Characterisation of mixed-mode delamination growth in carbon-fibre composites

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Abstract

Delamination is one of the main areas of concern when using composite materials in primary aircraft structures. Delamination can be generated by a variety of sources, such as impact damage, and can lead to large reductions in compressive properties. A substantial amount of work has already been done to investigate delamination. Experimental studies have been conducted where delaminations have been grown from embedded inserts within laminates under compressive loading. Analytical and finite element models have been developed which use failure criteria developed from fracture toughness tests to predict initiation of delamination growth. However, the fracture processes which occur at the delamination boundary are poorly understood and consequently there has been little work to link experimental and analytical studies. The main aim of the present work was to address the problem by examination of delaminated surfaces; i.e. by fractographic analysis.

Realistic delamination shapes, sizes and locations were identified by studying impact damage in a stringer-stiffened panel. Delaminations were then grown from embedded inserts in honeycomb-stiffened panels under quasi-static compression. The loading conditions and failure processes at the delamination boundary were characterised and understood through examination of the fracture surfaces, using failure surfaces generated in controlled coupon tests as a reference. The results of this work indicated how laminate stacking sequences can be tailored for improved damage tolerance.

As well as generating controlled failure surfaces, the coupons were used to characterise the delamination failure loci. The form of these loci were also explained through examination of the fracture surfaces and identification of the dominant damage mechanisms. The data was then fitted to failure criteria from the literature. Finally, finite element models of the delamination from embedded inserts were developed which predicted initiation and location of the damage processes at the defect boundary.
List of Contents

ABSTRACT 2

LIST OF CONTENTS 3

LIST OF TABLES 10

LIST OF FIGURES 12

1. INTRODUCTION 19

1.1 Background 19
   1.1.1 Delamination - Causes and Effects 19
   1.1.2 Delamination Mechanisms 19

1.2 Aims of the Work 20
   1.2.1 Discussion 20
   1.2.2 Programme Aims 20
   1.2.3 Characterisation of Impact Damage in Stringer-Stiffened Panels 21
   1.2.4 Mixed-Mode Delamination Toughness Testing 22
   1.2.5 Embedded Delaminations in Plain Panels 22
   1.2.6 New and Unique Aspects 23

1.3 Delamination Literature Survey 25
   1.3.1 Introduction 25
   1.3.2 Through-Width Delaminations 26
   1.3.3 Embedded Delaminations 27
   1.3.4 Delaminations in Structures 31

1.4 Mixed-Mode Literature Survey 32
   1.4.1 Introduction 32
   1.4.2 Aims of the Survey 32
2.2 Aim of Impact Studies 64

2.3 Experimental Details 64
   2.3.1 Panel Design and Geometry 64
   2.3.2 Impact Testing 65
   2.3.3 Non-Destructive Evaluation 66
   2.3.4 Sectioning and Microscopy 66

2.4 Experimental Results 66
   2.4.1 Non-Destructive Evaluation 66
   2.4.2 Optical Studies 68

3. MIXED-MODE TESTS 71

3.1 Preliminary Investigations 71
   3.1.1 Introduction 71
   3.1.2 Arcan Test Studies 71
   3.1.3 MMB Test Studies 72
      3.1.3.1 Test details 72
      3.1.3.2 Mode I Results and Fracture Morphology 73
      3.1.3.3 Mixed-mode Results and Fracture Morphology 74
      3.1.3.4 Mode II Results and Fracture Morphology 75
      3.1.3.5 Discussion 76
   3.1.4 Problems and Recommendations 78

3.2 Validation of the MMB test and specimen design 79
   3.2.1 Introduction 79
   3.2.2 Validation Specimen Manufacture and Testing 79
   3.2.3 The Effect of Rig Geometry on Toughness 80
   3.2.4 Comparison Between Data Reduction Methods 80
   3.2.5 Variation of Mixed mode Ratio with Crack Length 81
   3.2.6 Specimen Orientation for Multidirectional Specimens 81
   3.2.7 Edge Opening in Multidirectional Specimens 82
List of Contents

3.3 Experimental Details
  3.3.1 Test Programme 82
  3.3.2 Specimen Manufacture and Preparation 84
  3.3.3 Specimen Testing, Data Reduction and Fractography 85
  3.3.4 Moisture Conditioning Results 87
  3.3.5 Material Property Test Results 87

3.4 Mixed mode Test Results 87
  3.4.1 Details of the Experimental Results 87
  3.4.2 Test Results for Unidirectional T800/5245 and T800/924 89
  3.4.3 Test Results for Multidirectional T800/924 92
  3.4.4 Specimen Compliance Calculations 93

3.5 Fractographic Analysis 93
  3.5.1 Introduction 93
  3.5.2 Fracture Surface Morphology for MMB Specimens 94
    3.5.2.1 Unidirectional Specimens 94
    3.5.2.2 Multidirectional Specimens 96
  3.5.3 Cusp Angles 97

3.6 Failure Criteria 98
  3.6.1 Introduction 98
  3.6.2 Details of the Curve Fitting 98
  3.6.3 Results of the Curve Fitting 101

4. EXPERIMENTAL DELAMINATION STUDIES 104

4.1 Preliminary Studies 104
  4.1.1 Aims of the Current Phase 104
  4.1.2 Choice of Specimen Type 104
  4.1.3 Panel Design 106
  4.1.4 Manufacture 107
  4.1.5 Testing and Failure Analysis 108
## 4.2 Honeycomb Sandwich Panel Studies

109

4.2.1 Introduction and Programme Details 109

4.2.2 Experimental Details

4.2.2.1 Manufacture 110

4.2.2.2 Testing and Instrumentation 111

4.2.2.3 Image Analysis 111

4.2.2.4 Failure Analysis 112

4.2.3 Experimental Results 112

4.2.3.1 Testing Details 112

4.2.3.2 $0^\circ/90^\circ$ Ply Interface Delaminations 113

4.2.3.3 $+45^\circ/-45^\circ$ Ply Interface Delaminations 115

4.2.3.4 Effect of Ply Interface of Embedded Defect 117

4.2.4 Failure Analysis Results 117

4.2.4.1 $0^\circ/90^\circ$ Ply Interface Delaminations 117

4.2.4.2 $+45^\circ/-45^\circ$ Ply Interface Delaminations 121

## 5. FINITE ELEMENT DELAMINATION STUDIES

124

5.1 Introduction and Objectives 124

5.2 Description of the Finite Element Model

5.2.1 Mesh Geometry 124

5.2.2 Characterisation of Damage Initiation 126

5.2.3 Mesh Convergence 127

5.3 Model Predictions 128

5.3.1 Defect at a $0^\circ/90^\circ$ Ply Interface; Model #1 128

5.3.2 Defect at a $+45^\circ/45^\circ$ Ply Interface; Model #2 130

5.4 Comparison with Experimental Results 131

## 6. DISCUSSION

132
**List of Contents**

6.1 Delaminations in Real Structures 132

6.1.1 Background 132

6.1.1.1 Damage Mechanisms 132

6.1.1.2 Impact Damage Parameters in Plain Laminates 133

6.1.1.3 Impact Damage Parameters in Structures 135

6.1.2 Effect of Impact Location on the Damage Area 136

6.1.3 Effect of Impact Location on the Damage Distribution 137

6.2 Mixed Mode Tests 138

6.2.1 Discussion of Test Method 138

6.2.2 Discussion of Failure Analysis 140

6.2.2.1 Discussion of General Failure Mechanisms 140

6.2.2.2 Discussion of Cusp Angles 141

6.2.3 Discussion of Failure Criteria 142

6.2.4 Effect of Crack Length on Failure Loci 144

6.2.5 Effect of Moisture on Failure Loci 144

6.2.6 Effect of Material Type on Failure Loci 145

6.2.7 Effect of Ply Interface on Failure Loci 145

6.3 Experimental Delamination Studies 147

6.3.1 Discussion of Testing and Analysis 147

6.3.2 Delamination Growth Mechanisms 149

6.3.3 Defects at the 0°/90° [3/4] Ply Interface 150

6.3.4 Defects at the +45°/-45° [5/6] Ply Interface 152

6.3.5 Effect of Ply Interface on Damage Growth 154

6.3.6 Effect of Initial Defect Size on the Damage Growth 155

6.3.7 Effect of Specimen Variation on the Damage Growth 157

6.4 Finite Element Delamination Studies 157

6.4.1 General Aspects of the Model 157

6.4.2 Delamination Development and Mixed Mode Conditions 158

6.4.3 Initiation of Ply Cracking and Delamination 159
List of Contents

6.4.4 Effect of Coupon Data ........................................ 159
6.4.5 Effect of Failure Criterion .............................. 160

7. CONCLUSIONS .................................................. 162

8. IMPLICATIONS AND RECOMMENDATIONS ............. 164

9. ACKNOWLEDGEMENTS ....................................... 168

10. REFERENCES ................................................. 169

11. TABLES .......................................................... 185

12. FIGURES .......................................................... 207

13. APPENDIX A  MMB Specimen Stiffness Calculation .... 297

14. APPENDIX B  Parameters for the Failure Criteria ...... 299

15. APPENDIX C  Design Criteria for Sandwich Panels ...... 305

16. APPENDIX D  Analysis of the Cusp Tilt Angle .......... 306
List of Tables

Table 1-1  Comparison between the mixed-mode test methods
Table 1-2  Mixed-mode failure criteria
Table 2-1  Maximum forces, deflections and damage areas for impacts in the plain and stiffened panels
Table 3-1  Stacking sequences of the preliminary MMB specimens
Table 3-2  Test results from the preliminary MMB specimens
Table 3-3  Predicted engineering properties of the MMB specimens
Table 3-4  Effect of rig geometry on toughness tests conducted at 50% mode I (0°/0° ply interface in T800/5245)
Table 3-5  Validation test results from the 0°/90° ply interface MMB specimens (a=50mm)
Table 3-6  MMB rig geometries for the mixed-mode tests
Table 3-7  Results of the material property tests
Table 3-8  Results of the material flexural property tests
Table 3-9  Comparison between \( G_{IC} \) and \( G_{IIc} \) results and values given in the literature
Table 3-10 MMB test results for unidirectional T800/5245 (DRY) at crack lengths of 40mm and 60mm
Table 3-11 MMB test results for unidirectional T800/5245 (WET) at crack lengths of 40mm and 60mm
Table 3-12 MMB test results for unidirectional T800/924 (DRY) at crack lengths of 40mm and 60mm
Table 3-13 MMB test results for unidirectional T800/924 (WET) at crack lengths of 40mm and 60mm
Table 3-14 MMB test results for 0°/90° ply interface specimens (dry T800/924) at crack lengths of 40mm and 60mm (0°/0° ply interface results given for comparison)
Table 3-15 MMB test results for 0°/90° ply interface specimens (dry T800/924) averaged over all crack lengths (0°/0° ply interface results given for comparison)
Table 3-16 Predicted MMB specimen stiffnesses for 50% mode I tests (0°/0° ply interface for T800/5245)
Table 3-17 Cusp angle \( \beta \) against mixed-mode ratio for T800/5245 (0°/0° ply interface)
List of Tables

Table 3-18  Cusp angle $\beta$ against mixed-mode ratio for T800/924 (0°/0° and 0°/90° ply interfaces)

Table 3-19  Mixed-mode failure criteria given as a function of $G_T$ and $t$, where $t$ is the mixed-mode ratio $G_1/G_{II}$

Table 3-20  Ranking of the failure criteria by $\chi^2$ and $G_{IC}$ for the results from the MMB tests on T800/5245 (0°/0° ply interface)

Table 3-21  Ranking of the failure criteria by $\chi^2$ and $G_{IC}$ for the results from the MMB tests on T800/924 (0°/0° ply interface)

Table 3-22  Ranking of the failure criteria by $\chi^2$ and $G_{IC}$ for all the results from the MMB tests on 0°/0° ply interface specimens

Table 3-23  Ranking of the failure criteria by $\chi^2$ and $G_{IC}$ for the results from the MMB tests on 0°/90° ply interface specimens (dry T800/924)

Table 4-1  Core properties and predicted failure strains for the Nomex and aluminium honeycomb sandwich panels

Table 4-2  Honeycomb sandwich panels containing delaminations

Table 5-1  Model descriptions and equivalent experimental results

Table 5-2  Transverse strain ($\epsilon_{22}$) at node 15001 and difference from continuum (Craighead) result

Table 5-3  Predicted delamination initiation strains for Model #1 (Defect at 0°/90° ply interface)

Table 5-4  Predicted delamination initiation strains for Model #2 (Defect at +45°/-45° ply interface)

Table 5-5  Comparison between predicted and experimental initiation strains

Table 6-1  Cot$^2\beta$ against percentage mode I, where $\beta$ is the cusp angle
List of Figures

Figure 1-1 Typical sources of delamination in composite structures
Figure 1-2 The mechanism for delamination growth
Figure 1-3 Diagram of modes I, II and III (peel, shear and tearing)
Figure 1-4 The through-width delamination (TWD)
Figure 1-5 The embedded delamination
Figure 1-6 A CFRP skin-stiffened panel
Figure 1-7 The double cantilever beam test (DCB)
Figure 1-8 The end loaded split test (ELS)
Figure 1-9 The end notched flexure test (ENF)
Figure 1-10 The mixed-mode flexure test (MMF)
Figure 1-11 The variable mixed-mode test (VMM)
Figure 1-12 The mixed-mode bending test (MMB)
Figure 1-13 The symmetrically cracked laminate test (SCL)
Figure 1-14 The cracked lap shear test (CLS)
Figure 1-15 The edge delamination test (EDT)
Figure 1-16 The Arcan test (ARCAN)
Figure 1-17 The off-axis tension test (OAT)
Figure 2-1 Dimensions of the skin-stiffened panel
Figure 2-2 Impact locations on the skin-stiffened panel
Figure 2-3 Ultrasonic scans of damage caused by 15J impact on a plain panel
Figure 2-4 Ultrasonic scan of the entire skin-stiffened panel after impacting
Figure 2-5 Ultrasonic scans of damage at impact sites A to D
Figure 2-6 Variation in damage area versus distance from the stiffener centreline
Figure 2-7 Variation in peak impact force versus damage area
Figure 2-8 Variation in peak impact displacement versus damage area
Figure 2-9 Through-thickness damage distribution for an impact in a plain panel and
the bay (site A)
Figure 2-10 Through-thickness damage distribution for impacts close to the foot (site
B), over the foot (site C) and over the stringer centreline (site D)
Figure 2-11 A pair of matching fracture surfaces at impact site A (15J), in the centre
of a bay
Figure 3-1 Diagram of the Arcan loading rig
List of Figures

Figure 3-2 Fracture surfaces of a +45°/-45° ply interface specimen failed under pure mode I loading (x3)
Figure 3-3 Fracture surfaces of a 0°/90° ply interface specimen failed under 35% mode I loading (x3)
Figure 3-4 Dimensions of the insert used for manufacture of the preliminary modified MMB specimens
Figure 3-5 Dimensions of the (a) 0°/0°, (b) f1/f2 and (c) 0°/90° ply interface MMB specimens
Figure 3-6 Diagram and photograph of the MMB loading rig
Figure 3-7 Fracture toughness (G_T) versus crack length for the preliminary 100% mode I tests (DCB) for different ply interfaces
Figure 3-8 Matching fracture surfaces of a MMB 0°/90° ply interface specimen tested at 100% mode I (x3)
Figure 3-9 Matching fracture surfaces of a preliminary MMB +45°/-45° ply interface specimen tested at 100% mode I (x3)
Figure 3-10 Fracture toughness (G_T) versus crack length for the preliminary 68% mode I tests (MMB) for different ply interfaces
Figure 3-11 Micrograph of the fracture surface close to the insert of a preliminary MMB 0°/90° ply interface specimen tested at 68% mode I (x19, 55° tilt)
Figure 3-12 Micrograph of islands of resin failure on the fracture surface of a preliminary MMB 0°/+45° ply interface specimen tested at 0% mode I (x200, 30° tilt)
Figure 3-13 The stress field at the delamination tip.
Figure 3-14 Crack growth at 0°/0° and 0°/ϕ° ply interfaces illustrating crack migration mechanism
Figure 3-15a G_I versus G_II for dry T800/5245 at crack lengths of 40mm and 60mm
Figure 3-15b G_I versus G_II for wet T800/5245 at crack lengths of 40mm and 60mm
Figure 3-16a G_I versus G_II for dry T800/924 at crack lengths of 40mm and 60mm
Figure 3-16b G_I versus G_II for wet T800/924 at crack lengths of 40mm and 60mm
Figure 3-17 Effect of crack length on averaged G_I versus G_II for unidirectional T800/5245 and T800/924
Figure 3-18 Effect of material on averaged G_I versus G_II for dry and wet unidirectional laminates
Figure 3-19 Examples of G_T versus crack length for dry T800/5245 and T800/924 tested at 75% mode I
Figure 3-20 Percentage R-curve versus percentage mode I for T800/5245 and T800/924
**List of Figures**

<table>
<thead>
<tr>
<th>Figure</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>3-21</td>
<td>Fracture toughness ($G_T$) versus crack length for the 87.5%, 75% and 50%</td>
</tr>
<tr>
<td></td>
<td>mode I tests for dry T800/924 0°/90° (blue) and 0°/0° (red) ply interfaces</td>
</tr>
<tr>
<td>3-22</td>
<td>$G_I$ versus $G_{II}$ for dry T800/924 (0°/0° and 0°/90° ply interfaces)</td>
</tr>
<tr>
<td></td>
<td>averaged over all crack lengths</td>
</tr>
<tr>
<td>3-23</td>
<td>Low magnification micrographs of fracture surfaces generated at 100%,</td>
</tr>
<tr>
<td></td>
<td>75%, 50%, 25% and 0% mode I loading for dry T800/5245 (x1000)</td>
</tr>
<tr>
<td>3-24</td>
<td>Low magnification micrographs of fracture surfaces generated at 100%,</td>
</tr>
<tr>
<td></td>
<td>75%, 50%, 25% and 0% mode I loading for dry T800/924 (x1000)</td>
</tr>
<tr>
<td>3-25</td>
<td>High magnification micrographs of fracture surfaces generated at 75%,</td>
</tr>
<tr>
<td></td>
<td>50%, 25% and 0% mode I loading for dry T800/5245 (x5000)</td>
</tr>
<tr>
<td>3-26</td>
<td>High magnification micrographs of fracture surfaces generated at 75%,</td>
</tr>
<tr>
<td></td>
<td>50%, 25% and 0% mode I loading for dry T800/924 (x5000)</td>
</tr>
<tr>
<td>3-27</td>
<td>Low magnification micrographs of fracture surfaces generated at 87.5%,</td>
</tr>
<tr>
<td></td>
<td>75%, 50%, 25% and 0% mode I loading for dry 0°/90° T800/924 (x1000)</td>
</tr>
<tr>
<td>3-28</td>
<td>High magnification micrographs of fracture surfaces generated at 75%,</td>
</tr>
<tr>
<td></td>
<td>50%, 25% and 0% mode I loading for dry 0°/90° T800/924 (x5000)</td>
</tr>
<tr>
<td>3-29</td>
<td>Micrograph of a typical cusp illustrating variety of tilt angles which</td>
</tr>
<tr>
<td></td>
<td>could be chosen (x4100, 35° tilt)</td>
</tr>
<tr>
<td>3-30</td>
<td>Failure criteria versus summed rank in terms of $x^2$ (red) and $G_{IC}$</td>
</tr>
<tr>
<td></td>
<td>(blue) for 0°/0° ply interfaces in T800/5245, T800/924 and both materials</td>
</tr>
<tr>
<td></td>
<td>overall</td>
</tr>
<tr>
<td>4-1</td>
<td>Design and strain gauge positions on the Nomex honeycomb sandwich panel</td>
</tr>
<tr>
<td>4-2</td>
<td>Design and strain gauge positions on the aluminium honeycomb sandwich panel</td>
</tr>
<tr>
<td>4-3</td>
<td>Front skin of the aluminium honeycomb sandwich panel after failure</td>
</tr>
<tr>
<td>4-4</td>
<td>The Moiré grating fixture</td>
</tr>
<tr>
<td>4-5</td>
<td>Illustration of the instrumentation for monitoring the Moiré fringes</td>
</tr>
<tr>
<td>4-6</td>
<td>Photograph of a honeycomb sandwich panel in the test machine (panel I)</td>
</tr>
<tr>
<td>4-7</td>
<td>Development of the damage growth during testing of panel B (50mm</td>
</tr>
<tr>
<td></td>
<td>diameter circular insert at a 0°/90° [3/4] ply interface)</td>
</tr>
<tr>
<td>4-8</td>
<td>Development of the damage growth during testing of panel D (50mm x</td>
</tr>
<tr>
<td></td>
<td>71mm elliptical insert at a 0°/90° [3/4] ply interface)</td>
</tr>
<tr>
<td>4-9</td>
<td>Variation in damage height with applied strain for panels A, B, C, D and</td>
</tr>
<tr>
<td></td>
<td>I (inserts at a 0°/90° [3/4] ply interface)</td>
</tr>
<tr>
<td>4-10</td>
<td>Increase in lateral damage extent (width) with applied strain for panels A,</td>
</tr>
<tr>
<td></td>
<td>B, C, D and I (inserts at a 0°/90° [3/4] ply interface)</td>
</tr>
<tr>
<td>4-11</td>
<td>Development of the damage growth during testing of panel F (50mm</td>
</tr>
<tr>
<td></td>
<td>diameter circular insert at a +45°/-45° [5/6] ply interface)</td>
</tr>
</tbody>
</table>
List of Figures

