that point, a passenger was standing by the door, and he pushed the passenger out of the aeroplane. The distance from the door sill to the ground was about 5 feet. Another passenger then passed the R1 flight attendant and jumped out. The flight attendant then attempted to enter the cabin near row 1; however, the smoke and flames were too intense. Returning to the R1 door, he jumped to the ground.

Several passengers who had been seated in the coach cabin between rows 4 and 13, escaped via the two overwing emergency exits and the R3 service door. Because of the fire, only two passengers were able to escape from the left overwing emergency exit. They crawled along the left wing and jumped from the leading edge of the wing to the ground.

About 37 passengers escaped via the right overwing emergency exit. Their egress was hampered by the passenger seated in seat 10-F who stated that she was very frightened and 'froze', and was unable to leave her seat or open the window exit next to her. The male passenger seated in 11-D climbed over the 10-E seatback and opened the overwing exit; he pushed the passenger seated in 10-F out the window and onto the wing and then followed her. During the subsequent evacuation through the right overwing exit, two male passengers had an altercation at the open exit that lasted several seconds.

The outboard seatback at 10-F adjacent to the right overwing exit was found folded forward after the accident blocking approximately 25 percent of the exit opening. The retaining bolt at the seat's pivot point was sheared. The timing of this occurrence could not be determined.

Passengers who escaped by the right overwing exit made their way across the right wing and slid down the extended flaps. They were directed away from the aeroplane by flight attendants and fire fighters who, they estimated, arrived on scene 1 to 2 minutes after the B-737 struck the abandoned fire station.

Passengers seated around row 10 stated that prior to departure the flight attendant assigned to the R1 position conducted a special oral briefing for the persons seated in and around row 10. Passengers stated that the instructions provided by the R1 flight attendant aided in their evacuation.

Fifteen passengers seated aft of the overwing area who made their way to the rear of the cabin reported using the emergency floor path lighting. All of the passengers stated that the cabin filled with thick black smoke within seconds of the impact with the building.

The L3 flight attendant stated that she slightly opened her door without difficulty before impact with the building; however, the outside of the door was ablaze so she closed the door. She had taken about two steps into the cabin when the building was struck. She did not return to the door. After the final impact, she attempted to make her way to the overwing exits in accordance
with company procedure. Because of the number of passengers moving aft, she was only able to advance forward to the seats at rows 19 and 20 on the left. From there, she directed the passengers to the rear of the cabin.

After the final impact, the flight attendant who was assigned to the R3 door opened the door, deploying the emergency slide, and evacuated about 15 passengers. He then exited and directed passengers away from the aeroplane.

Four of the six exits were used during the emergency evacuation: the R1 forward service door, and left and right overwing emergency exits, and the R3 service door. The L1 exit was damaged subsequent to the secondary impact with the abandoned fire station. The L3 exit was opened by the L3 flight attendant during the slide to a stop between the first and second impacts; however, because of flames along the left side of the aeroplane, she stated that she closed the door and elected not to use it thereafter. Investigators found the door open with the slide deployed. It was determined that ARFF personnel had opened the door well after the accident.

The R1 slide pack did not deploy. It was found below the door in an area where the floor was burned away. The postcrash examination of the girt bar and its two retaining brackets revealed that the bolts that secured the retaining brackets to the floor on the inboard side of the door were bisected (sheared off at floor level). The R3 slide pack deployed as designed when the door was opened by the R3 flight attendant to initiate the emergency evacuation.

**AIRCRAFT FACTORS**
The aircraft was a B737-300 registered as N388US operated by USAir.

The aircraft was fitted with two doors at the front and two doors at the rear of the passenger cabin. In addition there was an overwing emergency exit above each wing.

The aircraft was manufactured in 1985. Although the interior was partially refurbished in 1989, most of the interior panel were from state-of-the-art materials at the time of original aircraft manufacture. The cabin seats contained fire blocking material with Poly-Benzol Iomedizal (PBI). Floor proximity emergency lights were installed in the cabin.

The cabin configuration was predominantly 3 abreast either side of a central isle. The total passenger capacity was 128.

**ENVIRONMENTAL CONDITIONS**
The accident happened at night with a visibility of 15 miles. Wind was 260 deg at 6 knots and the temperature was 14C.

**INJURIES TO OCCUPANTS**
Of the 89 persons aboard the B-737, 20 passengers, one flight attendant and the captain were fatally injured. Autopsies of the 19 passengers and one flight attendant who were removed from the wreckage revealed that they died of asphyxia due to smoke inhalation. One person who evacuated the aeroplane died as a result of thermal burns a few days later. The captain
succumbed to multiple traumatic injuries. In addition, one passenger died of thermal burn injuries 31 days after the accident.

2. Fire Penetration Mechanism

After the first impact and while the aircraft was still moving, the R1 flight attendant observed smoke coming from underneath the floor in front of him. As he was positioned rearward facing in the vestibule area, it is assessed that fire was coming through the floor from the cargo area. It is considered that the observed smoke appeared too quickly for a burnthrough of the fuselage skin to have occurred from the burning Metroliner underneath. It is assessed more likely that fire penetrated into the cargo area through ruptures in the lower fuselage skin.

It was stated that there was little damage to the carpet along the length of the fuselage and it is therefore concluded that there was no burnthrough in the rear fuselage. However, the roof in that area burnt through from inside out; hence, it is assessed that once the fire had entered at the front, it then propagated aft along the cabin and accumulated just rear of the main wing.

On the right side of the fuselage there were a number of small holes burnt through in the lower quadrant and a large hole had burnt through in the lower belly area. It is assessed that these penetrations were insignificant compared with the main fire entry path and did not constitute a significant threat to the occupants.

The firefighters were able to control the fire under the aircraft but not the one in the cabin. Later in the sequence of events the top of the fuselage burnt through in the forward cabin area between the first- and coach-class sections from the inside out. Eighteen minutes after the accident occurred, the top of the fuselage also burned away from just aft of the wing to the aft doors which caused the entire tail section to droop to the ground and allow firefighters to advance into the cabin and extinguish all remaining fires.

Based on the above, it is assessed that the prime burnthrough route was through the fuselage lower skin; however, more significant, non-burnthrough fire entry paths were also present.
3. Location of Injuries and Scenarios

Scenario 1

Scenario 2

Scenario 3

Scenario 4
4. Accident Scenarios and Survivability Chains

This accident is divided into four separate scenarios.

**Scenario 1** contains the flight deck area. It was subjected to substantial impact damage as the aircraft impacted the fire station building and was crushed. The captain sustained multiple traumatic injuries. The unknown injury was assumed to be serious and as a result of the fire.

**Scenario 2** contains the forward flight attendant vestibule area. This was the area in which fire first penetrated the passenger cabin. Visibility was restricted by very thick smoke. The scenario contains two flight attendants. The unknown injury was assumed to be serious and as a result of the fire.

**Scenario 3** contains the passenger cabin from seat row 1 in first class to row 7 in the coach section inclusive. This was subjected to dense smoke and fire and R1 & R2 were the only usable exits. It can be assumed that the smoke hindered the occupants’ attempts at locating the exits.
and as a result many were overcome. This scenario contains 27 passengers. The unknown injuries were assumed to be serious and as a result of the fire.

![Diagram of Scenario 3]

**Scenario 4** contains the passenger cabin from seat rows 8 to the rear of the cabin, including the aft cabin attendant area. This area had the longest time before smoke and fire hampered evacuation. R2, L2, and R3 exits were available for egress. The scenario contains 56 passengers and 2 flight attendants. The unknown injuries were assumed to be serious and as a result of the fire.

![Diagram of Scenario 4]

5. **Effect of Later Requirements**

It is assumed that there would be no change to the number of fatalities for Scenarios 1 and 2 had the aircraft been configured to the standards required by later requirements.

The aircraft was configured with floor proximity lighting and seat fire blocking but did not meet improved flammability standards.

Additional time would be provided to facilitate escape if the aircraft had been configured to these improved flammability standards. Furthermore improved access to Type III exits is likely to have relieved the congestion at the overwing exit. However, due to the intensity of the fire, which was most probably increased in severity by the release of oxygen, the reduction in fatalities resulting from these improvements would have been modest.
The survivability chain for Scenarios 1 and 2 are therefore unchanged, and Scenarios 3 and 4 become:

6. Effect of a Fire-Hardened Fuselage

Scenario 1

It is concluded that a fire-hardened fuselage would not have affected injuries in this scenario because the predominant cause of injury was impact.

Scenarios 2 and 3

It is concluded that a fire-hardened fuselage would not have affected the situation in Scenarios 2 and 3 because it is assessed that fire entered through ruptures in the lower fuselage skin.