Figure 4-12 Development of the damage growth during testing of panel H (50mm x 71mm elliptical insert at a +45°/-45° [5/6] ply interface)

Figure 4-13 Variation in damage height with applied strain for panels E, F, G and H (inserts at a +45°/-45° [5/6] ply interface)

Figure 4-14 Increase in transverse damage extent (width) with applied strain for panels E, F, G and H (inserts at a +45°/-45° [5/6] ply interface)

Figure 4-15 Damage width versus applied strain for 50mm circular defects at the 0°/90° [3/4] and +45°/-45° [5/6] ply interfaces

Figure 4-16 Damage width versus applied strain for 50x71mm elliptical defects at the 0°/90° [3/4] and +45°/-45° [5/6] ply interfaces

Figure 4-17 Damage height versus applied strain for 50mm circular defects at the 0°/90° [3/4] and +45°/-45° [5/6] ply interfaces

Figure 4-18 Damage height versus applied strain for 50x71mm elliptical defects at the 0°/90° [3/4] and +45°/-45° [5/6] ply interfaces

Figure 4-19 Lower fracture surface of the damage growth in panel I (50mm diameter circular insert at the 0°/90° [3/4] ply interface)

Figure 4-20 Lower fracture surface of the damage growth in panel C (35x50mm elliptical insert at the 0°/90° [3/4] ply interface)

Figure 4-21 Simplified diagram of the lower fracture surface for the damage growth in panel I (50mm diameter circular insert at the 0°/90° [3/4] ply interface)

Figure 4-22 Micrograph locations for the upper fracture surface (left side) of the damage growth in panel I (50mm diameter circular insert at the 0°/90° [3/4] ply interface)

Figure 4-23 Micrograph of the type (i) fracture (0°/90° [3/4] ply interface) at site (1) in Figure 4-22, at the longitudinal boundary of the insert (x400, 7° tilt)

Figure 4-24 Micrograph of the type (i) fracture (0°/90° [3/4] ply interface) at site (2) in Figure 4-22, at the lateral boundary of the insert (x500, 30° tilt)

Figure 4-25 Micrograph of the type (i) fracture (0°/90° [3/4] ply interface) at site (3) in Figure 4-22, showing 0° splitting above the insert (x40, 30° tilt)

Figure 4-26 Micrograph of the type (ii) fracture (-45°/0° [2/3] ply interface) at site (4) in Figure 4-22, showing fracture near the insert (x700, 30° tilt)

Figure 4-27 Micrograph of the type (ii) fracture (-45°/0° [2/3] ply interface) at site (5) in Figure 4-22, showing fracture away from insert (x1000, 25° tilt)

Figure 4-28 Micrograph at site (6) in Figure 4-22, showing -45° ply splitting at the boundary between type (ii) and (iii) surfaces (x22, 30° tilt)

Figure 4-29 Micrograph of fibre shear failure of ply 2 (-45°) along line A-A adjacent to the type (iii) fracture (+45°/-45° [1/2] ply interface) at site (7) in Figure 4-22 (x24, 40° tilt)
Figure 4-30  Micrograph of bundles of sheared fibres from ply 2 (-45°) along line A-A adjacent to the type (iii) fracture (+45°/-45° [1/2] ply interface) at site (7) in Figure 4-22 (x200, 60° tilt)

Figure 4-31  Distribution of mixed-mode fracture for the damage growth in panel I (50mm diameter circular insert at the 0°/90° [3/4] ply interface) showing the percentage of mode I failure at different sites.

Figure 4-32  Distribution of mixed-mode fracture for the damage growth in panel C (35x50mm elliptical insert at the 0°/90° [3/4] ply interface) after the delaminated 0° ply had been removed, showing the percentage of mode I failure at different sites.

Figure 4-33  Lower fracture surface of the damage growth in panel E (35mm diameter circular insert at the +45°/-45° [5/6] ply interface)

Figure 4-34  Lower fracture surface of the damage growth in panel G (35x50mm elliptical insert at the +45°/-45° [5/6] ply interface)

Figure 4-35  Simplified diagram of the lower fracture surface of the damage growth in panel E (35mm diameter circular insert at the +45°/-45° [5/6] ply interface)

Figure 4-36  Upper fracture surface (left side) of the damage growth in panel E (35mm diameter circular insert at the +45°/-45° [5/6] ply interface)

Figure 4-37  Upper fracture surface (right side) of the damage growth in panel E (35mm diameter circular insert at the +45°/-45° [5/6] ply interface) with the delaminated ply 4 (90°) removed

Figure 4-38  Simplified diagram of the lower fracture surface of the damage growth in panel E (35mm diameter circular insert at the +45°/-45° [5/6] ply interface) with the delaminated ply 4 (90°) removed

Figure 4-39  Micrograph locations for the upper fracture surface of the damage growth in panel E (35mm diameter circular insert at the +45°/-45° [5/6] ply interface)

Figure 4-40  Micrograph of the type (iv) fracture (+45°/-45° [5/6] ply interface) at site (1) in Figure 4-39, showing +45° ply splitting and resin cleavage at the insert boundary (x67, 45° tilt)

Figure 4-41  Micrograph of the type (iv) fracture (+45°/-45° [5/6] ply interface) at site (2) in Figure 4-39, showing shallows shear cusps (x700, 55° tilt)

Figure 4-42  Micrograph of the type (v) fracture (90°/+45° [4/5] ply interface) at site (3) in Figure 4-39, showing a shear boundary near the insert (x70, 35° tilt)

Figure 4-43  Micrograph of the type (v) fracture (90°/+45° [4/5] ply interface) at site (4) in Figure 4-39, showing compressive fibre fracture of ply 5 (110x, 60° tilt)
List of Figures

Figure 4-44 Micrograph of the type (vi) fracture (0°/90° [3/4] ply interface) at site (5) in Figure 4-39, showing damage growth from a 90° ply split (x300, 25° tilt)

Figure 4-45 Micrograph of the type (vi) fracture (0°/90° [3/4] ply interface) at site (6) in Figure 4-39, away from the initiation site (x500, 30° tilt)

Figure 4-46 Distribution of mixed-mode fracture for the damage growth in panel E (35mm diameter circular insert at the +45°/-45° [5/6] ply interface) showing the percentage of mode I failure.

Figure 4-47 Distribution of mixed-mode fracture for the damage growth in panel G (35x50mm elliptical insert at the +45°/-45° [5/6] ply interface) showing the percentage of mode I failure at different sites.

Figure 5-1 Mesh geometry for the delamination models

Figure 5-2 Flow chart for the Virtual Crack Closure (VCC) calculation

Figure 5-3 Schematic diagram showing the variables used in the calculation of $G_1$

Figure 5-4 Schematic diagram showing the variables used in the calculation of $G_{II}$

Figure 5-5 Blister shape (out-of-plane deflection) versus applied strain prior to initiation of delamination growth in Model #1 (defect at 0°/90° ply interface)

Figure 5-6 Magnitude of the mode I and II components around the boundary prior to initiation of delamination growth in Model #1 (defect at 0°/90° ply interface)

Figure 5-7 Magnitude of $\varepsilon_{22}$ in the delamination prior to initiation of delamination growth in Model #1 (defect at 0°/90° ply interface)

Figure 5-8 Blister shape (out-of-plane deflection) versus applied strain prior to initiation of delamination growth in Model #2 (defect at +45°/-45° ply interface)

Figure 5-9 Magnitude of the mode I and II components around the boundary prior to initiation of delamination growth in Model #2 (defect at +45°/-45° ply interface)

Figure 5-10 Magnitude of $\varepsilon_{22}$ in the delamination prior to initiation of delamination growth in Model #2 (defect at +45°/-45° ply interface)

Figure 5-11 Comparison of blister shape and height between experiments and models for defects at 0°/90° and +45°/-45° ply interfaces

Figure 5-12 Comparison of matrix splitting at the delamination plane between experiments and models for defects at 0°/90° and +45°/-45° ply interfaces

Figure 6-1 Profile of the interply zone thickness in 0°/0° and 0°/90° ply interfaces; (a) and (b) respectively

Figure 6-2 Mechanism for rib formation at 0°/90° ply interfaces under mixed-mode loading
List of Figures

Figure 6-3  Loading conditions at the delamination boundary
Figure 6-4  A fracture surface from damage growth in a panel containing an insert at the $0^\circ/90^\circ [3/4]$ ply interface
Figure 6-5  Sequence of failure for damage growth from the inserts at the $0^\circ/90^\circ [3/4]$ ply interfaces
Figure 6-6  Sequence of failure for damage growth from the inserts at the $+45^\circ/-45^\circ [5/6]$ ply interfaces
1. Introduction

1.1 Background

1.1.1 Delamination - Causes and Effects

Carbon-fibre composite structures are used in aircraft because of their high specific strength and modulus compared with conventional structures. However, the full potential of composites has been impeded by their limited resistance to damage initiation and growth\(^{1,2,3,4}\). Delamination is of particular concern since this can lead to large reductions in compressive properties\(^5\), particularly in aerospace structures where compressive performance is critical\(^{1,2,6}\). Delaminations can initiate at structural features (Figure 1-1), such as holes\(^7\), ply-drop-offs\(^4\) and edges\(^4,8\) or from damage such as low velocity impact and included defects\(^1,4,9,10,11\). The subsequent delamination growth from these features not only reduces strength but can also lead to exposure of the load bearing plies to environmental attack.

1.1.2 Delamination Mechanisms

The general mechanism by which delaminations initiate and grow is shown in Figure 1-2. Consider a laminate containing delaminated plies which is loaded in uniaxial compression (Figure 1-2a). At a critical load, the lack of out-of-plane support on the delaminated plies leads to buckling of these plies (Figure 1-2b) forming a delamination blister. These plies cease to carry any significant further load; increases in load are shed onto the base laminate. Usually, the unbalanced lay-up of the delaminated plies leads to complex deformations and the out-of-plane deflection of the delaminated plies generates a combination of peel (mode I), shear (mode II) and tear (mode III) forces (Figure 1-3) at the defect boundary. If these forces exceed the material strength, delamination growth will initiate (Figure 1-2c). The delamination will then continue to grow, either stably or unstably, shedding further load onto the base laminate. When the applied stress on the base laminate exceeds the residual strength, catastrophic compression or local bending failure of the load bearing material occurs (Figure 1-2d).
1.2 Aims of the Work

1.2.1 Discussion

There have been numerous experimental and analytical studies of delamination behaviour (Section 1.3). The experimental work encompasses observing the buckling of the delaminated plies, initiation of damage growth and the subsequent growth up to catastrophic failure. However, characterisation of the failure mechanisms at the delamination boundary has been limited. The analytical work has predicted buckling of the delaminated plies and characterised the loading conditions around the delamination boundary. Although delamination growth has been predicted using mixed-mode failure criteria, there is debate as to which criterion to use. To date, most of these criteria are empirical with little physical basis; consequently, there has only been limited success in predicting delamination growth. Also, modelling of delamination growth is very processor intensive, so only recently have the tools been available to fully model delamination.

There has been limited success in relating the experimental and analytical approaches; a major stumbling block has been the choice of delamination growth criterion, which is due to a lack of understanding of the detailed failure processes at the delamination boundary. The best means to address this problem is to dissect the delamination blister after test and examine the fracture surfaces; i.e. fractographic analysis.

1.2.2 Programme Aims

The main theme of the work reported here is to develop a link between the experimental and analytical approaches by investigating delamination growth through fractographic analysis. There were three main aims.

(i) Characterise and understand the loading conditions and failure processes at realistic delaminations through examination of the fracture surfaces using, as a reference, surfaces generated under controlled loading conditions.

(ii) Use the results of (i) to validate predicted loading conditions at the delamination boundary in a finite element model.
(iii) Characterise delamination failure loci using mixed-mode coupon tests and apply, via a failure criterion, to finite element models to predict delamination growth.

For the experimental aspect of this programme, compression tests were conducted on panels containing embedded delaminations (Chapter 4); the defect dimensions were chosen to be similar to those found in service. To simplify the tests, the delaminated panels were designed to eliminate global buckling. Prior to this, delamination growth in coupons was characterised to generate mixed-mode failure criteria and to identify the fracture morphology (Chapter 3).

In the analytical aspect of this work (Chapter 5), a finite element model of an embedded delamination was developed although its scope was limited; the model was confined to prediction of initiation of delamination growth.

To improve the applicability of this programme to real components, two carbon-fibre/epoxy materials currently in use in aerospace structures were chosen. The primary material was Hexcel T800/924 which is a second generation toughened epoxy system. The secondary material was BASF T800/5245 which is a bismalimide/epoxy mix. The two systems have the same fibre type (Toray T800) which made stiffness dominated properties (e.g. delamination buckling) similar. However, the matrices were very different which would allow a comparison between resin dominated properties.

The programme was split into three phases, each of which is now discussed.

(i) Characterisation of impact damage in stringer-stiffened panels
(ii) Mixed-mode delamination toughness testing
(iii) Embedded delaminations in plain panels

**1.2.3 Characterisation of Impact Damage in Stringer-Stiffened Panels**

Before beginning a detailed study of delamination growth, realistic damage states were identified. The current literature was briefly reviewed and a study undertaken using a stiffened panel; this was impacted at various locations, such as over a stringer, adjacent to a stringer or over the inter-stringer bay. The panel design and impact energies were typical
of those in service. The damage was examined using non-destructive techniques (ultrasonics) and optical microscopy, with the aim of identifying realistic delamination sizes, shapes and locations.

1.2.4 Mixed-Mode Delamination Toughness Testing

A wide range of mixed-mode tests have been developed and these were surveyed to choose which was the most applicable for generating fracture surfaces and producing failure criteria. Only mixed-mode (I/II) tests were surveyed since mode III was considered to be negligible for the type of delaminations under investigation (thin-film). The literature on delamination fracture surface morphology and failure criteria were also reviewed. A mixed-mode test was chosen for further investigation and tests were conducted. To date, most toughness testing had been conducted on 0°/90° ply interfaces\textsuperscript{12}. However, in real structures delaminations usually occur at non-zero ply interfaces\textsuperscript{6,8,13} and therefore mixed-mode tests were conducted to characterise such interfaces. The tests were quasi-static and were conducted at room temperature on both dry and moisture conditioned laminates. Mixed-mode delamination loci were generated and the fracture surfaces characterised using scanning electron microscopy. Using non-linear regression techniques\textsuperscript{14} the results were fitted to the criteria cited in the literature, allowing the most suitable (i.e. best fit) to be chosen.

1.2.5 Embedded Delaminations in Plain Panels

A specimen was developed to investigate delamination growth from artificial disbonds placed between the plies. Many workers have used anti-buckling fixtures\textsuperscript{8,15} to stabilise plain panels during delamination growth but this can be problematic. Work using honeycomb sandwich panels\textsuperscript{16} has proved to be quite successful, so this type of specimen was developed. Moiré fringe interferometry was used to monitor the initiation and growth of the delaminations during quasi-static compressive loading. Finally, the delamination surfaces were examined, referring to the results of the previous phase to ascertain the local loading conditions at the delamination boundary.

In parallel with the experimental work, models of embedded delaminations were developed using the HKS ABAQUS finite element code. Only thin-film models were
developed since this was considered to be the most critical delamination case in compressively loaded structures$^{17,18,19}$. The models were developed to predict the buckling loads and mode shapes of the delaminated plies and included anisotropic effects, such as stretch-bend coupling and the contact forces. The virtual crack closure (VCC) technique was used to partition these boundary forces into opening and sliding components, giving the distribution of $G_1$ and $G_{11}$ around the delamination perimeter. Using these results in conjunction with the criteria generated in the earlier phase allowed initiation of delamination growth to be predicted. The predictive models were then be compared with the experimental results.

1.2.6 New and Unique Aspects

There are a number of aspects of the work reported here which are unique. The findings of this work have already contributed to the thinking of other authors$^{5,20,21,22,23}$. In addition, parts of this work have been expanded upon and published elsewhere$^{24,25,26,27,28,29,30}$.

Firstly, impact damage in composite structures (Chapter 2) has not been previously studied in the detail given here. Extensive work has been done to characterise impact damage in plain coupons (as described in Section 6.1.1) but little has been done to study such damage in realistic structures. In particular, the ultrasonic and optical characterisation of the damage state at structural features is unique.

When this study began there was no accepted method to characterise mixed-mode delamination and the work in this area was patchy (Section 1.4). However, recently there has been much activity, particularly using the test method (MMB) used here to characterise the delamination toughness (Chapter 3). This test method is now recognised as the most appropriate for characterising toughness and is widely used. However, there are a number of important aspects of the work described in Chapter 3 which are unique. The philosophy of relating the delamination failure loci to the fracture morphology is unusual, particularly for non-zero ply interfaces. Consequently, an important finding was that the loading conditions (mixed-mode ratio) can be gleaned from the fracture
morphology, which has important implications for post-mortem analysis of failed components.

The majority of the previous work on MMB testing has used unidirectional specimens and most of the testing reported here studied this ply interface. However, a significant amount of testing was conducted on non-zero ply interfaces, in particular 0°/90°. All previous work investigating the toughness of such interfaces has encountered difficulties with crack migration, invalidating the results. In this work, delaminations were successfully propagated within a non-zero ply interface, and the cause of the invalid failures from the previous studies was identified.

The understanding of delamination growth developed from the coupon testing (Chapter 3) lead to important and unique findings in Chapters 4 and 5 in which embedded delaminations were investigated. Although studying embedded defects was not new, the use of a honeycomb stabilised panel rather than an anti-buckling guide was unusual.

A key aspect of this work was the understanding developed of the damage mechanisms at embedded defects through fractographic analysis (Chapter 4). Based on the findings of Chapter 3 (fracture toughness testing), the loading conditions around the delamination boundary were characterised and the detailed mechanisms by which the damage grew were explained. A key finding was that a simple defect (single plane disc) could lead to complicated damage mechanisms such as crack plane migration, ply splitting and even fibre fracture. This has important implications for modelling of delamination growth since all previous work has assumed damage growth from a single plane defect will remain within the same plane. The results demonstrated a direct link between the growth mechanisms identified in the coupon testing (Chapter 3) and those at embedded defects (Chapter 4). This work also showed that, to fully characterise delamination growth, only failure loci from 0°/90° and 0°/45° ply interfaces are required, negating the requirement to conduct coupon tests on laminates containing interfaces such as +45°/-45°. Consequently, from understanding the damage growth
mechanisms, simple rules for delamination growth from embedded defects were derived. These lead to general design rules for damage tolerant structures.

The modelling conducted (Chapter 5) was relatively limited but did substantiate the findings of the experiments (Chapter 4). Models of single plane embedded delaminations predicted mechanisms such as ply splitting and led to recommendations for realistic modelling of delamination growth.

Finally, another unique aspect of this work was the comprehensive review and use of a wide range of delamination failure criteria. These were fitted to the experimental coupon data (Chapter 3) and then used to predict delamination growth in the finite element models (Chapter 5). This work has demonstrated that the choice of failure criteria can be important, since they can have a significant effect on the predicted delamination initiation strains.

1.3 Delamination Literature Survey

1.3.1 Introduction

To review what is currently understood about delamination initiation and growth in carbon fibre composites, a literature survey was undertaken. The survey concentrated on the delaminations in thin-skinned stabilised structures, as used in aerospace components. This survey was split into three Sections:

(i) Through-Width Delaminations (TWD)
(ii) Embedded Delaminations
(iii) Delaminations in Stiffened Structures

The first Section considered the simplest model proposed; an interlaminar disbond which extends across the entire width of a flat laminate (Figure 1-4). Because it is simple to analyse, the through-width delamination (TWD) is a convenient vehicle for developing models and understanding growth mechanisms. However, it has proved to be an oversimplification and the second Section considered the more realistic case of the embedded delamination, where the disbond is entirely enclosed (Figure 1-5). However in service, plain laminates are rare and components often contain structural features. A
common feature is stringers which improve the stability of a structure (Figure 1-6). Therefore, in the final Section, work on delaminations within such structures is reviewed.

1.3.2 Through-Width Delaminations

Early analyses of the TWD used simple isotropic models\textsuperscript{17,31}. Chai\textsuperscript{31} developed a beam-column analysis and found that the critical case was when the delamination was close to one surface; the ‘thin-film’ case. The TWD has also been extensively studied by Whitcomb et al\textsuperscript{32,33,34}. In early work\textsuperscript{32} a Teflon\textcopyright strip was sandwiched between a unidirectional laminate and an aluminium plate. The depth and length of the strip were varied and the specimen tested under both quasi-static and cyclic loading. Later work was conducted on multidirectional laminates containing delaminations at \(0^\circ/0^\circ\) ply interfaces\textsuperscript{33}. Whitcomb originally analysed the specimens using a geometrically non-linear finite element (FE) analysis and showed that an energy based analysis was more suitable than a strength based approach. This was due to the complexity of the stress state at the delamination boundary which led to the latter approach being highly mesh sensitive. Whitcomb used the virtual crack closure (VCC) method to partition the total energy release rate \((G_T)\) into mode I \((G_1)\), mode II \((G_{II})\) and mode III \((G_{III})\) components (Figure 1-3); a detailed description of the VCC technique is given elsewhere\textsuperscript{3,35}. Some authors have developed simplified methods, using linear finite element analysis with non-linear beam theory to develop closed-form solutions to predict the loading at the delamination boundary\textsuperscript{33,34,36,37}.

Material anisotropy is important in TWDs\textsuperscript{17}; in particular the unbalanced stacking sequence of the delaminated plies leads to stretching-shearing coupling\textsuperscript{38}. The local ply orientation at the delamination boundary controls the magnitudes of \(G_1\), \(G_{II}\) and \(G_{III}\)\textsuperscript{38} and subsequently the delamination initiation strain.

In general, the work done using the TWD has shown that the mode I and mode II components \((G_1\) and \(G_{II}\)) at the boundary were very sensitive to initial deflection, stacking sequence, delamination size and depth. Although, \(G_1\) and \(G_{II}\) increased with load, beyond a critical load \(G_1\) falls\textsuperscript{34}. Consequently, the mixed-mode ratio \((G_1/G_{II})\) at the delamination
Introduction

boundary is strongly dependent on the applied strain\textsuperscript{34}. For short or deep delaminations, the delamination buckling and initiation strains were similar and the subsequent growth was usually unstable. In such cases, $G_\text{i}$ was greater than $G_{\text{ll}}$\textsuperscript{34,37}, Whitcomb concluding that the $G_\text{i}$ component controlled initiation of delamination growth\textsuperscript{32}. However, for large or shallow disbonds, the buckling strain was much lower than the delamination initiation strain\textsuperscript{36,39} and the critical load was dependent on $G_{\text{II}}$\textsuperscript{34,37}. Whitcomb\textsuperscript{33,34} noted that the growth criterion had a very large effect on the predicted initiation strain, although this conflicted with the findings of Donaldson\textsuperscript{36}.

1.3.3 Embedded Delaminations

Although the TWD is relatively simple to analyse, a more realistic model is the embedded delamination. In service, delaminations may occur at any location through the thickness of the laminate but the most commonly studied case is the thin film delamination. This has similarities with impact damage\textsuperscript{17,40,41} where the largest disbonds can be close to one face\textsuperscript{9,42,43}. There have been studies on mid-plane delaminations, particularly under bending loads\textsuperscript{44,45}, but the thin film case is the most critical\textsuperscript{17,18,24,40} since the delaminated plies buckle at relatively low loads. Furthermore, current aircraft structures are designed not to buckle below limit load and consequently generally experience in-plane compressive rather than bending loadings. In the thin-film case it is assumed that the behaviour of the sublaminate beneath the disbond does not affect the response of the blister. This is a valid assumption when the entire laminate global buckling strain exceeds the delamination buckling strain by a factor of two\textsuperscript{20,24,46}. However, in thin-skinned structures, this may not be the case, and there can be an interaction between the local buckling of the delaminated plies and global buckling of the entire laminate.

As with the TWD, the early work on embedded delaminations assumed isotropic properties and studied the effect of delamination on the residual strength\textsuperscript{18}. The analysis was split into two stages. Firstly, the buckling load and mode shape of the delaminated plies were predicted, from which the forces at the defect boundary were determined. This led to the second stage; prediction of initiation of delamination growth\textsuperscript{17,19}.
Local buckling of the delaminated plies is critical since restricting this mechanism leads to large increases in residual strength\textsuperscript{18,20,46}. Early work on predicting delamination buckling treated the laminate as isotropic and elastic\textsuperscript{17,18,19,47} which was relatively successful for small delaminations. This work studied the effect of disbond depth, aspect ratio, size, material properties and ply interface on delamination behaviour. Generally, small or deep delaminations buckled at high strains, if at all. However, large or shallow delaminations buckled at low strains.

To model large delaminations accurately, anisotropy should be included, which introduces stiffness coupling, and complicates the analysis\textsuperscript{12,18,41}. Also, the hygrothermal and residual stresses can lead to buckling of the delaminated plies even before any load is applied\textsuperscript{8,18}. Both Webster\textsuperscript{18} and Davidson\textsuperscript{48} employed a reduced bending stiffness to account for the coupling but this gave conservative results.

Some workers have developed closed-form analyses for predicting buckling of anisotropic delaminations\textsuperscript{16,17,19,20,46,47} but the most successful methods have used two-dimensional (2D)\textsuperscript{1,3,20,40,49} or three-dimensional (3D)\textsuperscript{1,17,24,40,44,50,51,52,53} finite element models. Furthermore, since the deflections of the delaminated plies often exceed the laminate thickness, non-linear models were usually required\textsuperscript{24,44}. Accurate prediction of the delamination blister shape prior to initiation of growth has been achieved by a number of authors\textsuperscript{1,20,24,40,44}.

The experimental studies of delamination buckling have highlighted some interesting trends\textsuperscript{15}. Firstly, the ambient air pressure can inhibit delamination buckling\textsuperscript{8,16,20,44} although this can be overcome by drilling a small hole to equalise the pressure between the atmosphere and inside the delamination. In addition, the shape of the delamination blister is not always the same as that of the initial defect\textsuperscript{1,8,40,41}. Small or horizontally aligned disbonds usually form a single delamination blister which is relatively simple to model\textsuperscript{40,41}. However, at large or vertically aligned disbonds, contact can occur before delamination growth, leading to reaction forces between the delaminated plies and the base laminate\textsuperscript{1,41,54}. These contact forces are attributed to material anisotropy and
significantly change the loading conditions at the boundary\textsuperscript{16,54,55}, greatly affecting the delamination growth and complicating the analysis\textsuperscript{3,20,40,48,50,54,56}.

After delamination bucking, the next stages are initiation and growth of the delaminations. Experimental studies have used a variety of techniques to detect delamination growth\textsuperscript{8,16,18,40} such as strain gauges\textsuperscript{16,46,57}, ultrasونics\textsuperscript{20,24,56} and acoustic emission\textsuperscript{20,24,44,46,56,58}, but the most successful has been shadow Moiré interferometry\textsuperscript{8,12,15,24,41,59}, a full description of which is given elsewhere\textsuperscript{15,59}. Moiré interferometry allows real-time detection of surface features and out-of-plane deflections. It is simple to use, relatively cheap and is non-contacting. However, it is important to prepare the surface of the laminate prior to testing and the ambient lighting during a test needs to be controlled. A further problem is that only out-of-plane deformations are monitored and any delamination growth due to pure shear (mode II) cannot be detected. In the experimental studies, for small or deep delaminations, growth initiated at high strains (occasionally at global buckling of the whole laminate\textsuperscript{20,40,41}) and was usually unstable, leading to catastrophic failure\textsuperscript{19}. However, as the size increased or the depth decreased, the delamination initiation strain fell\textsuperscript{1,19,20,40,53} although the buckle shape could complicate this trend\textsuperscript{8,15}. Initially, growth was stable but could become unstable leading to catastrophic failure. Generally, large or shallow delaminations gave greater reductions in strength than small or deep delaminations\textsuperscript{1,3,20,40}.

The analytical methods used to predict delamination growth have considered the forces at the delamination boundary and employed a criterion to predict initiation of growth. Early attempts\textsuperscript{10} used material strength based criteria but, as with TWDs (Section 1.3.2), criteria based on the energy change associated with delamination growth were more suitable\textsuperscript{2,34}. Early studies assumed self-similar growth (\textit{i.e.} a constant delamination aspect ratio) and considered the average strain energy release rate, $G_T$, around the delamination boundary\textsuperscript{33,47}. However, when anisotropic effects were fully accounted for, the distribution of $G_T$ around the perimeter was not uniform and was strongly dependent on the loading direction and ply orientations\textsuperscript{1,3,19,40,41,50}. 
Some workers have simplified the analysis by assuming that either $G_I$ or $G_{II}$ is zero; *i.e.* the delamination growth is pure mode II or pure mode I respectively\(^{34,47,12}\). Initiation of growth is defined when $G_T$ exceeds $G_{IC}$ or $G_{HC}$. Generally this approach is not valid since delamination growth is a mixture of modes\(^{8,13,50}\), and to fully model the growth processes, the $G_T$ has to be partitioned into mode I, mode II and mode III components. This has often been done using the VCC technique\(^{12,24,44,49,50,51}\). Growth is usually simulated by releasing nodes but recently a number of authors have used the ‘moving’ or ‘adaptive’ mesh approach\(^{20,24,56}\), where the mesh geometry is modified to simulate growth. The mixed-mode ratio ($G_I/G_{II}$) varies around the boundary\(^{3,8,40,50,52}\) and the locations of the pure mode maxima are dependent on delamination shape and applied strain\(^{50}\), although for the critical thin film case $G_{III}$ is negligible\(^{3,44,50}\). Very little work has been done to identify the detailed failure mechanisms at the delamination boundary and that which has been done indicates that these mechanisms are quite complex\(^{1,8,51,41}\). Delamination growth from a single plane defect can result in multiplane delamination and matrix cracking\(^{1,8,24,41,53}\), and it has been highlighted that the governing mechanisms need to be understood\(^{1,24}\). Delamination growth was usually transverse to the loading direction\(^{3,12,15,24,41,44,56,58}\), although this was dependent on the local ply orientations\(^{8,19,24,60}\).

Once the distribution of the mode I and II components is known, a growth criterion is applied to predict initiation of delamination growth. There has been some debate as to which criterion to use; in brittle systems, mode I dominates and the criterion $G_I = G_{IC}$ may be sufficient. A number of authors\(^{1,3,20,24,44,50,53}\) have noted that $G_I$ was a maximum at about $90^\circ$ to the loading direction and $G_{II}$ a maximum parallel to the loading direction. The maximum value of $G_I$ at initiation was relatively independent of delamination size and ply interface, whilst $G_{II}$ varied considerably. For tougher systems it has been suggested that a mixed-mode criterion must be derived\(^{1,2,3,17,24,49}\).

To predict the subsequent delamination growth after initiation the analysis process is repeated, *i.e.* predict the new buckle shape, characterise the loadings at the new boundary and apply the criterion. However, due to the cumbersome nature of the process, few have analysed this problem in detail for embedded delaminations. A number of authors\(^{3,12,20,44}\)
modelled the growth of delaminations using finite element analysis. As the delamination increases in size, contact forces and anisotropy become more important, complicating the analysis. In addition, after significant delamination growth, the mechanisms become more complex further complicating the analysis\(^3\).

1.3.4 Delaminations in Structures

The work discussed in the previous Sections was based on studies on plain (flat) panels. However, structural features can also affect delamination behaviour, and there is evidence that prediction of damage behaviour in coupons is overly conservative when applied to structures\(^1,24,25,26\). Studies on stiffened structures have predicted the buckling loads and modes using analytical (Raleigh-Ritz) and finite element methods\(^6\) but most studies have been aimed at understanding, predicting and reducing the stresses at the skin/stringer interface\(^62\). Work on damage, particularly artificial delaminations, is limited and Davies et al\(^62\) have highlighted the need for predicting delamination in composite structures.

To date, only limited work has been conducted on artificial delaminations in structures\(^63,26,21,27\). A 'structure relevant specimen' was developed by NLR (Holland)\(^63,25,26,21\) which simulated the constraint of the stringers, but was simpler and cheaper to manufacture. When the delaminations were close to the surface the panel failure consisted of three distinct phases. Firstly, stable delamination buckling and growth, then rapid delamination growth across the stiffened regions, followed by slow delamination growth in the bays. For deeper delaminations, the nature of the growth could not be clearly ascertained. Similar findings have been found by the author\(^27,25\) using stiffened panels containing artificial defects at a number of locations, such as in the inter-stringer bay, partly and fully beneath the stringers. Defects in the bay exhibited similar behaviour to that observed in plain laminates (as discussed in Chapter 4). However, for defects beneath the stringer, the out-of-plane constraint above the defect impeded local buckling of the delamination, and consequently had a significant effect on the growth processes.
Introduction

1.4 Mixed-Mode Literature Survey

1.4.1 Introduction

Most toughness tests are used to measure the pure mode I \((G_{IC})\) and pure mode II \((G_{IIc})\) toughnesses. In addition, mixed-mode test methods have been developed, where toughnesses are determined over a range of mixed-mode ratios \((G_I/G_{II})\) with an aim of determining the failure locus \((G_I\text{ versus } G_{II})\) of the material. All these tests are based on the change in strain energy (Irwin-Kies relationship; Equation 1-1) for an increment of crack growth \((G_T)\),

\[
G_T = \frac{dC}{P^2} \frac{P^2}{2w}
\]

where \(C\) is the specimen compliance, \(P\) is the load, \(a\) the crack length and \(w\) is the specimen width. Delamination growth will occur when the energy per unit area \((G_T)\) released by an increment of crack growth exceeds the toughness (or critical strain energy release rate, \(G_C\)) of the material. As discussed in the previous Section, the strain energy released can be partitioned into pure components (Figure 1-3); mode I (peel), mode II (shear) and mode III (tearing) such that:

\[
G_T = G_I + G_{II} + G_{III}
\]

where \(G_I\), \(G_{II}\) and \(G_{III}\) are the pure mode components of strain energy release rate. As was discussed in Section 1.3, the last term is usually ignored.

1.4.2 Aims of the Survey

The aim of the survey was to identify the mixed-mode tests most suitable for generating delamination growth criteria and for characterising fracture morphology. The following requirements were used to rank the tests:

(i) Closed-form solutions should exist for the mode I and II components \((G_I\text{ and } G_{II})\).
(ii) The spectrum of achievable mixed-mode ratios should be as large as possible.
(iii) The same specimen geometry and lay-up could be used for all mixed-mode ratios.
(iv) The mixed-mode ratio should be independent of crack length.
(v) The ratio should be independent of toughness and environmental conditions.
The first requirement was to ensure that the reduction and partitioning of the data could be performed quickly (using a spreadsheet). The second was to ensure consistency; errors were not introduced by using more than one test method\textsuperscript{67}. The third requirement was to minimise the scatter introduced by variations in manufacture. The fourth was to ensure that any changes in toughness with crack length were quantified and errors due to local material variations minimised. The final requirement was to ensure the results from tests at different ratios could be compared directly.

Since real delaminations are rarely at unidirectional ply interfaces\textsuperscript{8,13}, this survey also considered the suitability of tests for characterising non-zero ply interfaces.

1.4.3 Pure Mode Tests

Many of the mixed-mode tests that characterise delamination growth have been developed from pure mode test methods (\textit{i.e.} measuring either $G_{IC}$ or $G_{IIIC}$). Therefore, firstly the pure mode tests are described and then the mixed-mode test methods are discussed (Section 1.4.4). A fuller description of both pure and mixed-mode tests is given elsewhere\textsuperscript{28}, in which the pure mode toughnesses (modes I and II) of a range of materials are tabulated.

Recently, there has been some debate as to what constitutes mode II fracture. Isotropic materials only locally fracture in mode I, even when under pure mode II loading. It has been suggested that the same applies to fracture of fibre-reinforced materials\textsuperscript{68,69,70,71} although the crack is constrained by the fibres which complicates the fracture. This debate is discussed in more detail in Section 1.5.2.

As discussed in Section 1.3.3, Mode III is generally considered to be negligible in embedded delamination problems. Therefore, test methods to characterise mode III fracture are not discussed and can be found elsewhere\textsuperscript{72,73,74,75,76}. The mode III toughness ($G_{IIIIC}$) can be in excess of six times $G_{IC}$ and two times $G_{IIIC}$\textsuperscript{72,76}.

1.4.3.1 Mode I Tests

The most common test method for measuring mode I toughness is the double cantilever beam test (DCB)\textsuperscript{74,75,77,78,79,80}. Finite element models of this test have shown that $G_{II}/G_{I}=$
0.06 for unidirectional and cross-ply DCB specimens, but this ratio increases when angle plies are present\(^8\). The typical DCB specimen, 150mm long, 25mm wide and 3mm thick, is cut from a 24-ply unidirectional laminate and has an insert at one end which acts as a starter crack. The load is applied to the laminate surface either side of the insert (Figure 1-7); a number of techniques have been used such as hinges\(^82,83\), end-blocks\(^70,71\) or quick mounted hinges\(^84,85,74,86\). The specimen is often precracked prior to testing.

Before testing, the edges of the specimen are painted white and reference lines are marked on. Load is applied under displacement control at about 3mm/min to pull apart the sublaminates (specimen arms), producing stable crack growth along the specimen length. During the test, the load and arm displacement are measured against the position of the crack front. Variations of this test, such as the width-tapered (WTDCB) and the height-tapered (HTDCB) tests, have been developed\(^83,80,77,87\) which simplify the data reduction but can complicate the specimen fabrication.

There are a variety of data reduction schemes for the DCB test which have been reviewed in detail by Davies et al\(^77\). The two basic approaches are the compliance and energy methods. The compliance methods are based on Equation 1-1 and assume the crack growth is linear-elastic. The following equations can be derived using beam theory\(^65,70,88\).

\[
\begin{align*}
G_{IC} &= \frac{12P^2a^2}{E_{11}w^2h^3} \quad \text{(1-3)} \\
G_{IC} &= \frac{3P\delta}{2wa} \quad \text{(1-4)}
\end{align*}
\]

where \(h\) is the half-thickness of the specimen, \(E_{11}\) is the specimen axial stiffness and \(\delta\) is the opening displacement. By combining these equations, the value of \(E_{11}\) can be determined to check the data reduction. Hashemi et al\(^65\) showed that correction factors are required for rotation at the crack tip and large displacement effects, which typically account for as much as 10% of \(G_{IC}\)\(^88,89,65\).

An empirical approach, which assumed that the compliance was a function of the crack length to the \(n\)'th power, was proposed by Berry and developed by Whitney et al\(^77\).
is reasonable agreement between beam theory and the Berry method for thin laminates, but for thick laminates, shear deformations generate errors in the beam theory analysis\textsuperscript{77}. The Berry method is often used for reducing data from multidirectional laminates\textsuperscript{90,81}. The other data reduction schemes are energy based methods such as the area method (for stable crack growth)\textsuperscript{77,91,92} and the J-integral approach\textsuperscript{77,60}.

1.4.3.2 Mode II Tests

Two types of pure mode II tests are now commonly used: the end loaded split (ELS)\textsuperscript{93,66,65,88,94} and the end notched flexure (ENF)\textsuperscript{90,95,94,88,96,80,83,65,97,68,75} methods. Some other methods exist but are rarely used; the rail shear method, the centre notched flexure (CNF) test\textsuperscript{28}, the cantilever bend end notched specimen (CBEN)\textsuperscript{94}, and various mixed-mode methods which are capable of generating pure mode II fracture (see Section 1.4.4.1).

The specimen used in the ELS test (Figure 1-8) has the same geometry as the DCB and the data retrieval and reduction are similar. The uncracked end is restrained vertically using rollers but is free to move horizontally\textsuperscript{70} while the specimen arms are loaded downwards so as to deform the test piece, giving stable crack growth.

The ENF specimen (Figure 1-9) geometry is similar to the ELS specimen\textsuperscript{95}. The specimen is loaded in three point flexure, typically with a span (2L) of 100mm. After precracking, the specimen is loaded in displacement control until unstable crack growth and failure occur; the load, midpoint displacement and initial crack length are recorded.

There have been a variety of data reduction schemes for mode II tests: beam theory, compliance calibration, higher order beam theory, shear deformable plate theory\textsuperscript{95} and numerical methods. The first method is based on an approximate elastic solution for a cantilever\textsuperscript{97,98} but neglects the stress singularity at the crack tip (Equation 1-5\textsuperscript{95,70});

$$G_{IIc} = \frac{9a^2P^2}{16E_1w^2h^3} \left[1 + 0.2 \frac{h^2E_{11}}{a^2G_{13}} \right]$$

(1-5)

The second term in the right hand bracket is usually neglected. A more detailed discussion is given elsewhere\textsuperscript{95,88}.
The compliance calibration method is a widely used experimental technique for measuring toughness.\textsuperscript{95,97,91} The compliance (C) of the specimen is measured against a series of crack lengths and the results are fitted a polynomial which is derived by integrating Equation 1-1, leading to Equation 1-6:

\[ G_{\text{IIc}} = \frac{3P^2ma^2}{2w} \]

where \( G_{\text{IIc}} \) is the mode II critical strain energy rate calculated using the compliance calibration method. Although this method overcomes the problem of machine compliance, which is constant, it is time consuming. This method generates lower results than those calculated from Equation 1-5 and is considered to be a more accurate method,\textsuperscript{95} although there are some problems due to non-linearities in the loading of the specimen arms.\textsuperscript{99}

Numerical studies have indicated that the ENF specimen generates almost pure mode II.\textsuperscript{95,100} However, numerically predicted values of \( G_{\text{IIc}} \) differ slightly from those given by analytical techniques which is attributed to friction and non-linearities.\textsuperscript{95} Comparison\textsuperscript{100} between ELS and ENF methods has indicated that the two methods give similar results although other authors\textsuperscript{99,101} found a large difference between the tests. The ENF test is generally unstable whilst the ELS is generally stable and generates less scatter.\textsuperscript{102} However, recently methods have been developed, using control systems, to ensure stable growth in the ENF.\textsuperscript{103} A further method, a four point bending equivalent of the ENF, has been proposed by Martin et al\textsuperscript{69} in which \( G_{\text{II}} \) is not a function of crack length and frictional effects are reduced. A closed-form solution has been developed for this method.