Scenario 4

It is concluded that a fire-hardened fuselage would not have affected the situation in Scenario 4 because it is assessed that fire entered the scenario by propagating along the fuselage from Scenario 3.

Summary

It is assessed that no benefits would be gained by a fire-hardened fuselage in this accident since the prime fire entry path was through fuselage ruptures.
AIRCRAFT: A320   DATE OF ACCIDENT: 14th September ‘93
REGISTRATION: D-AIPN   LOCATION OF ACCIDENT: WARSAW

1. Description of Accident

RESUME
On 14-Sep-1993 a Lufthansa A320 registered as D-AIPN was landing at Okecie in Warsaw, Poland.

The pilot in the left seat was subject to check but was the pilot flying at the time of the accident. The pilot in the right seat was the instructor who was in overall command of the aircraft.

Okecie tower warned the crew of windshear and so the flight crew increased the approach speed by 20 knots, in accordance with the Flight Manual. A storm front passed through aerodrome area at that time which produced a tail wind and as a result the aircraft touched down too fast. The very light touch of the runway surface with the landing gear and lack of compression of the left landing gear leg (to the extent understood by the aircraft computer as the actual landing) resulted in delayed deployment of spoilers and thrust reversers. Delay was about 9 seconds. Thus the braking commenced with delay and in a condition of heavy rain the aircraft did not stop on the runway.

The aircraft ran off the end of the runway, collided with an embankment and stopped the other side of it. The aircraft caught fire as a result of the impact.

There were 6 crew and 64 passengers aboard. 1 crew member and 1 passenger suffered fatal injuries. 2 crew and 49 passengers suffered serious injuries. 3 crew and 14 passengers escaped with minor or no injuries.

IMPACT
Okecie tower warned the crew of windshear and so the flight crew increased the approach speed by 20 knots, in accordance with the Flight Manual. A storm front passed through aerodrome area at that time which produced a tail wind of approximately 20 knots and as a combined result the aircraft touched down too fast.

After first contact of the right landing gear assembly with the runway the pilot attempted to use wheel brakes, and when they failed to work, demanded the right-seat pilot to assist.

Automatic systems of the aircraft depend on oleo strut compression and a weight-on-wheels switch to unlock the use of ground spoilers and engine thrust reversers. Only when the left landing gear touched the runway were these systems activated. As a result of the light and fast landing the operation was delayed by about 9 seconds. The systems then began to operate; the spoilers deployed to full angle (50 deg), the thrust reverser system began to work and N1 of the engines came to 71%, but the wheel brakes, depending on wheel rotation being equivalent of circumferential speed of 72 kts began to operate only after about 4 seconds.
Deceleration of the aircraft progressed in conditions of heavy rain and with a layer of water on the runway. The aircraft was decelerated with normal performance but on the last 180 metres of runway deceleration decreased by about 30%. Residual length of the runway (left from the moment when braking systems had begun to work) was too small to enable the aircraft to stop on the runway. Seeing the approaching end of runway and the embankment behind it, the pilot tried to turn the aircraft but only managed to deviate the aircraft a little to the right.

The aircraft ran off the end of runway with a speed of 72 kts and having travelled 90 metres its left wing collided with the embankment. The aircraft slid over the embankment, destroying an aerial and stopped right behind the embankment. During this stage the landing gear of the aircraft and the left engine were destroyed.

The bottom part of the fuselage up to the wing area was found significantly deformed, and broken in the wing area.

The radar dome and radar antenna were found detached from the aeroplane and destroyed.

The aft part of the fuselage, from aft doors, was found complete, with minor deformation.

The left main landing gear was detached, the wheel assembly partially burnt and no pressure was found in the tyres. The right main landing gear was partially detached from the fuselage fittings but otherwise complete. Wheels and tyres were not damaged. The front landing gear detached from the fuselage but the wheels and tyres were not damaged.

FIRE
In the collision of the aircraft with the embankment and with the aerial located on it, the fuel tanks of the aircraft were broken and the fuel began to spill on the left side of fuselage. It was most probably ignited because of contact with hot parts of the damaged left engine or with the electrical system of the aerial. It caused a fire on the left wing. The fire spread onto an area of about 600 square metres. Shortly the fire penetrated into passenger cabin, creating smoke at first, and later filling the whole cabin. In 3 minutes from the emergency call, 5 Aerodrome Fire Service cars came to the scene. They managed to extinguish the external fire and successfully evacuated the passengers remaining in the area of danger (and blocking access for the fire services) to a safe distance.