1.4.4 Mixed-Mode Tests
The mixed-mode tests which have been used to characterise carbon-fibre composites fall into three groups;

(i) Beam Methods
(ii) Axial Methods
(iii) Direct Loading Methods

The first group was developed from the pure mode tests (DCB, ENF, ELS) and relies on bending moments to induce mixed-mode fracture. The second group uses material
property mismatches within the specimen to induce mixed-mode fracture. Finally, the direct loading methods generate mixed-mode fracture by applying the load at an angle to the plane of the crack. A comparison between the mixed-mode methods is shown in Table 1-1 and a more detailed discussion is given elsewhere.  

1.4.4.1 Beam Methods

Mixed-mode fracture in the beam methods is generated by applying bending moments to a specimen containing a starter crack. Firstly, the mixed-mode flexure test (MMF), Figure 1-10, is a variation of the ENF test discussed in Section 1.4.3.2. The lower arm is cut away and the upper arm loaded, which generates unstable mixed-mode crack growth at the crack tip. This specimen uses a standard ENF test rig and the insert is usually at the mid-plane which generates a mode I proportion of 57% at the crack tip. The mixed-mode ratio is weakly dependent on the crack length and Crews et al. noted that different arm thicknesses may affect the toughness. A similar specimen to the MMF is the Cantilever Beam Opened Notch (CBON). The specimen has the same geometry as the MMF, but the thinner end of the beam is clamped and the other end is loaded, as with an ELS specimen. A closed-form solution has been developed, but the mixed-mode ratio varies with the crack length.

Most tests are designed to generate one mixed-mode ratio per specimen such that the ratio is independent of crack length. However, in the variable mixed-mode (VMM) test this is not the case (Figure 1-11). This test allows a spectrum of ratios to be obtained from one specimen, but assumes there is no dependence of the toughness on crack length (i.e. no R-curve effect). As shown in Figure 1-11, the specimen is pinned at the ends whilst an upward load is applied to the central loading point. For short crack lengths, the fracture is pure mode II but once the crack has passed the central loading point the mixed-mode ratio varies between pure mode II (when at the central point) and pure mode I (when at the end).

One approach to mixed-mode testing is to combine two pure mode methods and to analyse the results as a linear superposition; i.e. the loading can be partitioned into pure mode I and pure mode II components. This approach requires that there is no interaction between
the pure loading components as has been demonstrated by Juntti\textsuperscript{86} and Asp\textsuperscript{84}. One such test method has been proposed by Crews et al\textsuperscript{109}, the MMB test, a superposition of the DCB and ENF tests. The method uses a lever arrangement to introduce the two pure loading components simultaneously into a standard DCB specimen. By changing the lever length, a spectrum of mixed-mode loading ratios can be introduced into the test piece; for short lengths mode II dominates whilst at long lengths mode I dominates. This test has been used and developed further\textsuperscript{110,67,70,84,86,75,111,112,113,114,115,22} to eliminate non-linearities and is being evaluated by various working groups\textsuperscript{116,82}.

A diagram of the MMB rig is shown in Figure 1-12 (and in more detail in Figure 3-6). The hinge end of the specimen is attached to the base and the other end rests on a roller a distance 2L from the hinge holder. The loading lever is attached to the upper hinge and rests on another roller (fulcrum). During the test, a downward load P is applied to the end of the lever (via a loading yoke) a distance c from the fulcrum. This results in an opening load of $P_1$ at the cracked end of the specimen and a flexural load of $P_{11}$ at the fulcrum. In practice, mixed-mode ratios greater than 85% mode I cannot be achieved using the standard test specimen due to the large lever lengths required. The data retrieval is the same as for the DCB test and the test is stopped when the delamination reaches the fulcrum. For mode I dominant ratios the crack growth is stable, but as the mode II component increased the crack becomes unstable. Zhou et al\textsuperscript{117} developed a criterion for crack tip stability based on the lever and crack lengths. In addition, a method has been developed by Martin et al\textsuperscript{118} which uses mechanical feedback to stabilise the crack growth under mode II dominated loading.

The MMB was analysed using simple beam theory to derive expressions for $G_I$ and $G_{11}$:

\[ G_I = \frac{3P^2(3c - L)^2}{4w^2h^3L^2E_{11}} \left[ a^2 + \frac{2a}{\lambda} + \frac{1}{\lambda^2} + \frac{h^2E_{11}}{10G_{13}} \right] \] \hspace{1cm} (1-7)

\[ G_{11} = \frac{9P^2(c + L)^2}{16w^2h^3L^2E_{11}} \left[ a^2 + \frac{h^2E_{11}}{5G_{13}} \right] \] \hspace{1cm} (1-8)

where \[ \lambda = \frac{1}{h} \sqrt{\frac{6E_{32}}{E_{11}}} \]
Introduction

$c > L/3$, $E_{22}$ is the transverse stiffness and $G_{13}$ is the through-thickness shear stiffness. The latter ($G_{13}$) is usually replaced with $G_{12}$; the in-plane shear stiffness. For materials with low toughnesses or a heavy rig a further correction is required to account for the weight of the lever\textsuperscript{67,86,119}. The total fracture toughness, $G_T$, is the sum of the two pure components $G_I$ and $G_{II}$. Comparison with finite element models have indicated that these expressions give errors of less than 3\%\textsuperscript{110,117,120}.

The rig geometry proposed by Crews and Reeder\textsuperscript{110,109,67} used a span between the lower loading points (2L) of twice the span between the upper loading points, and could only achieve a limited range of crack lengths. Kinloch et al\textsuperscript{70} proposed a development in which these two spans were not equal. Although this led to more complicated expressions for $G_I$ and $G_{II}$ (Equations 1-9 and 1-10), longer crack lengths could be achieved:

\begin{align}
G_I &= \frac{3P^2(a + \chi h)^2}{w^2 h^3 E_{II}} \left[ \left(1 - \frac{c + b}{2L}\right) - \frac{c}{b} \right]^2 \tag{1-9} \\
G_{II} &= \frac{9P^2(a + \chi h)^2}{4w^2 h^3 E_{II}} \left[ \left(1 - \frac{c + b}{2L}\right) + \frac{c}{b} \right]^2 \tag{1-10}
\end{align}

where $b$ is the span between the upper loading points. When $b=L$ these equations gave identical results to Equations 1-7 and 1-8. These equations have been developed further to include large deflection effects\textsuperscript{70,114}.

Recently, a compliance calibration method has been derived\textsuperscript{104,82,120,86,117,118},

\[ C_{Exp} = \frac{\delta}{P} = \alpha + \beta a_{eff}^3 \tag{1-11} \]

where $C_{Exp}$ is the compliance, $a_{eff}$ is the crack length, and $\alpha$ and $\beta$ are constants which are mixed-mode ratio dependent. This leads to the following expression for $G_T$:

\[ G_T = \frac{3\beta a_{eff} P^2}{2w} \tag{1-12} \]

Benzegagh\textsuperscript{104} found that at mode II dominated loading there was a significant difference between Equations 1-7 and 1-8, and the compliance calibration (Equation 1-12).
A detailed study of the MMB test has been conducted by a number of workers in Sweden\textsuperscript{84,86,85,24,74,46,58}. The mode I and II loading point displacements were characterised to ensure that the test is a linear superposition of the DCB and ENF tests. Equations were developed from Equations 1-7 and 1-8 which included both load and displacement terms (as opposed to load and stiffness)\textsuperscript{86,84}; the conventional equations required accurate measurement of $E_{11}$ whilst the new expressions did not. The conventional load-based method gave accurate results but was thickness dependent whilst the new load/displacement method gave a discrepancy in the mixed-mode ratio but was not sensitive to specimen thickness. This discrepancy was attributed to the dial gauges used to monitor the displacements. Other methods such as the Berry method\textsuperscript{85} have been also considered. The MMB test results have shown good agreement with other mixed-mode ratio tests\textsuperscript{120,104}, although some authors have found discrepancies with the beam theory results (Equations 1-7 and 1-8)\textsuperscript{84,85,86,118}.

One family of mixed-mode tests similar to the ELS are the split cantilever beam methods; an ELS type specimen is fixed at the uncracked end whilst the arms are loaded vertically. There are two approaches: symmetrically cracked laminate methods (SCL) and non-symmetrically cracked laminate methods (NSCL). In the former, non-symmetrical loading is applied to a laminate containing an insert at the mid-plane; a linear superposition of the DCB and ELS test methods. In the NSCL methods, symmetrical or simple loading is applied to a laminate containing an insert which is not at the mid-plane. The SCL methods generate mixed-mode condition through the loading on the specimen, while NSCL methods achieve mixed-mode condition through the laminate geometry.

A popular SCL method is the fixed ratio mixed-mode (FRMM)\textsuperscript{93,88,65,23,121,22,70}. This is the same as the ELS except only that the lower sublamine was loaded downwards which generated 57% mode I fracture. The data retrieval was the same as that for the ELS specimen and the crack growth was usually stable. The FRMM test is a special case of the SCL mixed-mode test (Figure 1-13) proposed by Bradley and Hibbs\textsuperscript{122,78}. Both specimen arms were loaded simultaneously and, by changing the ratio of upward to downward load, the mixed-mode ratio at the crack tip could be varied between pure mode I (both loads
equal and opposite; DCB) and pure mode II (upper load zero and lower load upward; ELS). To remove any in-plane loads the specimen is restrained vertically and a complicated gripping system was designed to control the load introduction\(^{122}\). A similar method, the imposed displacement DCB (IDCB), was proposed by Benzeggagh et al\(^{105}\).

Work on NSCL test methods has been done using the asymmetric FRMM\(^{88,65}\), MMF\(^{105}\), DCB\(^{99,71,105,86}\), ELS\(^{142,71}\) and ENF\(^{123}\) tests. The mixed-mode ratio was dependent on the position of the insert, but the specimen design only permits a limited range of mixed-mode ratios. Furthermore, complications can arise in the data reduction (Section 1.4.6.2).

### 1.4.4.2 Other Methods

Another set of mixed-mode test methods are axial methods with rely on mismatches in stiffness or Poisson's ratio between the laminae to generate mixed-mode fracture; the mixed-mode ratio is controlled by altering the lay-up and geometry.

In the cracked lap shear test (CLS) a stepped laminate is loaded in axial tension (or compression), inducing mixed-mode fracture at the step (Figure 1-14)\(^{96,124,44,105,24,86,115,74,114}\). However, the crack does not always propagate in a single plane and may contain a mode III component\(^{124}\). There is a closed-form solution for \(G_T\)\(^{79,98}\), but numerical techniques must be used to partition the mode I and II components\(^{98,96,109}\). Furthermore, due to non-linear effects, the mixed-mode ratio is dependent on material toughness\(^{96}\). A variation of the CLS test is the double cracked lap shear specimen (DCLS)\(^{125}\) which is simply two CLS specimens back to back. This specimen avoids the non-linearities associated with the CLS test but generates two simultaneous cracks per specimen which complicates the test.

A popular axial test is the edge delamination tension (EDT)\(^{126,86,115,114,74,127}\) in which the lay-up is such that there are large differences in the Poisson's ratio between the laminae. When the specimen is loaded in tension, mixed-mode stresses develop at the edges, forming edge delaminations (Figure 1-15). The mixed-mode ratio has no closed-form solution and is determined numerically\(^{128}\). There are a number of other problems...
associated with the EDT test such as transverse cracking\textsuperscript{129}, residual stress effects\textsuperscript{109,126} and sensitivity to specimen width\textsuperscript{130}. Unlike previous mixed-mode test methods, the EDT specimen does not require an insert from which to initiate fracture\textsuperscript{128}, although this can make determination of delamination initiation difficult\textsuperscript{80}. This difficulty can be partly overcome by placing inserts along the edges\textsuperscript{126}, although this can lead to the toughness being sensitive to crack length\textsuperscript{126}. Inserts complicate the laminate manufacture and may deform during fabrication resulting in a non-uniform laminate thickness.

The final group of methods are the direct loading methods where mixed-mode fracture is generated by directly applying the load at an angle to the crack plane rather than via bending or property mismatches. The mixed-mode ratio is varied by changing the angle (\(\alpha\)) between the plane of the crack and the applied load. The simplest of these methods is the Arcan test\textsuperscript{12,131,132,86,74} (Figure 1-16). When \(\alpha = 0^\circ\), the loading is pure mode II whilst when \(\alpha = 90^\circ\), the loading is pure mode I. By changing the pairs of loading points, the angle \(\alpha\) and thus the mixed-mode ratio can be varied. In practice, the crack growth was unstable and there was a large degree of scatter which was attributed to the specimen debonding from the fixture, and the short crack length\textsuperscript{12}. Another drawback is that the ratio is dependent on the laminate material properties\textsuperscript{28}. Although this effect is limited when comparing unidirectional materials, it is important when comparing different lay-ups.

All the methods discussed so far are interlaminar tests in which the crack plane is parallel to the laminate plane. An alternative test method is the intralaminar test where the crack plane is perpendicular to the laminate and one such test, the off-axis tension test (OAT), has been used to characterise a wide range of composites\textsuperscript{133,134,135,74}. A unidirectional laminate, containing a through-thickness crack parallel to the fibres, is loaded at an angle \(\theta\) to the ply direction (Figure 1-17). The mixed-mode ratio depends on \(\theta\); at low values of \(\theta\), shear stresses predominate, while at high values of \(\theta\), peel stresses are dominant. However, there are some limitations; at low values of \(\theta\) the stress at the crack tip is not uniaxial and large specimen aspect ratios are required to generate a uniform stress field\textsuperscript{133}.
Also, the toughness increases with initial crack length \(^34,135\). In addition, intralaminar failure is physically different from interlaminar delamination, and this is reflected in a 45\% disparity in toughness between interlaminar and intralaminar test results \(^36\).

### 1.4.5 Non-Zero Ply Interfaces

#### 1.4.5.1 Pure Mode Tests

Characterisation of the toughness of non-zero ply interfaces has been reviewed by a number of authors \(^37,74\) and numerous problems have been identified. Firstly, the crack tends to migrate from the insert plane, complicating the data reduction because the specimen is no longer geometrically symmetrical \(^77,138\). This crack migration has been attributed to matrix cracking along the specimen edges which interacts with the main crack front \(^60,139,92\). Many workers have noted that the crack plane eventually migrates to a 0\(^\circ\) ply \(^60,137,92,139,91,106\). Robinson et al \(^60,23\) and Hunston \(^140\) developed a modified DCB which contained inserts along the specimen edges which drove the crack along the mid-plane. Unfortunately, these edge inserts can interact with anticlastic effects to make crack length measurement error prone. However, this specimen design has also been successfully employed by Trakas et al \(^73\) and Singh et al \(^29\).

A second problem with characterising non-zero ply interfaces is lack of symmetry in the individual arms of the laminates which can introduce stiffness coupling \(^77,140\). This can be minimised by optimising the lay-up \(^60,92,23,106,138,141\), although that can result in large displacement effects \(^77,60\) and cracking in the arms \(^74,101\) which invalidate beam analysis and require sophisticated reduction methods \(^142\). The stacking sequence should also be optimised to minimise residual stresses and anti-clastic effects \(^141,23,106,107,143\). Bonded aluminium strips \(^101\) have been used in increase the stiffness but this is problematic. Further complications can arise due to crack front curvature, which is controlled by the stacking sequence and can lead to difficulties in identifying the crack front position. Davidson et al \(^141,143,106,107,35,144\) developed two parameters, \(D_C\) and \(B_T\) to quantify the transverse and twisting curvatures respectively. These parameters were derived from the material properties and stacking sequence, and should be minimised to avoid complications in testing multidirectional laminates \(^60,143\).
There is also a question of the validity of the data reduction schemes for multidirectional laminates. Davidson et al.\(^{141,106,107}\) found that for the ENF tests beam theory and compliance calibration methods agreed for unidirectional laminates, but for non-zero ply interfaces the error was at most 13%. However, other authors have successfully used beam theory\(^{73,23}\) and compliance calibration methods for multidirectional laminates\(^{74,138}\).

To characterise mode I fracture of non-zero ply interfaces, DCB tests have been used\(^{90,60,77,139,81,92,91,145}\). A number of authors\(^{137,23,91,74,73}\) have found that at initiation \(G_{IC}\) is independent of ply interface. This has been used as justification for not characterising the toughness of multidirectional ply interfaces\(^{137,139,4}\), using 0°/0° ply interface toughnesses which give conservative design values\(^{137,68,139}\). However, after initiation, the toughness is dependent on ply interface\(^{137,74}\). At low ply angles at the delaminating interface (0° < \(\theta\) < 10°) there is no intermingling (nesting) of the plies during fabrication which results in a reduction of \(G_{IC}\)\(^{90,139,92}\). At large ply angles, the plies act as obstacles to the crack front and cause bifurcation, greatly increasing \(G_{IC}\)\(^{81,139,91,24,74,46,106,107,101,58,73,138}\). In brittle systems this effect leads to the crack migrating towards ply interfaces containing 0° plies\(^{140,90,77,8,13}\). There is evidence\(^{60,23}\) that the migration mechanism is material dependent; in XAS/914, a delamination would propagate within a ±45° ply interface whilst in T800/924 it would migrate. Furthermore, Chai found that in tough systems this migration mechanism did not occur and the toughness was independent of ply orientation\(^{139}\).

Characterisation of mode II fracture at non-zero ply interfaces\(^{146,90,91,145,147,23,74}\) has exhibited the same inherent problems as observed for mode I; crack plane migration at large ply angles and stiffness coupling. In mode II tests, the crack consistently migrates up through the plies (towards the compression surface of the laminate) until it reaches a 0° ply\(^{91,137,141,74,58,73}\). However, no clear mechanism for this phenomenon has been identified. For 0°/ϕ° ply interfaces, \(G_{IC}\) has been shown\(^{137,101}\) to be a maximum when \(\phi=30°\) and a minimum when \(\phi=90°\). However, other work has indicated that \(G_{IC}\) for 0°/90° ply interfaces was greater than that for 0°/0° or 0°/ϕ° ply interfaces\(^{141,106}\).
1.4.5.2 Mixed-Mode Tests

Most studies on non-zero ply interfaces have concentrated on pure modes, particularly mode I. As discussed in Section 1.4.4.2, axial mixed-mode test methods (EDT, CLS, DCLS) rely on stiffness mismatches to generate mixed-mode loading conditions at the delamination plane, thus characterising non-zero ply interfaces. However, this consequently makes using these methods to compare different ply interfaces problematic. This, and the inherent difficulties with these test methods (Section 1.4.4.2) make their use unattractive. Beam methods, on the other hand, allow direct comparison between different ply interfaces. However, these tests have the same inherent difficulties described in the previous Section; crack plane deviation and stiffness coupling.23,22,74,106

The test most commonly used for characterising non-zero ply interfaces is the MMB22,46,29,58, often in conjunction with the modified specimen with edge inserts23,60,29 (Section 1.4.5.1). A number of workers have also used the FRMM22,23 and MMF106,107 tests in conjunction with this specimen design. With conventional specimens, as was seen with mode II tests, the delamination migrates upwards through the plies to a 0° ply46,106,58. Some workers have found that the 0°/0° ply interface has the lowest toughness58,106,107 supporting characterisation of this ply interface for conservative design values. However, consistent mixed-mode toughness results for non-zero ply interfaces are scarce. In addition, there are difficulties with interpreting data from multidirectional mixed-mode tests, as discussed in Section 1.4.6.2

1.4.6 General Considerations and Discussion

1.4.6.1 Fracture Toughness Issues

There are a number of issues which should be considered before identifying the fracture toughness tests most suitable for the present work. One phenomenon, particularly in mode I dominated loading, is the R-curve effect77,91, toughness increasing with crack length which introduces problems when generating structural data77,74. This effect is attributed to fibre bridging and is dependent on specimen thickness, environment74,148 and interply resin thickness74,113,46,104. Tests conducted on 0°/5° ply interfaces have been used to eliminate fibre bridging but to still characterise the toughness of unidirectional ply
interfaces. Fibre bridging also leads to errors in crack length measurement, particularly for short lengths. Fibre bridging is an artifact of testing unidirectional laminates and will not occur in practice. In real structures, delaminations occur at non-zero ply interfaces, in which nesting and consequently fibre bridging is negligible. On this basis, most of the toughness design values in the USA are based on initiation rather than propagation, to eliminate fibre bridging effects. However, the material and local ply orientations can have a large effect on the thickness of the interply resin layer and consequently the local stress state and toughness at the ply interface. This questions the use of unidirectional laminates to characterise multidirectional toughness.

Crack length measurement is one of the largest sources of error in fracture toughness testing, particularly at high loading rates. Various methods have been suggested to overcome this problem, such as using strain gauges, acoustic emission, ultrasonics and conductive grids. The conductive grids are the most successful but tend to be very expensive. The crack length measurement can be further complicated by the crack front being curved, due to anti-clastic effects. Crack length measurement is particularly critical for detecting initiation of delamination growth and there have been investigations into determining this event, even using techniques such as acoustic emission and Moiré interferometry. The most widely accepted method is to define initiation as a 5% offset from linearity in the load/displacement response.

Where the crack growth is unstable, precracking and insert thickness can have a large effect on the results. Many workers have studied this, most notably Davies et al. The insert thickness should be minimised to reduce the effect of the resin fillet which forms at the insert tip and, ideally, the insert should have a thickness similar to a fibre diameter. In addition, the insert film can be disrupted during manufacture, introducing 'quilting' which affects the fibre distribution at the defect plane. Under mode I dominated loading, the insert thickness should be less than 15μm and under mode II loading precracking is required. To overcome the problem of insert thickness, precracking is used. A mode I precrack allows accurate post-test measurement.
Introduction

of initial crack length. However, the mode I precrack should be kept short\textsuperscript{153,152} since it introduces fibre bridging which can reduce $G_{\text{IIc}}$\textsuperscript{97,95}. Mode II precracks can be generated either statically or cyclically. However, the static precrack makes initial crack measurement difficult, whilst the cyclic crack is time consuming to generate\textsuperscript{95}. Mixed-mode precracks have also been used\textsuperscript{109,67}. Turnel et al\textsuperscript{153} showed that not precracking during MMB testing gave a 28% overestimate in toughness. Other phenomena can introduce errors; in tough systems, large displacements can introduce non-linearities, whilst under mode II dominated loading friction can act as a toughness enhancing mechanism.

Environment (temperature and moisture content) can have an important effect on the test results. Moisture and high temperatures can promote mechanisms such as fibre bridging\textsuperscript{74,148,91,154}, and subsequently increase R-curve effects. At a micromechanical level, the presence of moisture changes the toughness and stiffness of the matrix. In some materials it also reduces the fibre/matrix bond strength\textsuperscript{148,91,74}. Generally moisture has a limited effect on toughness\textsuperscript{148,154,74,91} but increasing temperature can reduce mode II toughness, particularly at high temperatures\textsuperscript{154}.

1.4.6.2 Mode Partitioning

Recently there has been debate over mode partitioning methods. Discrepancies have arisen when predicting the mixed-mode ratio at the crack tip using the two accepted approaches; global and local. The global method is based on the energy change of the entire laminate during crack growth and is derived from the Irwin-Kies expression (Equation 1-1). This leads to the closed-form equations for $G_1$ and $G_{\text{II}}$ used in Section 1.4.4.1. The second approach, the local method, considers the forces and displacements around the crack tip to determine the mixed-mode ratio and assumes a singular field at the crack tip\textsuperscript{71}. Since fracture toughness coupons are designed to simulate the loading conditions at delaminations in real structures, this discrepancy is very important. Currently, results of coupon tests are analysed using the global method (closed-form solutions), but then applied, via finite element models and the virtual crack closure method, using a local method\textsuperscript{70,155,156}. 

Page 47
Introduction

The discrepancy between the global and local approaches splits into two types of problem; geometrically unsymmetrical laminates and multidirectional laminates. Firstly, consider geometrically unsymmetrical laminates. In a unidirectional specimen, if the arm thickness are identical (i.e. the specimen has mirror symmetry about the midplane), the global and local approaches predict identical mixed-mode ratios\textsuperscript{70,71,105,123}. However, if the thicknesses differ, so does the mixed-mode ratio predicted by the global and local methods\textsuperscript{123,70,71,105} although the total toughness, $G_T$, is unaffected. Kinloch et al\textsuperscript{70,71} considered a number of examples, such as the ADCB, where the laminate arm thicknesses, $h_1$ and $h_2$, could differ. For equal and opposite loading on the arms, the global method indicated that the mixed-mode ratio was 100\% mode I and was independent of $h_1/h_2$. However, the local analysis indicated that the mixed-mode ratio varied between 100\% mode I ($h_1=h_2$) to 62\% mode I ($h_1>>h_2$). Kinloch et al have presented evidence, based on the magnitude of the toughnesses for different arm thicknesses, that the global analysis was correct\textsuperscript{70,71,149}. However, Benzeggagh et al\textsuperscript{105} studied a range of geometrically unsymmetrical test specimens and derived both global and local solutions for mode partitioning. The experimental results showed that the local solution was correct and beam theory predicted the wrong mixed-mode ratio. Robinson\textsuperscript{157} and Shim et al\textsuperscript{123} have had similar findings which are supported by fractographic evidence. This indicates that there was an interaction between the mode I and II bending moments which were not considered by the global analysis.

The second problem is associated with multidirectional ply interfaces\textsuperscript{155,71,156,127}. At unidirectional ply interfaces, the elastic properties of the adjacent plies are the same, so the mixed-mode ratio is unambiguous. However, in satisfying the boundary conditions when modelling a distinct interface in a multidirectional laminate leads to near-tip stresses and displacements which oscillate as the tip is approached; the mixed-mode ratio is not uniquely defined. For finite element models, if the element size used in virtual crack closure calculations is similar to the laminate thickness this effect is negligible. However, as the mesh density is refined, the problem is exacerbated and the mixed-mode ratio becomes increasingly mesh dependent\textsuperscript{127,155}. A number of methods have been proposed to overcome this oscillating stress problem.
One method is to model a thin resin interlayer within the delaminating interface, although this can slow down the model considerably because very small elements are required\textsuperscript{155,156,127}. Also, the thickness of the interlayer has a large effect on apparent toughness\textsuperscript{127,74,111,150}. Finally, delamination growth within a resin interlayer is physically unrealistic; in reality the crack will migrate through the resin layer to the fibres of the adjacent ply and, under mode II loading, form by coalescence of secondary cracks\textsuperscript{155}.

A second method is known as the $\beta=0$ method, in which an 'insignificant' material property, such as $\nu_{13}$, is artificially changed such that the oscillation is eliminated\textsuperscript{155,156,127}. This method is highly dubious since it is physically unrealistic\textsuperscript{155,127,71} and it is questionable if changing the 'insignificant' material property really has no effect on the results. Davidson et al\textsuperscript{156} have argued that the $\beta=0$ method has some physical basis on a microscopic level.

A third method has been proposed by Beuth et al\textsuperscript{155} who modelled the adjacent plies as in-plane orthotropic materials, with one axis aligned with the crack front direction. This defines near-tip stress and displacement relationships which are separated from oscillating effects. Physically, this uses a characteristic length for defining the mixed-mode ratio, and this characteristic length is greater than the region in which the oscillation dominates. Davidson et al\textsuperscript{156} and Martin et al\textsuperscript{127} proposed a crack-tip element (CTE) which used classical plate theory, formulating closed-form solutions for $G_l$ and $G_{ll}$. This method needed a normalising parameter which is determined using a model of a known loading condition; some success has been achieved using this approach.

Kinloch et al\textsuperscript{71,70} have considered the global loading conditions on the cracked multidirectional laminate; this approach does not require a singular field at the crack tip\textsuperscript{156}. It was shown that for a multidirectional interface, the global loading conditions are rotated at the crack tip; e.g. due to differences in Poisson's contractions, pure mode I loading would lead to local mixed-mode conditions at the crack tip. This led to the Kinloch criterion discussed in Section 1.5.2.
To date, it has been identified that there is clearly a discrepancy with partitioning the pure modes, both in geometrical unsymmetrical and multidirectional ply interfaces. Kinloch et al\textsuperscript{70,71} and Olsson et al\textsuperscript{46} attributed the problem to the process zone at the crack tip dominating the fracture, rather than the singularity assumed using LEFM, suggesting that the global approach is more valid. This process zone is complicated by large scale mechanisms such as fibre bridging and secondary matrix cracking\textsuperscript{46,111} and can be of the order of a millimetre in size\textsuperscript{70,111} under mode II dominated loading. Unfortunately, this does not resolve the difficulty of applying fracture toughness data, generated using the global approach, to finite element models of delamination growth.

1.4.6.3 Pure Mode Tests

The most consistent pure mode test methods are the DCB (mode I), ENF (mode II) and ELS (mode II) tests. The specimens are easy to manufacture and test. The results are relatively tolerant to variations in specimen geometry and the data retrieval is simple. Finally, the data reduction uses closed-form solutions, albeit with correction factors. However, there are still a number of drawbacks associated with these tests. They all require an insert to be placed at the mid-plane during manufacture; they are generally unsuitable for qualifying components which have already been manufactured. Secondly, it is difficult to characterise multidirectional laminates, although these difficulties can be overcome by balancing the lay-up and using edge inserts\textsuperscript{60}.

1.4.6.4 Mixed-Mode Tests

The advantages and disadvantages of all the mixed-mode tests are tabulated in Table 1-1. Firstly, considering the beam methods, all except the NSCL generate a wide range of mixed-mode ratios. The toughnesses can be partitioned using a closed-form solution although this often requires corrections. The crack stability in these tests presents the same problems as those associated with pure tests. The tests in which crack growth is stable (VMM, SCL, NSCL and MMB) can exhibit R-curves, whilst tests in which it is unstable (MMF, NSCL and MMB) are sensitive to precrack type. As with the pure mode methods, the geometry of the beam test methods also make them susceptible to problems when characterising multidirectional laminates.
Introduction

The VMM test is designed such that a range of ratios can be determined from each specimen. This eliminates specimen variation but, if there is a dependence of toughness on crack length (i.e. an R-curve effect), it is difficult to interpret the results\textsuperscript{66,109}. For the MMF and NSCL tests different specimens are fabricated to generate different mixed-mode ratios, which can introduce errors due to manufacturing variations. Although closed-form solutions have been developed, problems have arisen due to the unsymmetrical specimen geometry for most mixed-mode ratios\textsuperscript{71}, as discussed in the Section 1.4.6.2.

The MMB and SCL tests are linear superpositions of pure mode tests and generate a range of mixed-mode ratios by changing the loading conditions. The same specimen geometry is used for all mixed-mode ratios, geometries are simple and the specimens are easy to manufacture. However, inevitably, the test rigs are complicated. The SCL test requires two load cells to measure the load applied to each arm whilst the MMB uses a complicated loading fixture.

Although axial tests have the advantage that they do not require a starter crack, there are a number of problems. Specimen manufacture can be time consuming and expensive, and the range of mixed-mode ratios which can be generated is limited. Since the crack growth is usually at a non-zero interface, multidirectional ply interface data can be generated without the problems associated with the beam tests. Conversely, it is difficult to generate unidirectional data using these tests. The specimen loading is generally simple, and the crack growth is generally stable. However, although closed-form solutions exist for $G_C$, numerical analysis is required to partition the toughness.

There are also drawbacks particular to the individual test methods. In the CLS and DCLS methods, the mixed-mode ratio can vary with toughness and crack length whilst the DCLS test has two crack fronts which complicates the data reduction. The EDT test can be used over a larger range of ratios but is sensitive to residual stresses and transverse cracking. Although problems with detection of initiation can be overcome by using inserts, these can lead to geometry dependence.
Finally, for direct loading methods, the mixed-mode spectrum is quite large but these tests have problems at high and low proportions of mode I. Closed-form solutions exist for $G_1$ and $G_{II}$, but for accurate results numerical analysis is required. The Arcan method employs the same specimen for all mixed-mode ratios although it requires a special loading rig, is susceptible to bonding failures and the crack growth is unstable. It is questionable how applicable intralaminar tests (OAT) are characterising delamination growth. Firstly, the fracture is through-thickness rather than at a ply interface, so there is no interlaminar resin-rich region and extensive fibre bridging can occur. The resulting R-curve behaviour complicates the data reduction and the results will be strongly dependent on the tow alignment. Secondly, at a delamination, plane strain conditions are prevalent whilst in intralaminar test pieces, plane stress conditions prevail, which may lead to overestimates of delamination resistance. Although intralaminar specimens are easy to manufacture, different test pieces are required for different mixed-mode ratios. Furthermore, non-zero ply interfaces cannot be investigated using intralaminar methods.

1.4.7 Chosen Mixed-Mode Test Methods

Of the three groups of mixed-mode test methods, beam methods best meet the requirements of the present programme. Of these methods, the load based methods (MMB, SCL) were the most attractive. These tests have closed-form solutions, a large range of mixed-mode ratios and the same specimen geometry for all mixed-mode ratios. Furthermore, the mixed-mode ratio is independent of crack length and toughness. Both these test methods are superposition's of two pure mode tests and thus the corrections developed for the DCB, ELS and ENF tests could be applied. The SCL test requires two load cells so the MMB test was selected for characterising delamination growth in unidirectional laminates.

For multidirectional laminates, the beam methods were prone to non-linearities, although these problems could be overcome by using an optimised stacking sequence. Although axial methods generate fracture at non-zero ply interfaces, there were too many inherent problems. Of the direct loading methods, the mixed-mode ratios which could be achieved
were dependent on the mechanical properties of the laminate. Intralaminar tests cannot be used to characterise non-zero ply interfaces. Therefore, the most suitable method for characterising multidirectional interfaces was again the MMB test. However, to compare with the MMB test results, a limited number of Arcan tests were also conducted

### 1.5 Failure Criteria Literature Survey

#### 1.5.1 Introduction

A wide range of mixed-mode tests has been developed to characterise composite materials, the main objective of which has been to derive a mixed-mode failure criterion for predicting delamination growth. The data from the mixed-mode tests; $G_{II}$ versus $G_{I}$ (failure locus) is plotted and a curve (failure criterion) is fitted to this data. This is then used for predicting damage initiation and growth of artificial defects. If the loading conditions (*i.e.* the magnitude and proportions of $G_I$ and $G_{II}$) at the edge of such a delamination fall beneath the criterion then the delamination will not grow. However, if the loading conditions are outside, delamination growth will occur. There has been little success in using failure criteria developed for isotropic material to predict delamination growth and therefore new types have been developed (Table 1-2).

#### 1.5.2 Failure Criteria

Most brittle epoxies systems\textsuperscript{28,128,96,68} have a mode I toughness which is considerably lower than the mode II toughness and it has been suggested that most structural delamination failures are dominated by mode I loading\textsuperscript{68}. This can result in the mode I component controlling the mixed-mode toughness (except at high mode II ratios). To model this, the mode I and mode II criteria (Equations 1-13 and 1-14) have often been employed\textsuperscript{128,34,53,68,112,127}:

\begin{align}
G_I &= G_{IC} \quad \text{(1-13)} \\
G_{II} &= G_{IC} \quad \text{(1-14)}
\end{align}

These criteria are simple to apply, but usually more sophisticated criteria are required to accurately model delamination. Generally, under structural conditions, the toughness is controlled by some combination of the mixed-mode components with neither mode
Introduction

consistently dominating failure. In thin structures, and under conditions where out-of-plane loading is significant, mode I dominates. However, in thick laminates and within impact damage, mode II is significant. The simplest mixed-mode criterion is the $G_C$ criterion, (Equation 1-15)\(^2,88,96,12,133\) although under mode II dominated loading frictional effects occur which causes this criterion to break down\(^96\). Armanios et al\(^125\) used a weighted form of this criterion to compensate for the friction effect.

$$G_C = G_I + G_{II}$$ \hspace{1cm} (1-15)

A criterion which has been extensively used is the linear criterion (Equation 1-16) which generates a linear locus between $G_{IC}$ and $G_{IIc}$\(^133,36,132,12,158,79,2,23,34,44,156,115,85,112,24\):

$$\frac{G_I}{G_{IC}} + \frac{G_{II}}{G_{IIc}} = 1$$ \hspace{1cm} (1-16)

Both Equations 1-15 and 1-16 are simple rules of mixtures and assume no interaction between the pure components. A form which allows for non-linearity of the locus\(^115\) is the power law criterion\(^133,98,79,134,88,96,130,74,114\):

$$\left[ \frac{G_I}{G_{IC}} \right]^m + \left[ \frac{G_{II}}{G_{IIc}} \right]^n = 1$$ \hspace{1cm} (1-17)

When $m,n > 1$, the locus is convex while when $m,n < 1$, it is concave, and if $n > m$ the locus is skewed towards the mode I axis\(^67\). The values of $m$ and $n$ are material dependent and are determined empirically\(^159\). Whitcomb\(^34\) used Equation 1-17 to predict delamination growth but Reeder\(^67\) noted that it could not describe the full locus and there was no physical basis for $m$ and $n$. Yan\(^66\) proposed a similar criterion (Equation 1-13) which allowed a range of material responses:

$$G_C = G_{IC} + \rho \left( \frac{G_{II}}{G_I} \right) + \tau \left( \frac{G_{II}}{G_I} \right)^2$$ \hspace{1cm} (1-18)

Reeder\(^67\) noted that the introduction of mode II at high mode I ratios could increase the magnitude of the mode I component, generating a 'hump' in the locus, and Yan's equation could model this behaviour whilst Equations 1-13 to 1-17 could not. However, Equation 1-13 is unable to model mode I and mode II dominated loading\(^67\). A similar polynomial expression, which considers of the stress-intensity factor at the crack tip, is the 'K' criterion\(^2,133,134\).
Introduction

\[ G_c = G_{IC} - (G_{IC} - G_{IC}) \sqrt{\frac{G_I}{G_{IC}}} \]  
\[ (1-19) \]

A variation of this is the 'exponential K' criterion (Equation 1-20) which gives a wide range of loci but exhibits a 'jog' at the mode I axis for \( \eta \leq 1 \):

\[ G_c = (G_{IC} - G_{IC}) e^{\eta \frac{G_I}{G_{IC}}} + G_{IC} \]  
\[ (1-20) \]

All the criteria discussed so far are empirical and have little physical basis. Furthermore, the degree of interaction between mode I and mode II components is not easy to quantify. Hashemi et al.\(^8\) addressed the interaction by introducing a parameter \( I \):

\[ \begin{bmatrix} G_I \\ G_{IC} \end{bmatrix} - 1 \begin{bmatrix} G_{II} \\ G_{IC} \end{bmatrix} - 1 \frac{G_I G_{II}}{G_{IC} G_{IC}} = 0 \]  
\[ (1-21) \]

The larger the value of \( I \), the greater the interaction between the mode I and II components. However, rather than being derived physically, the value of \( I \) was determined empirically. At low values of \( I \), a large change in the components has little effect on \( G_c \), whilst for a large value of \( I \), small changes in the components have a large effect on \( G_c \). Typically, for epoxy systems\(^9\), \( 0.26 < I < 3.12 \) although fibre bridging and friction\(^16\) could change the degree of interaction. This led to a general form of Equation 1-21\(^67,161\):

\[ \begin{bmatrix} G_I \\ G_{IC} \end{bmatrix} - 1 \begin{bmatrix} G_{II} \\ G_{IC} \end{bmatrix} + \beta + \Phi \frac{G_I}{G_{IC} + G_{II}} \frac{G_{II}}{G_{IC} G_{IC}} = 0 \]  
\[ (1-22) \]

where \( \beta \) and \( \Phi \) are constants. Equation 1-22 gives a wide range of responses but is quite difficult to use\(^67\). Ramkumar et al.\(^79\) developed a simpler interaction criterion:

\[ \begin{bmatrix} G_I \\ G_{IC} \end{bmatrix} \frac{G_I}{G_{IC}} + \frac{G_{II}}{G_{IC} G_{IC}} = 0 \]  
\[ (1-23) \]

One approach for deriving failure criteria is through investigation of the fracture surface morphology\(^98,2,134,133,71,67,162\). Hahn\(^2,134,162,135\) proposed that the toughness was related to the total area of fractured matrix (Section 1.6). Under high mode II dominated loading, the presence of shear cusps\(^13,163\) resulted in a greater area of fractured matrix which led to Equation 1-24\(^2,134,67,135\):

\[ G_c = G_{IC} - \Omega + \Omega \sqrt{1 + \frac{G_{II}}{G_I} \sqrt{\frac{E_{11}}{E_{22}}}} \]  
\[ (1-24) \]
where \( \Omega \) is a constant. The term in the large square brackets can be related experimentally to the tilt angle of the cusps. However, except when \( \Omega = 1 \), this criterion predicted an infinite \( G_{ILC} \). To overcome this difficulty this expression was modified to give the exponential hackle criterion (Equation 1-25)\(^{33,36} \):

\[
G_c = (G_{IC} - G_{ILC})e^{\gamma(1-N)} + G_{ILC}
\]

where

\[
N = \sqrt{\left[ 1 + \frac{G_{II}}{G_{I}} \sqrt{\frac{E_{II}}{E_{22}}} \right]}
\]

and \( \gamma \) is a constant.

Reeder\(^{67} \) suggested a simple bilinear criterion which was based on fractographic observations. The criterion depended upon two parameters, \( \xi \) and \( \zeta \), which were the gradients of the two line segments given by Equations 1-26 and 1-27:

\[
G_I = \xi G_{II} + G_{IC} \quad (1-26)
\]

\[
G_I = \zeta G_{II} - \zeta G_{ILC} \quad (1-27)
\]

The particular equation used depends on the mixed-mode regime, although fitting experimental data to these expressions is complicated.