It was impossible to stop the fire inside the aircraft. Neither the foam introduced through the open entrance, nor the attempts to open the emergency exits on the left wing and to break into the cabin were successful. Eventually pouring foam through the broken out cockpit windows gave a positive result. After 2 minutes from the beginning of the activities on the scene, the tank in the middle part of wing blew out. For the next 30 minutes extinguishing of the burning fuselage through the hole created in the roof was continued.

15 minutes after the emergency call four cars of the national fire service came to the aircraft and entered into action.
Calculation of the residual fuel, remaining on board at the moment of impact and the amount drained from the wreckage indicates that about 2900 litres of aviation kerosene JA-1 was burnt in the fire and due to action of the high temperature of oxygen bottles about 12000 litres of oxygen was released, which obviously increased the intensity of the fire.

After the fire had been extinguished, about 6000 litres of fuel were drained from the tanks of the wreckage.

The upper part of the fuselage from the cockpit to the fin and to bottom of the passenger cabin was burnt out, including cabin furniture and equipment. The cockpit was found burnt out.

**EVACUATION**
There were 2 flight crew, 4 cabin crew and 64 passengers on board.

A successful evacuation of passengers, organised by the 4 cabin crew, in conditions of an aircraft fire, contributed to the rescue of 63 passengers of the 64 on board.

The front and aft passenger doors were found open with escape slides deployed.

During the landing the cabin crew were seated in two pairs, one near to the front entrance and the other near to aft entrance. Only two were available to act immediately. A stewardess from the aft pair, due to breathing difficulties, fainted after opening the door and initialisation of the escape slide and was unable to take part in the further activities, and chief steward (with injured head), who was in the front part of cabin, remained unconscious all the time during passenger evacuation. After regaining consciousness he managed to release the injured pilot blocked in the cockpit, enabling him to leave the aircraft through the open front door. But he was not able to lift the body of the instructor remaining in the cockpit.

The prompt and successful evacuation of 63 persons out of the passenger cabin during increasing smoke and intensive fire was directly due to the behaviour of the cabin crew, in spite of their injuries. The two active cabin attendants played a significant and unquestionable role preventing the panic and organising the movement of passengers to the exits.

The passenger seated in the utmost left seat in "business class" sustained a fracture of the first lumbar vertebra and of both hands. This made him probably unable to leave his seat unaided. In addition, his temporary loss of consciousness during the impact did not allow him to draw the attention of other passengers and cabin attendants.

The situation would have been significantly more severe if the injured persons needed individual direct assistance leaving the wreckage or if the type of injuries required immediate intervention, e.g. because of haemorrhage or need for reanimation. A sufficient number of ambulances did not come to the scene quickly enough and some injured were carried to the airport by casual means of transport (e.g., bus).
AIRCRAFT FACTORS
The aircraft was an A320, registered as D-AIPN and operated by Lufthansa.

The airframe was production serial number 105, manufactured in 1990 with a Certificate of Airworthiness dated 25-Apr-1990.

The aircraft carried JA-1 kerosene fuel.

At the time of the accident the tyre tread depths were low and may have been a factor in the inability of the aircraft to stop within the confines of the runway, especially in heavy rain.

The cabin was fitted with two doors at the front and two doors at the rear of the passenger cabin. In addition there were 2 overwing emergency exits over each wing.

ENVIRONMENTAL CONDITIONS
The accident happened during daylight hours in windshear conditions.

Wind was 150 deg at 12 kts. The ambient temperature was about 20C.

INJURIES TO OCCUPANTS
There were 6 crew and 64 passengers aboard. 1 crew member and 1 passenger suffered fatal injuries. 2 crew and 49 passengers suffered serious injuries. 3 crew and 14 passengers escaped with minor or no injuries.

Of the 2-person cockpit crew, the left-seat pilot survived (injured by impact) and the right-seat pilot was killed outright.

The autopsy of the body of the right-seat indicates that he was killed during the impact due to collision with cockpit interior elements. It was confirmed by extensive damage to the internal organs, namely: rupture of pericardial sac and of the main artery wall, rupture of internal membrane of aorta, perforation of the lungs with broken ribs. Presence of the carbon oxide haemoglobin or alcohol in the blood of the pilot was not stated. During examination of stomach contents and kidney neither drugs nor medicines affecting the capacity or capability to perform pilot duties were discovered.