A criterion which has been used by a number of authors is the Benzeggagh criterion\(^{104,120,105,111,113} \) which is based on the stress intensity factor around the delamination crack tip.

\[
G_c = G_{IC} + (G_{ILC} - G_{IC}) \left[ \frac{G_{II}}{G_I + G_{II}} \right]^m
\]

\( (1-28) \)

A variation of this equation has also been used in which the denominator \( (G_I + G_{II}) \) is replaced by \( G_{ILC} \). The value\(^{104,105,113} \) of \( m \) is between 1 and 0.6 and, when \( m=1 \), the expression reduces to the linear criterion (Equation 1-16). As \( m \) increases, the locus becomes increasingly non-linear\(^{113} \). Singh et al found that \( m\approx8 \) for laminates containing interleaves\(^{111} \).
Finally, Kinloch et al\textsuperscript{71,70} have proposed a criterion which is based on the hypothesis that, upon delamination growth, an induced critical mode I component ($G_0$) is exceeded. That is, the apparent mode II fracture was solely due to mode I fracture at an angle to the plane of the laminate. This hypothesis is also supported in recent work by O'Brien\textsuperscript{68} and has similarities to the Hackle criterion (Equation 1-24). The hypothesis can be expressed as;

$$G_0 = G_1 + \sin^2 \omega \, G_{II}$$

(1-29)

where $\omega$ is the 'surface roughness'. By substituting for pure modes I and II, this gives:

$$\sin^2 \omega = \frac{G_{IC}}{G_{II}}$$

(1-30)

This implies that $G_{IIIC}$ can be estimated by measuring $G_{IC}$ and $\omega$. The 'worst case' is considered to be when $\omega=45^\circ$ ($G_{IIIC}=2G_{IC}$); this is equivalent to shear microcracks forming ahead of the main crack front, as is discussed in Section 1.6. In practice, $G_{IIIC} \approx 2G_{IC}$ for brittle and modified epoxy systems\textsuperscript{58,91,119} (as tabulated in Reference 28), but in very tough and thermoplastic systems\textsuperscript{119,68} $G_{IIIC} < 2G_{IC}$, indicating a different failure mechanism as shown in Section 1.6. Based on this hypothesis, Equation 1-31 has been developed\textsuperscript{70}:

$$G_M = G_C \left[ \cos^2(\phi - \phi_0) + \sin^2 \omega \sin^2(\phi - \phi_0) \right]$$

(1-31)

where $G_C$ is the measured fracture energy, $G_M$ is a constant, $\omega$ is the slope of the surface roughness, $\phi_0$ is the phase angle of the elastic mismatch (between $18^\circ$ and $26^\circ$) and $\phi$ is given by:

$$\cot^2\phi = \frac{G_1}{G_{II}}$$

This criterion has been shown to give good agreement with experimental data\textsuperscript{70,71} and has some physical basis, which may justify it's use.

1.5.3 Discussion and Conclusions

The main reason for conducting mixed-mode tests is to develop delamination failure criteria. If delamination growth in composite structures can be predicted, components can be designed which resist such growth. This has particular importance for predicting impact damage and defect growth in aircraft structures\textsuperscript{34}. However, to date, there has been only limited success in generating accurate and consistent mixed-mode fracture toughness data. Therefore, fitting this data to a criterion is prone to error, which throws many of the
Introduction

empirical models into doubt. Fortunately, fracture toughnesses and failure criteria can be linked using fractography\textsuperscript{134,67}; this technique gives an insight into the dominant failure mechanisms. Fractography has been the basis of many of the physically based models.

From the evidence presented in the literature, delamination criteria appear to be material dependent and no general failure criterion exists\textsuperscript{67}. In brittle systems, fracture is often dominated by the mode I component and therefore the $G_I$ and $G_{II}$ criteria (Equations 1-13 and 1-14) often suffice\textsuperscript{128}. The general interaction criterion (Equation 1-22) and the bilinear criterion (Equations 1-26 and 1-27) can give accurate descriptions of behaviour which is supported by fractographic evidence\textsuperscript{67}. In toughened epoxies, where $G_{IC} - G_{IJC}$, the $G_C$ criterion (Equation 1-15) can give adequate results. However, for systems where $G_{IC} \neq G_{IJC}$, physically based criteria are required. One additional factor which should be considered is environment which can strongly affect the criterion chosen\textsuperscript{134,148,98,91}.

1.6 Delamination Fractography Literature Survey

1.6.1 Introduction

Fractographic analysis of failure surfaces gives an insight into damage mechanisms and, ultimately, delamination failure criteria\textsuperscript{105}. Many of the fractographic features in fibre-reinforced composites are matrix dominated and have been studied in detail by workers studying bulk polymers\textsuperscript{64,13}. Firstly, the pure mode fracture surface morphology in both brittle and toughened unidirectional systems will be considered. Then mixed-mode fracture in unidirectional systems will be discussed. Finally, fracture at non-zero ply interfaces is reviewed. This work concentrates on dry laminates tested at room temperature; the effects of environment on fracture morphology are material dependent and are discussed in detail elsewhere\textsuperscript{164,148,91,98}.

1.6.2 Mode I Fracture Morphology

Macroscopically, mode I peel fracture surfaces are rough, spectrally reflective and usually within a ply\textsuperscript{13,163}. In very brittle systems mode I fracture is dominated by two major features; fibre pull-out and matrix cleavage\textsuperscript{131,13,159,165,122,132,145,87,134,91,158,121,74,68}. Fibre pull-out results in loose fibres\textsuperscript{87,165,13,78,122}, the surfaces of which are often clean\textsuperscript{131,159,145}.
Introduction

although some thin matrix sheaths have been noted\textsuperscript{87}. The imprints of the fibres have been described\textsuperscript{145} as smooth and featureless but this morphology is dependent on fibre type\textsuperscript{87}. Occasionally, cusps (Section 1.6.3) have been observed in fibre imprints\textsuperscript{145,87} which are attributed to local shear failure of the fibre/matrix interface\textsuperscript{131}. Donaldson\textsuperscript{133}, Garg\textsuperscript{164} and Johannesson et al\textsuperscript{135} found that intralaminar fracture surfaces were rougher and exhibited more fibre bridging than interlaminar surfaces.

In brittle systems the matrix morphology is dominated by a very small process zone ahead of the crack tip\textsuperscript{159}. The matrix fracture is usually brittle cleavage containing river markings and scarps from which the failure direction can be determined\textsuperscript{13,165,87}. Fracture surfaces of bismaleimide systems are similar to those of epoxies.

In tougher systems the mechanisms of fracture vary. Bascom et al\textsuperscript{87} noted similar features to brittle systems, although some toughened systems\textsuperscript{159,142,122,78} are dominated by fibre/matrix debonding. The toughening agents in epoxy systems such as Hexcel 914 occur as spheroids, (1-2\textmu m in diameter) which cover the fracture surface\textsuperscript{13}. The surface is dominated by fibre pull-out and matrix cleavage around the spheroids giving the surface a cellular appearance. In tough systems\textsuperscript{159,158} the fracture is characterised by a combination of fibre/matrix debonding, loose toughening particles, matrix deformation and void formation, although some regions of brittle cleavage occur at resin-rich regions. Very tough systems\textsuperscript{131,159} are characterised by slip-stick delamination growth, gross matrix deformation and a dimpled fracture surface (\textquoteleft duplex\textquoteright). Fibre bridging also occurs which enhances the toughness.

1.6.3 Mode II Fracture Morphology

Mode II shear fracture surfaces are optically dull and smooth\textsuperscript{163,13} and occur within the ply interface. Microscopically, mode II fracture is dominated by sigmoidal platelets on the surfaces; cusps\textsuperscript{100,147,165,122,97,78,158,91,145,131,132,67,74,68,121}. Various mechanisms have been proposed for these features\textsuperscript{13,145} but in-situ observations\textsuperscript{100,122,147,13} have shown that they are generated from inclined secondary cracks (usually at 45\textdegree) forming ahead of the main crack tip. The inclination of the cusps is perpendicular to the resolved principal stress in
Introduction

the matrix due to the applied shear\textsuperscript{105,13,145,73}. The observed increase in toughness in mode II over mode I is attributed to the formation of these features and the resulting increase in the total fractured area\textsuperscript{68,73,113}. Cusps are usually regularly spaced between the fibres and are generally perpendicular to the fibre direction, inclined away from the direction of applied shear. Friedrich\textsuperscript{165} noted that cusp size decreased with increasing rate of loading although Hiley\textsuperscript{166} found no such effect. On the opposing fracture surface, scallop shaped features can occur\textsuperscript{78,13,145} which are the cusp imprints. Shear fracture surfaces are also characterised by debris formed by friction between the crack faces\textsuperscript{165,163,13,122}, fibre fracture\textsuperscript{131,13} and fibre/matrix debonding\textsuperscript{145,67} can also be prevalent. Donaldson\textsuperscript{133}, Hahn and Johannesson\textsuperscript{134,135} studied intralaminar shear which was characterised by broken fibres, debris and clean fibres.

In brittle systems, cusps are distinct and regular whilst in bismaleimide systems\textsuperscript{97} they are densely packed and irregular. Tough systems\textsuperscript{122,78,97,164,67} behave in a similar manner to brittle systems, except that the cusps are usually irregular and drawn. Hiley\textsuperscript{97} ranked composite toughness from the fracture surface morphology, although this was not feasible for two phase systems such as Hexcel 914 where the matrix fracture was obscured by the spheroids\textsuperscript{97,13}. Spheroids can leave semi-circular depressions\textsuperscript{13} which indicate the direction of shear loading. In very tough matrix systems\textsuperscript{135,158,159,131,100} the mechanisms for shear failure are plastic deformation, void formation and coalescence. A few cusps occur in resin-rich regions, but these are very irregular and V-shaped.

1.6.4 Mixed-Mode Fracture Morphology

Mixed-mode fracture surfaces are usually a mixture of the pure mode morphologies. Most of the studies of mixed-mode morphology have been performed on brittle systems\textsuperscript{131,122,78,13,67,98,132,121}. Mode I dominated surfaces are very similar to those of pure mode I and are dominated by fibre bridging and matrix cleavage. As the mode II component increases, shallow scarps develop, which then change into shallow steps and cusps with serrated feet\textsuperscript{13}. For a given mixed-mode ratio, the cusps are all at the same inclination\textsuperscript{122}. Generally, the surface roughness, angle and number of cusps increases as the mode II component increases\textsuperscript{131,122,98,113,22,119,117} until, at almost pure mode II loading,
the cusps are closely packed and upright. The degree of fibre bridging decreases with increasing mode II component\textsuperscript{22}. However, rather than a gradual change, a sudden change in morphology when the mode II component exceeds about 45% has been noted by some workers\textsuperscript{78,67}. Finally, striations or ribs occur\textsuperscript{13} which decrease in spacing as the shear component increases. Similar trends in fracture morphology occur in toughened systems\textsuperscript{122,78,135,67,131} whilst intralaminar mixed-mode fracture\textsuperscript{133,134,135} exhibits smaller cusps and more fibre bridging.

1.6.5 Fractography of Non-Zero Ply Interfaces

Fractographic studies have been performed on the pure mode I\textsuperscript{139,148,92,91,81}, pure mode II\textsuperscript{147,148,91,145} and mixed-mode\textsuperscript{98} surface morphologies of non-zero ply interfaces. As discussed in Section 1.4.5, multidirectional fracture surfaces are complicated because the crack growth is not self-similar (i.e. the crack can migrate) and a variety of mechanisms occur. In addition, at multidirectional ply interfaces, the interply resin layer is thicker which can increase process zone size and toughness\textsuperscript{111,150}.

Generally, similar features to those observed in unidirectional ply interfaces occur at multidirectional fracture surfaces. Mode I surfaces are dominated by matrix cleavage, although the degree of fibre bridging can be greatly reduced\textsuperscript{74,46,73}. In mode II dominated surfaces, shear cusps dominate the fracture surface, although these have been observed to be smaller than those in 0°/0° ply interfaces\textsuperscript{145}. In addition, grid-like or hatched matrix deformation has been observed under mode II dominated and mixed-mode surfaces\textsuperscript{139,92,148,81,145,137,23,73}. When the crack is changing plane, extensive fibre bridging, crack bifurcation (transverse cracks) and fibre fracture occurs\textsuperscript{13,137,24,105}. This change in plane also leads to changes in the local mixed-mode ratio\textsuperscript{137}.

1.6.6 Discussion

The evidence shown here indicates that mixed-mode fractures are a mixture of pure mode morphologies. Under high mode I loading ratios, fibre pullout, matrix cleavage and other peel features are prevalent. Under high mode II loading ratios, typical shear features such as cusps are prevalent. There are indications that the inclination, spacing and shape of the cusps can be used to infer the local mixed-mode ratio during fracture. This would be very
useful for post-mortem analysis of failed structures. However, delamination in structures is rarely of a fixed mixed-mode ratio and surface morphology is also dependent on material, environment and manufacturing factors.
2. Delaminations in Real Structures

2.1 Introduction

One of the most important sources of delamination is impact damage which can be generated by events \(^2,4,6,167\) such as dropped tools, hail or runway debris during take-off. The former, which is usually referred to as low velocity impact, is of particular concern because the damage is frequently not visible \(^30\) but can reduce the compressive strength by as much as \(40\% \)^168,169. Furthermore, impact damage can lead to exposure of the load-bearing plies to environmental attack, further reducing strength.

The main driving force for studying embedded delaminations is to model the behaviour of impact damage. However, there are differences in the behaviour of these two types of defect. Davidson\(^41\) found that the initial buckling of an impact damage under compressive load was gradual, whilst that at embedded defects was sudden, although this may be due to experimental conditions. For impact damage, the buckled blisters were smaller than the initial defect dimensions\(^41,21,26\) and the subsequent growth was more limited in extent than that observed from embedded defects. Generally, failure in panels containing embedded defects occur at significantly higher strains than that of panels containing impact damage\(^21,26,6\). However, there are parallels in the damage growth mechanisms, particularly when the impact damage state is dominated by delamination\(^21,26,6\).

Typically, impact damage is composed of a mixture of matrix cracks, delamination and fibre fracture\(^9,9,42,43,170\). There have been comprehensive studies to characterise impact damage in plain (flat) laminates with respect to parameters such as impact conditions, material, lay-up and laminate geometry, and these are discussed in more detail in Section 6.1.1.2. Much of the work has investigated the residual compressive strength (RCS) after impact\(^167,168,171\). Although impact damage in plain panels has been extensively studied, impact damage in composite structures is poorly understood. In stiffened panels for example, further effects such as local geometry, local support by the sub-structure and the overall dynamic response must be considered; these are discussed
in Section 6.1.1.3. It is important that impact damage in structures is characterised since the results from coupons can be too conservative when applied to in-service components.

2.2 Aim of Impact Studies
The aim of this part of the present programme was to identify the delamination shapes, sizes and positions typical of realistic impact damage. To achieve this a realistic aerospace structural element was chosen for study; the stringer-stiffened panel (Figure 1-6). This type of component is used extensively on fixed wing aircraft, particularly on the wing-skins, fuselage and in the tail surfaces. The geometry of the panel was chosen to be representative of that used in large aircraft.

The damage size was required to be large enough to represent a risk in a real component but small enough to allow the structure to tolerate some damage growth before failure. Dropped tools and maintenance can constitute impact energies of up to 50J on military aircraft; impacts of energies greater than this are considered to be reportable and would be repaired before flight. A damage diameter of between 35mm and 50mm was expected to meet the size requirement. Rather than solely studying the damage from an impact at one location on the stiffened panel, the damage states from impacting a panel at various different locations was investigated.

2.3 Experimental Details

2.3.1 Panel Design and Geometry
The stringer-stiffened panel was designed to withstand an applied compressive strain of 6000με prior to buckling; this is similar to the maximum strain future military aircraft could be expected to withstand. The panel was manufactured from BASF T800/5245 tape which was different from the primary material used in this programme (Hexcel T800/924) but it was hoped that it would give comparable results.

From manufacturing and weight saving considerations, I-section stringers with tapered feet (to reduce skin/stringer debonding) were chosen to stabilise the panel. The panel
Delaminations in Real Structures

delamination geometry and lay-up are shown in Figure 2-1 and a photograph of the fabricated panel is shown in Figure 1-6. Three I-section stringers were cocured onto the skin in a one-shot operation; further details of the manufacture are given elsewhere30. Two panels were manufactured; one for validation of the design and one for impact testing. To validate the design, a compressive test was performed on the first panel, using a Schenck 1000kN servo-hydraulic test machine. A compressive load of 850kN was required to achieve an applied strain of 6000με, and no divergence was noted in the strains, demonstrating that buckling had not occurred. The test was continued up to failure, which occurred at a load of 950kN (about 7000με). Preliminary post-failure analysis indicated that the failure had initiated at a skin/stringer interface.

To allow comparison with impact damage in plain (flat) panels, another laminate was manufactured with the same lay-up as the skin of the stiffened panel.

2.3.2 Impact Testing

Firstly, impact tests were conducted on the plain laminate. This laminate was impacted with energies of 10J, 15J, 20J and 30J at different locations using a free-fall impactor. The drop height was fixed at 1m and the different energies were achieved by changing the impactor masses; the impactor velocity was constant. In each test, the laminate was clamped between two metal rings of 100mm internal diameter, in accordance with CRAG Method 403173. From ultrasonic analyses (Section 2.3.3) the damage caused by each impact was found to be roughly circular, the area of which increased with increasing impact energy. The 15J impact, with damage of about 50mm in diameter, was considered to be the optimum size for further investigation and this impact energy was used for the studies on the stiffened panel.

The remaining stiffened panel was impacted at 15J on the skin face at the locations shown in Figure 2-2. These locations were chosen to maximise the distance between sites and the distance from the panel ends. The impact locations were as follows;

(i) Over the centre of an inter-stringer bay (site A)
(ii) Over a bay, 38mm from the stringer centreline (site B)
(iii) Over a stringer foot, 12.5mm from the stringer centreline (site C)
Over the centreline of a stringer (site D)

All but impact site B were 75mm from the ends of the panel. Ultrasonic analysis of the damage showed it to be approximately the required size (50mm in diameter) in the centre of the bay (site A), but very small beneath the stringer centreline (site D).

2.3.3 Non-Destructive Evaluation

After impacting, the damage was inspected using ANDSCAN, a portable pulse-echo ultrasonic system. The damage area was taken as that which caused a 6dB or more attenuation of the back-face echo with respect to the maximum signal for best material. The position of the damage through the thickness of the laminate was also be determined by measuring the time-of-flight of the reflected signals.

2.3.4 Sectioning and Microscopy

Each damaged area was removed from the panel and cut in half (usually parallel to the 90° plies), to provide sections for optical and electron microscopy; the latter are described and discussed elsewhere. The surfaces to be examined using optical microscopy were ground flat on silicon carbide paper, then polished using diamond and alumina paste. These were then examined using a binocular microscope at magnifications of between 25x and 400x. A calibrated graticule was used to measure the size and position of the delaminations and cracks.

2.4 Experimental Results

2.4.1 Non-Destructive Evaluation

Ultrasonic scans of the impact damages are shown in Figure 2-3 to 2-5. In the plan views of the impact site (the upper part of Figure 2-3, and Figure 2-5) the depth of the damage is indicated by colours; from yellow, near the front face, to mauve near the back face. The surrounding blue area is the undamaged material. The lower part of Figure 2-3 shows a computer-generated oblique view of the damage distribution. The damage areas, peak impact forces and maximum deflections are tabulated in Table 2-1.

The ultrasonic scan of the damage caused by the 15J impact on the plain laminate is shown in Figure 2-3. Individual lobe-shaped delaminations can be seen which when
superimposed produce a roughly circular region of damage, 1550mm$^2$ in area. The lower figure shows the conical distribution of the damage; less damage close to the front face and more at the back face, which is typical of low-velocity impact in thin composite plates$^{172}$.

An ultrasonic scan of the entire stiffened panel after impact testing is shown in Figure 2-4. Undamaged material is shown in black and mauve, while the damage areas and stringers are shown in white, red and yellow. Each stringer appears as a series of parallel white and red bars; the white bars correspond to the centreline of the stringer and to the tapers on its feet, while the red bars correspond to the flat areas in between. All the impacts were of the same energy (15J), except for impact site E (30J). Plan views of impact sites A to D are shown in Figure 2-5.

For the impact over the centre of a bay (impact site A), the vertical red band corresponds to the back face of the skin and the purple and blue areas to each side correspond to the stringer feet. Overall, the damage was approximately elliptical with an area of 1620mm$^2$, but it was enlarged by a single delamination, close to the back face, which extended at 45$^\circ$ from the impact site. Excluding this delamination, the remainder of the damage was roughly circular in distribution. As with the plane panel impact, the damage distribution through the thickness was conical.

For the impact close to the stringer foot (impact site B), the purple region corresponds to the bay and the black regions correspond to the stringer feet. The damage close to the front face was approximately circular and individual lobe shaped delaminations were visible. However, the damage closer to the back face was elliptical, with the major axis of the ellipse parallel to the stringer. The damage towards the back face also extended towards the stringer foot. The plan area was 1750mm$^2$, and the through-thickness distribution was again conical.

For the impact over the stringer foot (impact site C), the purple region corresponded to the stringer and the red regions to the bays. The damage was not circular but elliptical, with
the major axis parallel to the stringer. The damage area was 900mm$^2$ and, in comparison with previous impacts, the through-thickness distribution was more columnar.

The plan area of the damage for the impact over the stringer centreline (impact site D) was considerably smaller (110mm$^2$) than those caused by the other impacts and was slightly elliptical in shape, with the major axis parallel to the stringer. The through-thickness damage distribution was conical.

The largest damage area was generated by impacts in the bay (impact sites A and B) and were slightly larger (between 1620 and 1750mm$^2$) than the area generated by the impact on the plain panel (1550mm$^2$). The damage area from the impact close to the stringer was larger, by 8%, than that generated by the impact in the centre of the bay. As the impact site approached the stringer centreline the damage area dropped, by 44% for the impact over the stringer foot (impact site C), and by 93% for the impact over the stringer centreline (impact site D), with respect to the impact in the bay (impact site A).

Figure 2-6, 2-7 and 2-8 show the variation of various parameters against damage area. In Figure 2-6, as the distance of the site from the stringer increases, so does the damage area. Figures 2-7 and 2-8 show how the peak force and displacements change with damage areas. As the impact force increases, the damage area falls whilst, as the impact displacement increases, so does the damage area.

2.4.2 Optical Studies
Figures 2-9 and 2-10 are schematic diagrams at the impact sites in the plain panel and at the sites A to D in the stiffened panel. It should be noted that these diagrams only illustrate the extent of the damage parallel to the sectioning direction.

Figure 2-9a is a schematic diagram of the delaminations and cracking which occurred in the plain laminate with the section taken parallel to the 0$^\circ$ direction. There was little evidence of damage on the front face, but internally there was delamination, ply cracking and some fibre fracture, all of which were more extensive towards the back face. Directly beneath the impact site, the delaminations were mostly at $+45^\circ/-45^\circ$ ply interfaces, but as
they extended outwards they generally occurred within interfaces which had a $0^\circ$ ply lowermost. The largest delaminations were thus at the $-45^\circ/0^\circ$ and $90^\circ/0^\circ$ ply interfaces.

Figure 2-9b is a schematic diagram of the damage caused by the impact at the centre of the bay (impact site A). The section was taken parallel to the $+45^\circ$ direction and, like the previous impact on the plain laminate, there was very little damage on the front face, but extensive internal delamination and matrix cracking. Directly beneath the impact point the delaminations were at $+45^\circ/-45^\circ$ ply interfaces in the top half of the skin and predominantly at $0^\circ/-45^\circ$ in the bottom half. However, to either side of the impact point the largest delaminations were at $+45^\circ/-45^\circ$ and $0^\circ/90^\circ$ ply interfaces. The large delamination which was noted on the ultrasonic scan was between plies 20 and 21 ($+45^\circ/90^\circ$ interface).

The distribution of damage caused by the impact in a bay, adjacent to a stringer (impact site B) is in Figure 2-10a; the section was taken parallel to the $90^\circ$ direction. There was some front-face damage but the degree of delamination, fibre fracture and matrix cracking increased with depth. The delaminations extended further from the impact site on the stringer side than on the bay side, particularly towards the back face. Directly beneath the impact point the delaminations were mainly at $-45^\circ/0^\circ$ and $+45^\circ/-45^\circ$ ply interfaces, but away from the impact point they were at $0^\circ/90^\circ$ interfaces near the front face and $+45^\circ/-45^\circ$ interfaces near the back face.

The damage at impact site C over the stringer foot is shown in Figure 2-10b; the section was parallel to the $90^\circ$ direction. There was a crush zone on the front face, about 5mm wide and eight plies deep, which consisted of matrix cracking and broken fibres. Directly beneath this zone was a column of material, about 5mm across, which was undamaged and extended through the thickness to the back face of the skin. Either side of this undamaged zone, the delaminations extended to roughly the same distance from either side of the impact point. These delaminations were predominantly at $0^\circ/90^\circ$ interfaces near the front face and $+45^\circ/90^\circ$ interfaces near the back face, with the $90^\circ$ ply always lowermost. In the stringer foot, there was extensive matrix cracking, delamination, and fibre fracture. On the bay side of the impact point the lowermost delaminations extended
Delaminations in Real Structures

to (or initiated at) the surface of the tapered foot and on the stringer side of the impact point they extended into the stringer core region.

The damage from the impact onto the stringer centreline (impact site D) was quite limited and is shown in Figure 2-10c. At the front face there was a small crush zone of broken fibres and matrix cracks, 1mm wide and four plies deep. Below this was a column of undamaged material, which extended to the stringer-core region of the stringer. There was only a limited amount of delamination which was confined to the top half of the skin at 0°/90° ply interfaces. The only other damage was around the stringer core region where matrix cracks and delaminations occurred at the areas of high curvature.

Typical matching fracture surfaces are shown in Figure 2-11 (impact site A), with the uppermost surface (i.e. that closest to the front face) on the left. The extent of the damage is indicated by the dotted red line; the delaminations were between plies 19/20, 20/21 and 21/22 (-45°/+45°, +45°/90° and 90°/0° interfaces respectively). Closer examination showed that the shape was due to preferential growth of the delamination parallel to the fibre direction in the lowermost ply at the interface.

As was noted in the previous Section, in the plain laminate and at impact site A on the stiffened panel, the predominant mode of failure was delamination, although there was also some limited fibre and matrix debris at the impact site. As the impact site approached the stringer (impact sites B, C and D), damage at the front face included more transverse cracking and, ultimately, fibre fracture and front face penetration.

The results of the impact studies are discussed in Section 6.1, where the mechanisms for formation of the damage are identified. The damage size, shapes and ply interfaces chosen for the embedded defect studies (Chapters 4 and 5), based on the findings of this Section, are detailed in Section 4.2.1.
3. **Mixed-Mode Tests**

3.1 **Preliminary Investigations**

3.1.1 **Introduction**

In Chapter 1, mixed-mode test methods were reviewed and two were chosen for further study; the Arcan and Mixed-Mode Bending (MMB) tests. For assessing the test methods, rigs were manufactured and preliminary tests conducted. The results of these preliminary investigations are described in this Section. From these results, the MMB test was chosen for further study and adopted for the programme described for the remainder of the Chapter.

3.1.2 **Arcan Test Studies**

Due to insurmountable difficulties encountered with the Arcan test method and consequent abandonment, the description and results will be kept brief; further details of this test are given elsewhere. Specimens, manufactured from Hexcel T800/924, were prepared to investigate four ply interfaces; 0°/0°, 0°/90°, +45°/-45° and 0°/+45°, using the rig illustrated in Figure 3-1. Load was applied at opposing holes, at a rate of 5 x 10^{-2} \text{ mm/s}, until failure occurred. The test data was reduced using the method described in the references. A number of problems were encountered which were mainly attributed to difficulties with setting-up. This test was time-consuming and very sensitive to misalignment; a slight error in the specimen bonding led to extreme difficulty siting the specimen into the rig and testing. Consequently, only a limited number of specimens were tested.

Firstly, specimens tested under pure mode I loading exhibited very low toughnesses. All the specimens had failed within an off-axis ply close to the mid-plane and exhibited a large degree of fibre bridging (Figure 3-2); similar trends were noted for the mixed-mode tests at 35% mode I. Two 0°/90° ply interface specimens were tested under these conditions but the toughness of one was twice that of the other. However the fracture surfaces were similar (Figure 3-3); at the mid-plane and smooth with regularly
Mixed-Mode Tests

spaced 90° cracks. A +45°/-45° specimen was also tested at this mixed-mode ratio, but
the toughness was again low. The failure plane had changed to a 0°/+45° by migrating
through a crack in the +45° ply; this failure was clearly invalid.

3.1.3 MMB Test Studies

3.1.3.1 Test details

For the MMB studies, two sets of laminates were manufactured; the first for
conventional fracture toughness specimens and the second for modified specimens with
efficient inserts, as described by Robinson and Song. The laminate stacking sequences
are shown in Table 3-1 and were all manufactured from the same batch of Hexcel
T800/924 to dimensions 300mm x 380mm. A 10μm PTFE insert was placed at the mid-
plane, as indicated by an ‘I’ in Table 3-1. For the conventional specimens, this insert
was 100mm wide and was placed at the mid-length. For the modified specimens, a
doubly forked insert (shown as the darker grey in Figure 3-4) was placed at the mid-plane. After curing to the manufacturer’s recommended schedule, specimens
(Figures 3-5a and 3-5b) were cut from the laminates. Prior to bonding the surfaces were
cleaned, grit-blasted and cleaned again. Then, after attaching the hinges, the specimen
edges were painted white and 1mm intervals marked. The length between each of these
intervals and the root of the hinge was measured prior to testing.

Testing was conducted in a servohydraulic test machine with a 1kN load cell; the test rig
is shown in Figure 3-6. Tests were conducted at three mixed-mode ratios; 0%, 68% and
100% mode I. For the 100% mode I, the standard DCB test method was used whilst
for the other two ratios the MMB test method was used. The multidirectional specimens
were tested with the central 0° ply lowermost. After locating in the rig, each specimen
was loaded at a rate of 0.015mm/min. For the 0% mode I tests, a 5mm precrack was
introduced prior to testing, since delamination initiation directly from the insert could
introduce significant scatter. During testing, as the crack extended past each mark along
the specimen edge, the load and cross-head displacement were recorded.
Mixed-Mode Tests

For the 100% mode I tests (DCB), $G_1$ was calculated using Equation 1-4. For the MMB tests, the mode I and mode II toughnesses, $G_1$ and $G_{II}$, were calculated using Equations 1-7 and 1-8 in Chapter 1. To account for the reduced fracture area in the multidirectional specimens, the $w^2$ term was replaced by $w_c w_s$, where $w_c$ was the width of the fractured composite and $w_s$ was the specimen width. This lead to Equations 3-1 and 3-2:

$$G_1 = \frac{3P^2}{w_c w_s h^3 E_{11}} \left[ 1 - \frac{c+b}{2L} - \frac{c}{b} \right] \left[ a^2 + \frac{2}{\lambda} + \frac{h^2 E_{11}}{10G_{13}} \right]$$  \hspace{1cm} (3-1)

$$G_{II} = \frac{9P^2}{4w_c w_s h^3 E_{11}} \left[ 1 - \frac{c+b}{2L} + \frac{c}{b} \right] \left[ a^2 + \frac{h^2 E_{11}}{5G_{13}} \right]$$  \hspace{1cm} (3-2)

where $\lambda = \frac{1}{h} \sqrt{\frac{6E_{22}}{E_{11}}}$

$G_1$ and $G_{II}$ were summed to give the total energy release rate ($G_T$) which was plotted against crack length ($a$) for each specimen. To enable comparison, $G_T$ at crack lengths of 40mm and 50mm was determined for each specimen using the linear interpolation$^{14}$:

$$G_T(x) = G_T(m) + (G_T(n) - G_T(m)) \left( \frac{x - m}{n - m} \right) \quad n > x > m$$  \hspace{1cm} (3-3)

where $G_T$ is known at crack lengths $m$ and $n$, and is to be determined at crack length $x$.

3.1.3.2 Mode I Results and Fracture Morphology

Figure 3-7 shows $G_T$ plotted against crack length for each 100% mode I specimen, whilst averaged results for crack lengths of 40mm and 50mm are tabulated in Table 3-2.

The toughness of the conventional and modified $0^\circ/0^\circ$ ply interface specimens was almost independent of crack length (Figure 3-7) although the scatter was quite high in the former (15%). The fracture surfaces were dark and spectrally reflective; typical of mode I fracture. For the modified specimens, the actual and observed locations of the crack front did not coincide, indicating some opening (anti-elastic bending) of the edges. Also the toughnesses were 20% lower than those of the conventional specimens.

The $0^\circ/90^\circ$ ply interface specimens exhibited very different behaviour to the $0^\circ/0^\circ$ ply interface specimens. The initial toughness was similar, but rapidly increased with crack
Mixed-Mode Tests

length, reaching a peak 200% greater than that in the 0°/0° ply interface specimens. As the crack grew further, the toughness fell, reaching a constant value at crack lengths greater than 60mm. The scatter between the tests was high (20%). The fracture surfaces of the 0°/90° ply interface specimens (Figure 3-8) was very different from those of the previous specimens. The failure had migrated from the mid-plane, through the central 90° ply, into an adjacent 90°/0° ply interface. The central region of the fracture plane exhibited bands of pulled-up 90° plies whilst the outer thirds of the surface had failed within the central 0° ply.

The trends from the +45°/-45° and 0°/+45° ply interface specimens were similar to those from the 0°/90° specimens. The initial toughnesses were similar to that of the 0°/0° ply interface, but rose to a peak, after which the toughness fell. The peak values were considerably greater than the toughness of the 0°/0° ply interface specimens, although the scatter was large. The fracture surfaces (Figure 3-9) exhibited evidence of growth beneath the edge inserts and the main fracture had a herringbone appearance; delaminations had spread towards the specimen edges. On one side the failure was within the lowermost ply whilst the other side was within the uppermost ply. SEM examination indicated fibre fracture, matrix splitting (intralaminar fracture) and multiplane delamination.

3.1.3.3 Mixed-mode Results and Fracture Morphology

The variation of $G_T$ against crack length for each specimen tested at 68% mode I is shown in Figure 3-10. For both the conventional and modified 0°/0° ply interface specimens there was a slight increase in toughness with crack length, with the greater scatter in the modified specimens. Both sets of specimens exhibited similar fracture morphology; failure at the mid-plane and dominated by shallow cusps. However, the conventional specimens had a convex crack front whilst the modified had a concave crack front, and had opened along the edges prior to initiation of growth. Furthermore, the toughness of the modified specimens was 90% greater than that of the conventional, indicating that the toughness had been modified as an artefact of the test method.
For the dissimilar ply interface specimens the behaviour was very different; the initial toughness was similar to that of the conventional 0°/0° ply interface specimens but rose rapidly to toughnesses of over 200% greater, although the scatter was generally high. For the 0°/90° ply interface specimens, the fracture morphology (Figure 3-11) was similar to those tested at 100% mode I; the crack had jumped through the central 90° ply into an adjacent 90°/0° ply interface. The central fracture band was within this interface, growing adjacent to the 0° fibres, and had a grid-like morphology. The outer regions of the fracture plane extended beneath the edge inserts, but the failure was close to the 90° ply, with shear cusps oriented perpendicular to these plies. In the mixed-mode case there was generally less fibre bridging than was observed in the 100% mode I case, although this was clearly still an invalid failure.

The fracture surfaces of the +45°/-45° and +45°/0° ply interface specimens were smoother than were observed at 100% mode I, and the failure was mainly within the +45° ply. Closer inspection revealed there was multiplane delamination (at the ply interface adjacent to the mid-plane) and extensive intralaminar fracture; again an invalid failure.

3.1.3.4 Mode II Results and Fracture Morphology

All the 0% mode I tests were unstable, so only one value of toughness could be determined from each specimen (Table 3-2). The crack lengths were measured by inspection of the fracture surfaces rather than by monitoring the specimen edges during testing.

Firstly, both the conventional and modified 0°/0° ply interface specimens exhibited toughnesses similar to those quoted in the literature and the scatter was less than 10%. The fracture surfaces were dull and flat in appearance and covered in numerous shear cusps; typical of pure mode II failure.

The average toughness of the 0°/90° ply interface specimens was slightly lower than that of the 0°/0° ply interface specimens, but the fracture surfaces were very different;
the crack plane had migrated to a $90^\circ/0^\circ$ interface adjacent to the mid-plane. The surface was split into a central band of shear failure and matrix splits, and outer bands of mode I dominated failure within the $0^\circ$ ply. Clearly, this failure was invalid.

The $+45^\circ/-45^\circ$ and $0^\circ/+45^\circ$ ply interface specimens were 220% and 100% tougher than the $0^\circ/0^\circ$ ply interface specimens respectively. The main failure in the $+45^\circ/-45^\circ$ ply interface specimens was at the mid-plane, whilst in the $0^\circ/+45^\circ$ ply interface specimens it was mainly close to the central $0^\circ$ ply. However, closer inspection of both sets of specimens revealed multi-plane delamination, intralaminar fracture and fibre pull-out. There were also shear cusps on the surface but these were not aligned perpendicular to the fibres as observed in conventional specimens. In the $0^\circ/+45^\circ$ ply interface specimens there were ‘islands’ of failure adjacent to the $+45^\circ$ ply (Figure 3-12).

3.1.3.5 Discussion

The conventional $0^\circ/0^\circ$ ply interface specimens gave consistent results which compared favourably with the literature. However, there were a number of factors which still needed to be investigated. Most notably, the effect of rig geometry, data reduction scheme and crack length on toughness and mixed-mode ratio. These are discussed in more detail in Section 3.2.

The preliminary assessment of the modified specimen design highlighted a number of difficulties. Edge opening and crack front curvature led to discrepancies between the perceived and actual crack tip positions. The subsequent errors in crack length measurement were compounded by the data reduction, leading to large errors in the results. The degree of edge opening was related to the transverse bending stiffnesses ($D_Y$) of the sublaminates; the lower the bending stiffness, the greater the edge opening. The transverse bending stiffnesses are tabulated in Table 3-3 for the specimens under investigation; some of these values are the average of the individual sublaminates. In addition, the crack front curvature can be characterised by the parameter $D_C$; when $D_C$ is large, crack front curvature increases. The transverse bending stiffnesses of all the laminates were low, particularly of the $0^\circ/0^\circ$ ply interface specimens which explains the
Mixed-Mode Tests

discrepancy between the conventional and modified results. Therefore, to overcome edge opening, the stacking sequences had to be adjusted.

A serious problem with multidirectional ply interfaces were failure modes (multi-plane delamination and fibre fracture) which were not representative of single plane delamination growth. In particular, crack migration which led to growth at interfaces other than the mid-plane, invalidated the data reduction. To address this problem, the micromechanisms of failure should be considered, as discussed in detail elsewhere\textsuperscript{29}.

Consider the crack growth from the delamination tip. The relatively high toughness of the interply resin zone means the crack plane migrates towards one ply; this leads to one fracture surface being dominated by fibres and the other by fibre imprints. Mixed-mode delamination growth is through coalescence of microcracks ahead of the main crack front and these microcracks are generated by the resolved local tensile stress in the resin. In the MMB test (Figure 3-13), these stresses act to rotate the crack plane from the horizontal, leading to the crack migration towards the upper ply of the central interface. The coalescence of these microcracks leads to the shear cusps observed on mixed-mode and mode II fracture surfaces.

Considering two different ply interfaces; 0°/0° (Figure 3-14a) and 0°/φ (Figure 3-14b). At the 0°/0° ply interface, once the fracture plane has migrated to the upper ply (0°), the fibres constrain further upward propagation into the ply and so the crack plane remains adjacent to the fibres of the upper ply. Even if the crack tip encounters a local inhomogeneity, such as voids, which lead to propagation towards the lower ply, the driving force will again promote migration towards the upper ply.

At the 0°/φ ply interface (upper ply is φ), the fibres of the upper ply did not restrain crack propagation upwards through the ply, leading to crack migration though the φ ply (Figure 3-14b). Deep cracks developed into this ply and eventually, when the crack front encountered the next ply interface, there was a change in the crack plane. For the 0°/90° and 0°/+45° ply interface specimens, the interface adjacent to the mid-plane had
Mixed-Mode Tests

the same ply orientations but the uppermost ply was now a 0°. The crack plane was now constrained by the upper ply (0°) leading to growth within this interface.

These mechanisms indicate that, to avoid crack plane migration, the uppermost ply should be parallel to the global crack growth direction (0°). However, in the +45°/-45° ply interface specimens, this was clearly not feasible and crack plane migration would always occur. As a direct consequence of this mechanism, in real structures delaminations always grow parallel to one of the plies at a delaminated interface; growth at a +45°/-45° ply interface will locally be a 0°/90° ply interface. Consequently, testing of +45°/-45° ply interfaces was abandoned since it would not be observed under real conditions. Due to material constraints, testing on 0°/+45° ply interfaces was also abandoned.

The damage process shown in Figure 3-13 was valid for mixed-mode and mode II loading, but under pure mode I loading the shear terms (τ₁₁) would be negligible. The crack plane would nominally be horizontal, not favouring one plane of the interply boundary. In practice, local inhomogeneities lead to bifurcation of the crack front and, in ply interfaces other than 0°/0°, will lead to crack plane migration. Therefore, for non-zero ply interfaces, pure mode I testing was not feasible.

3.1.4 Problems and Recommendations

Of the two test methods investigated the MMB test was clearly less problematic. There were significant difficulties with the Arcan test; principally the specimen alignment was very critical. Furthermore, the results exhibited a large degree of scatter and many failures had not occurred at the mid-plane. A further difficulty was that the mixed-mode ratio at a given loading angle varied with the specimen stiffness, making comparison between different ply interfaces problematic.

For the unidirectional ply interface tests, the MMB method gave results which were consistent with the literature and the scatter between identical tests was below 10%. The specimens were easy to manufacture, using the CRAG specification173, and the tests
Mixed-Mode Tests

were relatively quick and simple. Difficulties were encountered with the multidirectional ply interface specimens, although with a revised stacking sequence and specimen orientation these should be overcome.