In the blood of the fatally injured passenger 22.6% of carbonoxide haemoglobin was found, and in the opinion of the person who performed the autopsy intoxication with carbon oxide in the environment of the high temperature was the cause of the death.
The injury profile was as follows:

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<th>Injury Type</th>
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<td>Head injuries</td>
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<tr>
<td>Chest injuries</td>
<td>8</td>
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<tr>
<td>(Broken ribs)</td>
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<td>Abdomen contusions</td>
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<td>Broken limbs</td>
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<tr>
<td>Burns</td>
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<td>Other</td>
<td>9</td>
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<tr>
<td><strong>Total</strong></td>
<td><strong>56</strong></td>
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2. **Fire Penetration Mechanism**

When the aircraft came to a stop, spilled fuel on the left side of the fuselage ignited and spread onto an area of about 600 square metres, including the left wing. With this amount of fire impinging on the fuselage side, it is assessed that it burnt through and penetrated into the passenger cabin from the left side. Smoke was reported as appearing first and later the fire entered, eventually filling the whole cabin.

Only one surviving passenger was identified as suffering burn injury and so it is concluded that the burnthrough occurred toward the end of the evacuation time.

It was stated that the Aerodrome Fire Service cars came onto the accident scene approximately 3 minutes from the emergency call and moved the evacuated passengers away from the aircraft. It is therefore assessed that evacuation had been completed in 2 to 3 minutes.

One passenger was stated as having immobilising impact injuries and it is assumed that this person was the eventual fatality, being overcome by the fire.

The wreckage was described as having a hole burnt through the roof and the upper part of the fuselage burnt out. It is assumed that this was as a result of the intense internal fire burning through from the inside out and the major burning of fuselage skin would have occurred well after the evacuation was completed.

Based on the above, it is assessed that the prime burnthrough route was through the fuselage skin. However there was insufficient information to be conclusive about other burnthrough areas.
3. Location of Injuries and Scenarios

Scenario 1

Scenario 2

Scenario 3

Scenario 4
4. Accident Scenarios and Survivability Chains

This accident is divided into four separate scenarios.

**Scenario 1** contains the flight deck area where all injuries were sustained from the impact. The scenario contains the two flight crew.

![Scenario 1 Diagram]

**Scenario 2** contains the forward flight attendant area where injuries were sustained from the impact. The scenario contains two flight attendants.

![Scenario 2 Diagram]

**Scenario 3** contains the whole passenger cabin. This has been used because of limited detail on passenger locations during the accident. The scenario contains all 64 passengers.

![Scenario 3 Diagram]
Scenario 4 contains the rear flight attendant area where only minor injuries were sustained from the impact. The scenario contains two flight attendants.

5. Effect of Later Requirements

The aircraft was manufactured in 1990. It is assumed that lower heat release materials had been fitted and that seat-blocking layers were already installed. Since there was only one fire fatality in this accident, any errors in these assumptions would have a minimal effect on the conclusions of this study.

It is therefore assumed that later requirements would not have affected the situation in any scenario.

6. Effect of a Fire-Hardened Fuselage

Scenario 1

It is concluded that a fire-hardened fuselage would not have affected the situation in Scenario 1 as all injuries were sustained as a result of the impact.

Scenario 2

It is concluded that a fire-hardened fuselage would not have affected the situation in Scenario 2 as all injuries were sustained as a result of the impact.

Scenario 3

It is concluded that a fire-hardened fuselage would have delayed the entry of fire and may have provided the passengers with additional time to go back and remove the immobilised passenger who was seated near to the forward passenger door.
The high, median, and low prediction from the use of a fire-hardened fuselage is:

For 30 seconds protection:

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<tr>
<td>M</td>
<td>14</td>
<td>50</td>
<td>0</td>
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</tbody>
</table>

Scenario 4

It is concluded that a fire-hardened fuselage would not have affected the situation in Scenario 2 as all injuries were sustained as a result of the impact.

Summary

The assessed median number of lives saved by a fire-hardened fuselage are:
APPENDIX B  Dimitris Sikoutris full Curriculum Vitae.
Appendix B: Dimitris Sikoutris Curriculum Vitae.

Dimitris Sikoutris Curriculum Vitae

PERSONAL
Surname/Name Dr. Sikoutris Dimitrios
Date of Birth 08 August 1981
Present Status Research Engineer, PhD, MSc, DiplEng, Mechanical & Aeronautics Engineering. Patras University Campus, GREECE
Nationality Hellenic (Greek)

Addresses
Rega Feraiou 34, Ano Ovria, 26500 Patra, GREECE
Tel: (+30) 2610-524130
Mob: (+30) 6977939559
Email: dsikoutr@mech.upatras.gr
dsikoutris@gmail.com

EDUCATION
Diploma in Mechanical & Aeronautics Engineering Dept. of Mechanical & Aeronautics Engineering, University of Patras, GREECE. Started: September 2000 Graduated: November 2005 Ranking: First of his class Degree: 8.23/10
Philosophiae Doctor Dissertation


DIPLOMA THESIS

• “Kinematic and Aerothermodynamic Analysis of the Space Transportation System STS”, 2005.

FOREIGN LANGUAGES

English.

COMPUTING and PROGRAMMING EXPERIENCE

Operating Systems
Dos, Windows 3.11, 98, NT, 2000, XP, VISTA, WIN 7, Unix

Programming Languages
Fortran 77, Fortran 90

Scientific Computation packages
MatLab, Origin, Excel, Mathematica
Windows:
ANSYS, FLUENT, CFX,
GAMBIT, ICEM-CFD, Phoenics,
Lusas

Linux:
ANSYS, FLUENT, CFX,
GAMBIT

Computer Aided Design
CATIA V5, SolidWorks, Mechanical Desktop

WORK EXPERIENCE

• Hellenic Army, Corps of Engineers/Technicians, Chief Instructor on Composite Materials Maintenance and Repair Procedures, 2012.
• Hellenic Army, Corps of Engineers/Technicians Certified Aircraft/Helicopter Maintenance and Repair Engineer, 2012.
• Research Engineer in Applied Mechanics Laboratory, Dept. of Mechanical & Aeronautics Engineering, University of Patras, GREECE. 2006-present.
• Practice work as undergraduate Mechanical Engineer at Frigoglass R&D Department, 2003.

RESEARCH INTERESTS

• Application of Composite Materials in the Aeronautics and Aerospace Industries.
• Composites materials (characterisation modelling, thermomechanical characterisation).
• Manufacturing processes of Composites Materials.
• Aerodynamic Design and Manufacturing of Unmanned Aerial Vehicles UAVs.
• Aerodynamic Design and Analysis of lightweight vehicles, Race Car Aerodynamics.
• Analysis of Re-entry Spacecrafts (Space Shuttle kinematic and aerothermodynamic analysis in hypersonic regime during re-entry in the earth’s atmosphere).
Appendix B: Dimitris Sikoutris Curriculum Vitae.

Participation in Research Programs

- **FP 7, EU-STREP:** “Multifunctional Layers for Safer Aircraft Composite Structures”, LAYSA, 2008-present
- **FP 6, EU-STREP:** “Vulnerability Analysis for Near Future Composite/Hybrid Aerostructures: Hardening via New Materials and Design Approaches Against Fire and Blast Due to Accidents or Terrorists Attacks”, VULCAN, 2006-2011.

- Design and Manufacturing Manager of the University of Patras ATLAS IV radio-controlled Unmanned Aerial Vehicle (UAV), 2012-present.
- Design and Manufacturing Manager of the University of Patras ATLAS III radio-controlled Unmanned Aerial Vehicle (UAV), 2010-2011.

**PUBLICATION in JOURNALS & Technical Papers**


Participation in Conferences

• D. Sikoutris, D. Vlachos and V. Kostopoulos, “Multi-stage decomposition modeling of polymer matrix composites. Fire burnthrough response of CFRP aerostructures”, Sixth International Conference on Composites In Fire (CIF 6), Newcastle, UK, June 2011.


Awards:
• Young Aerospace Engineer of the Year 2009, Technology & Innovation Award. “The Atlas Project: Development of a Radio-Controlled Full Composite Aircraft”.
• Diploma Thesis Award from the Technical Chamber of Greece. 2006.

Member of:
• European Association of Aerospace Students, EUROAVIA, 2002-present.
• Technical Chamber of Greece.

Other Activities
• Volunteer as Team Leader in Children Summer Camp. 2000-present.
• Coach & manager in a teen basketball team.
• Aeromodelling.
• DIY projects.