3.2 Validation of the MMB test and specimen design

3.2.1 Introduction

In this Section, a number aspects of the MMB test were investigated to ensure consistent results, and to finalise the design and test procedure. The validation was conducted for both the unidirectional and multidirectional specimens and two sets of laminates were manufactured; 0°/0° and 0°/90° ply interface specimens. The first set of laminates (0°/0° ply interface) were to investigate the following aspects of the MMB test:

i. Optimise specimen manufacture
ii. Effect of the rig geometry on toughness
iii. Effect of the data reduction scheme on toughness
iv. Effect of crack length on mixed-mode ratio

The second set (0°/90° ply interface) were to investigate the following aspects:

i. Optimise specimen manufacture
ii. Effect of specimen orientation during test
iii. Effect of edge opening and crack length measurement

3.2.2 Validation Specimen Manufacture and Testing

Two laminates were manufactured using the lay-ups indicated in Table 3-1. The 0°/0° ply interface laminate had the same lay-up as had been previously used but was manufactured from BASF T800/5245. The lay-up of the 0°/90° ply interface laminate had an increased transverse bending stiffness $D_Y$ (Table 3-3) to reduce edge opening during deformation but to achieve this, the thickness was increased to 4mm. This laminate was manufactured from Hexcel T800/924, although from a different batch to that used in the previous tests. The specimen manufacture was the same as that described previously in Section 3.1.3.1, except the inserts were inspected prior to lay-up to ensure there were no tears or nicks. For the 0°/90° laminates, the doubly forked insert (Figure 3-4) had slightly different dimensions; the gaps between prongs were 12mm and
Mixed-Mode Tests

each prong was 27mm wide. Subsequently, the $0^\circ/90^\circ$ specimens (Figure 3-5c) were 36mm wide; this width increase was to reduce the relative variation in fracture surface width and minimise any edge effects. To avoid hinge misalignment, a simple rig was developed which clamped the hinges and specimens during bonding. This ensured that the hinges were perpendicular to the specimen edges, aligned along the centreline and were friction free. Each specimen was inspected for hinge misalignment after bonding.

The rig described in Section 3.1.3.1 was used for the testing. For the $0^\circ/0^\circ$ ply interface specimens, tests were only conducted at 50% mode I, but two different rig geometries were used to achieve this. Firstly, a short fulcrum (b) of 55mm, with the lever length (c) set at 47.0mm, and a longer fulcrum of 80mm, with the lever length set at 27.3mm. The $0^\circ/90^\circ$ ply interface specimens were tested at three loading conditions; 100%, 75% and 0% mode I. The 75% mode I test used a fulcrum of 80mm and a lever length of 41.4mm whilst the 0% mode I test used a fulcrum of 55mm and a lever length of 18.3mm. For both these tests the lower span ($2L$) was fixed at 110mm and all the test specimens were precracked at 75% mode I. For the 100% mode I test, a standard DCB test was used\(^{3,73}\).

3.2.3 The Effect of Rig Geometry on Toughness

One problem identified with the MMB test was the short maximum crack length although this can be overcome by increasing the fulcrum (‘b’ in Figure 3-6). Tests were conducted to ensure that the same loading conditions (mixed-mode ratio) could be achieved with different rig geometries. The results are shown in Table 3-4; only three specimens were tested at each rig geometry, so the scatter was large. However, the results indicated that it was valid to use the larger fulcrum, although this led to higher loads and crack instability which limited which tests could be conducted at the larger fulcrum lengths. As a compromise, mixed-mode ratios of 50% mode I and above would be conducted at the larger fulcrum length whilst those tests below 50% mode I would be conducted at a smaller fulcrum length.

3.2.4 Comparison Between Data Reduction Methods

In the literature, two data reduction schemes have been used for the MMB test. The original scheme developed by Crews and Reeder\(^{109}\) used a correction $\lambda$ to account for
effects such as end-rotation. The more commonly used scheme was suggested by Kinloch et al\textsuperscript{70} and used Equations 1-9 and 1-10 for $G_1$ and $G_{11}$ respectively. The expressions were consistent with those used for the pure mode I and mode II tests\textsuperscript{71}, which made it preferable to use these expressions for the main test programme. There was found to little difference between the two schemes; the Kinloch scheme gave toughnesses of between 3% and 5% greater than the NASA scheme whilst the mixed-mode ratios differed by less than 1%. The effect of the data reduction scheme on the results was clearly negligible compared to the inherent scatter and therefore, for the further tests, the Kinloch scheme was adopted.

3.2.5 Variation of Mixed-mode Ratio with Crack Length

Effects such as fibre bridging can lead to the toughness changing with crack length and it is important to ensure that any such effects can be isolated from changes in mixed-mode ratio with crack length. Examination of some of the validation test results indicated that there was a negligible variation (below 1%) in the mixed-mode ratio with crack length; the proportion of mode II (shear) loading slightly increased. This small effect was attributed to the constraint of the roller ahead of the crack tip. Any significant changes in toughness with crack length using the MMB can be attributed to effects other than changes in mixed-mode ratio.

3.2.6 Specimen Orientation for Multidirectional Specimens

All the preliminary tests on the $0^\circ/\phi$ ply interface specimens (Section 3.1.3) had been conducted with the $\phi$ ply uppermost. Further tests on $0^\circ/90^\circ$ ply interface specimens were used to investigate the effect of specimen orientation on the toughness; the results of the tests are given in Table 3-5 ($a=50\text{mm}$).

Firstly, in the 100\% mode I tests, the displacement of the arms were symmetrical so there was no effect of specimen orientation. The results and fracture surfaces were similar to those seen in the previous tests (Figure 3-7) on the $0^\circ/90^\circ$ ply interface specimens. For the mixed-mode tests one specimen (N5) was tested in the same orientation as the previous specimens; the central $90^\circ$ ply uppermost. This specimen exhibited a high toughness and the fracture surface again indicated an invalid failure.
Mixed-Mode Tests

mode. However, the other specimens (N7, N8 and N9) were tested with the central 0° ply uppermost. The toughnesses were significantly lower than in specimen N5, the scatter was low and, as in the previous 0°/0° ply interface specimens, there was little effect of crack length on the toughness. The fracture had occurred at the mid-plane of the specimen, close to the 0° ply, leaving a surface almost identical to that observed in the 0°/0° ply interface specimens. There was no evidence of intralaminar or fibre failure, suggesting that the test was valid.

Finally, the toughnesses of the specimens tested under 0% mode I loading exhibited the same trends to those tested under mixed-mode conditions. Specimens N10 and N11 were precracked and tested with the central 0° ply uppermost. The toughness of these specimens was lower than that of the 0°/0° ply interface specimens but the fracture surfaces were very similar; covered in shear cusps and failure adjacent to the central 0° ply. However, one specimen (N12) was tested with the central 0° ply uppermost but had been precracked with the 90° ply uppermost. The precracked region exhibited a change in fracture plane, as well as intralaminar and fibre fracture, accounting for the high toughness.

3.2.7 Edge Opening in Multidirectional Specimens

In Section 3.1.3 the discrepancy between the observed and actual crack front positions in the multidirectional specimens was attributed to low transverse stiffness. Using classical laminate theory, for a given radius of curvature along the length, the transverse radius of curvature in the original 0°/90° ply interface specimens was 50% lower than that in the revised 0°/90° ply interface specimens. Ultrasonic inspection indicated that, in the revised 0°/90° laminates the discrepancy in crack length was less than 3mm.

3.3 Experimental Details

3.3.1 Test Programme

There were two aims of the mixed-mode testing. Firstly, to characterise failure loci (G_I versus G_II) under different conditions, and thus develop an understanding of the processes which controlled the loci. These failure loci were used in conjunction with
finite element models in Chapter 5 to predict delamination growth from embedded defects. Secondly, the test specimens were used to characterise the fracture morphology under different loading conditions, linking experimental observations and modelling predictions. Furthermore, fractographic observations from surfaces generated under controlled loading and environmental conditions can be used during post-mortem analysis of full-scale structural failures. Therefore, a further aim was to determine if the mixed-mode loading ratio and moisture content could be quantified from the fracture morphology.

Two materials were chosen for study; BASF T800/5245 and Hexcel T800/924. Both are currently used on military aircraft and have the same fibre type; Toray T800. However, the matrix systems are very different. BASF 5245 is a bismalamide/epoxy mix whilst Hexcel 924 is a modified epoxy system. The latter system was used in the studies described in Chapters 4 and 5.

Since moisture has a large effect on the resin dominated properties, for the unidirectional specimens (0°/0° ply interface), two moisture conditions were investigated. Firstly, tests were conducted on dry laminates (designated ‘DRY’), such as with aircraft structures in arid conditions. The second moisture condition (designated ‘WET’) was the equilibrium content from conditioning at 84% RH at 60°C; this extreme condition is the worst which is likely to occur in service\textsuperscript{174}. The 0°/90° ply interface specimens were only investigated in the DRY state, and the tests described in Chapter 4 were under these conditions.

For the unidirectional specimens (0°/0° ply interface) tests were conducted at nine mixed-mode ratios; from 0% mode I to 100% mode I in steps of 12.5%. At each mixed-mode ratio, at least five specimens were tested. The tests were all conducted using the MMB rig except for those at the 100% mode I which were conducted using the standard DCB test. For the 0°/90° ply interface specimens the tests were conducted at fewer mixed-mode ratios; 0%, 25%, 50%, 75% and 87.5% mode I loading. These specimens were all tested with the 0° ply uppermost to ensure valid failures.
Mixed-Mode Tests

To generate supplementary information for the data reduction, material property tests were also conducted. The CRAG\textsuperscript{173} tests 300 and 301 were used to determine the values of axial and transverse stiffness ($E_{11}$ and $E_{22}$) respectively. In addition, CRAG tests 101 and 302 were used to determine the in-plane shear stiffness ($G_{12}$) and the flexural stiffness $E_f$. The stiffnesses of the $0^\circ/90^\circ$ laminates were predicted using classical laminate theory from the unidirectional property data.

3.3.2 Specimen Manufacture and Preparation

Using the stacking sequences indicated in Table 3-1, panels of dimensions 450mm x 300mm were laid-up. Five and three panels were manufactured for the $0^\circ/0^\circ$ and $0^\circ/90^\circ$ ply interface specimens respectively. PTFE film was inserted at the mid-plane, as described in Section 3.1.3.1, ensuring no tears or wrinkles were introduced during manufacture. Unidirectional laminates and angle ply laminates from both materials were manufactured for the material property tests, the details of which are given elsewhere\textsuperscript{173}. All the laminates were cured and post-cured according to the manufacturer’s specifications, after which they were C-scanned to ensure high quality. After cutting to the dimensions shown in Figure 3-5a and 3-5c, each MMB specimen was labelled and the dimensions measured using a digital vernier at three locations for the width and thickness.

Selected specimens (travellers) were used to monitor all the specimen weights during conditioning. Firstly, all the specimens were dried in a vacuum oven at 60°C until there was no further weight loss. Half the specimens (DRY) were then stored in a desiccator until testing. The other half (WET) were conditioned to equilibrium at 84% RH, 60°C and then stored at this condition until testing. Within two weeks of testing, the specimens were hinged as shown in Figure 3-5 and described in Section 3.1.3.1. Prior to bonding, the bonded surfaces were briefly dried in a vacuum oven to reduce moisture ingress into the adhesive during cure. After bonding, the hinges were inspected to ensure alignment with the specimen edges.
3.3.3 Specimen Testing, Data Reduction and Fractography

Before testing, all the specimens were precracked under a mixed-mode loading ratio of 75% mode I, to give an initial crack length of approximately 30mm. After precracking the edge of the crack front was labelled and the specimen removed from the rig.

The main tests were conducted as described in Section 3.1.3.1. The lower span of the rig (2L) was kept at 110mm and the fulcrum (b) and the lever length (c) were varied to generate different mixed-mode loading ratios (Figure 3-6). The tests were conducted for mixed-mode ratios of between 87.5% and 50% mode I loading with b=80mm whilst for mixed-mode ratios of between 37.5% and 0% mode I, b=65mm; the rig geometries are tabulated in Table 3-6. The tests were stopped before the delamination reached the central roller. After each test the final position of the crack front on the edge was marked and this was later compared to the actual position. The 100% mode I tests were conducted according to the CRAG procedures.

After testing, typical specimens were chosen and the fracture surfaces examined using optical microscopy at magnifications of between 10x and 100x. Selected fracture surfaces were then gold sputter coated and examined using a scanning electron microscope (Hitachi S450) at magnifications of between 50x and 20000x. Micrographs of the surfaces were taken using a video capture and archiving system. These were later imported into an image analysis package to allow measurement of the surface detail.

The fracture toughness data was reduced using a Microsoft Excel spreadsheet. The rig and specimen geometries, recorded values of load, displacement and crack length were input into the spreadsheet. The values of longitudinal, transverse and in-plane shear stiffness (E_{11}, E_{22} and G_{12}) determined from the material property tests, were also used. The values of G_{I} and G_{II} for each recorded value of crack length were output, using both the Crews and Reeder expressions (Equations 1-7 and 1-8) and the Kinloch expressions (Equations 1-9 and 1-10). A number of correction factors were calculated; (Equation 1-9), \Gamma (Equation 3-6) and \lambda (Equations 1-7 and 1-8). These were given by.
Mixed-Mode Tests

\[ \chi_1 = \sqrt{\frac{E_{11}}{11G_{12}}} \left[ 3 - 2\left( \frac{\Gamma}{\Gamma + 1} \right)^2 \right] \]  

(3-4)

\[ \chi_{II} = 0.42\chi_1 \]  

(3-5)

\[ \Gamma = 1.18 \frac{\sqrt{E_{11}E_{22}}}{G_{12}} \]  

(3-6)

The total strain energy release rate, \( G_T \) (=\( G_I + G_{II} \)) and the mixed-mode ratio, \( G_I/G_{II} \), were output for each recorded value of crack length. Graphs of \( G_I \), \( G_{II} \) and \( G_T \) versus crack length \( a \) were also plotted.

The data from each test (i.e. \( a \), \( G_I \) and \( G_{II} \)) were imported into a spreadsheet which contained all the results. These results were then analysed using linear regression to give \( G_I \) and \( G_{II} \) as functions of crack length, \( a \), for each test;

\[ G_I = m_I a + c_I \]  

(3-7)

\[ G_{II} = m_{II} a + c_{II} \]  

(3-8)

where \( m_I \), \( m_{II} \), \( c_I \) and \( c_{II} \) were constants. The accuracy of these expressions was checked against the original data and, where the error between the original and predicted values was greater than 10\%, the following weighted interpolation formula was used:

\[ G(a) = G_k + (G_{k+1} - G_k) \left( \frac{a-a_k}{a_{k+1} - a_k} \right) \]  

(3-9)

where the required value \( G(a) \) is between the known values \( G(a_k) \) and \( G(a_{k+1}) \). A difference of 10\% was chosen since this is the scatter usually observed in composites.

Using Equations 3-7 to 3-9, \( G_I \) and \( G_{II} \) were determined for each test at arbitrary values of crack length. Therefore, for each set of tests, the average values (\( G_I \) and \( G_{II} \)) and the coefficient of variation (\( C_V \)) for arbitrary values of crack length could be determined. The overall scatter for each mixed-mode ratio was given by averaging for all the results between crack lengths of 30mm and 70mm.

The percentage R-curve, which represents the increase in \( G_T \) with crack length, was determined for each test using Equation 3-10;
Mixed-Mode Tests

\[
R = \frac{G_{a=60} - G_{a=40}}{G_{a=40}}
\]  

(3-10)

where \(G_{a=40}\) and \(G_{a=60}\) were the values of \(G_T\) at crack lengths of 40mm and 60mm respectively. The values of \(G_T\) at \(a=40\)mm and 60mm were chosen since this was the range in which the effect of the starter crack (30mm) and the central roller (65mm or 80mm) would be minimised. The average \(R\) was then determined for each mixed-mode ratio. Finally, the values of \(G_I\) and \(G_{II}\) at constant values of crack length (e.g. \(a=40\)mm) were plotted to generate failure loci.

3.3.4 Moisture Conditioning Results

During conditioning T800/5245 continued to exhibit a slight increase in weight even after 100 days at the final conditioning stage. This was attributed to irreversible chemical changes in the matrix, as have been previously observed in this system\(^{175}\). Testing of these specimens was conducted when the moisture content had reached about 0.7% by weight. The T800/924 specimens quickly levelled out to a constant moisture content of 1.4% by weight. The scatter between specimens was less than 3%.

3.3.5 Material Property Test Results

The material property results are shown in Tables 3-7 and 3-8. Unfortunately, due to problems with debonding of the end-tabs, only flexural tests could be conducted on both dry and wet specimens, with all the other tests only conducted on the dry specimens.

3.4 Mixed-mode Test Results

3.4.1 Details of the Experimental Results

The MMB tests were reasonably successful, with scatter generally below 10%. However, problems were encountered due to misaligned hinges, crack instability and hinge debonding.

The largest source of error was attributed to misaligned hinges. For pure mode I tests (DCB) this was not a significant problem since the specimen was relatively free to move, thus minimising secondary loading. However, in the MMB test (Figure 3-6), the
specimen was highly constrained so any hinge misalignment could result in the introduction of a mode III component or a change in the mixed-mode loading ratio. This was particularly prevalent under mode II dominated loading, where a small change in lever length resulted in large changes in mixed-mode ratio. When the hinges were misaligned, this also resulted in a variation in mixed-mode ratio across the specimen width. After testing, the results from any specimens with slanted crack fronts, symptomatic of specimen misalignment, were rejected.

The tests conducted under mode II dominated loading also encountered difficulties with crack instability. Once growth had initiated the crack would propagate very quickly. This made it impossible to accurately record the crack length and introduced large errors into the data reduction. In some cases only one value of toughness could be determined with any confidence from each specimen. Attempts were made to overcome this problem by loading the specimens at lower rates, but this could introduce creep effects and led to difficulties with controlling the test machine. To simplify the analysis and reduce errors, only data generated by stable tests were included in the failure loci.

Some problems with hinge debonding were encountered in the wet specimens. From inspection of the bonded surfaces this attributed to poor surface preparation.

In the 0°/90° ply interface tests a number of other problems arose. Firstly, although edge opening had been reduced by optimising the lay-up, some difficulties still occurred. At a given mixed-mode ratio, the opening of the crack tip was harder to distinguish than in the 0°/0° ply interface laminates; under mode II dominated loading identification of the position of the crack tip was very difficult. Fortunately, at these mixed-mode ratios, the growth was often unstable so the crack growth was not monitored during testing. A further aspect which may have contributed to this problem was that the hinges were narrower than the specimen width.

As a consequence of the difference in stacking sequence between the 0°/0° and 0°/90° ply interface laminates, the axial stiffness of the former was over twice that of the latter
Mixed-Mode Tests

However, the $0^\circ/90^\circ$ ply interface specimens were wider and thicker, so the second moment of area was much larger; consequently the bending stiffness term, $E I$, was 20% larger. At a given crack length this resulted in slightly different arm deflections between the $0^\circ/0^\circ$ and $0^\circ/90^\circ$ ply interface specimens, but this difference was not considered large enough to have introduced discrepancies in the results.

3.4.2 Test Results for Unidirectional T800/5245 and T800/924

The pure mode I and II toughnesses ($G_{IC}$ and $G_{IIIC}$) generated from the tests are tabulated in Table 3-9, although only published data from testing dry laminates was available for comparison. Also, due to crack instability, scatter for $G_{IIIC}$ could not be determined and the values given at the higher crack lengths were extrapolated from the test data. The values of $G_{IC}$ agreed quite well with the published data although the values for T800/5245 were strongly dependent on the crack length. The values of $G_{IIIC}$ at short crack lengths (less than 40mm) agreed with the published data, but at higher crack lengths these proved to be unrealistic.

The unidirectional results are tabulated in Tables 3-10, 3-11, 3-12 and 3-13, and the failure loci generated (with the unstable results omitted) are shown in Figures 3-15 to 3-16. The combination of linear regression and weighted interpolation modelled the data accurately. In the pure mode I tests (DCB) the accuracy of the data could be checked by back calculating the specimen stiffness, which was constant.$^{89}$ Unfortunately, when the tests were conducted, such a check was not available without using a compliance calibration (Section 1.4.4). The accuracy of the results could only be gauged by considering the scatter and the pure mode I and II toughnesses ($G_{IC}$ and $G_{IIIC}$). The first tests conducted were on dry T800/5245 (Table 3-10) which exhibited a large degree of scatter; this was attributed to lack of familiarity with the test method. The results for wet T800/5245 (Table 3-11) had a lower scatter. For both dry and wet T800/924 (Tables 3-12 and 3-13) the scatter was quite high at short crack lengths, particularly under mode II dominated loading where instability occurred. However, at higher crack lengths the scatter was reduced.
The large scatter for the failure loci of the dry T800/5245 (Figure 3-15a) limited the usefulness of these results. However, there was a 'hump' in the data close to the G\textsubscript{1} axis (87.5\% mode I), particularly at large crack lengths. This implied that when a small mode II component was introduced to pure mode I loading, the components interacted positively to enhance the toughness; a similar hump has been noted by other workers\textsuperscript{67}. For mixed-mode and mode II dominated loading the loci were almost linear, which implied that there was little or no overall interaction between the mode I and mode II components and a simple rule of mixtures governed the toughness.

In the failure loci for wet T800/5245 (Figure 3-15b), for the shorter crack lengths, the hump noted in the dry data was more evident and the remainder of the locus was slightly concave; there was a negative interaction between the mode I and mode II components. For the longer crack length, the locus was similar to that for the shorter crack length although there was a much larger hump.

The failure loci for dry T800/924 (Figure 3-16a) were very different from those of the dry T800/5245 (Figure 3-15a). Crack length had little effect on the loci and both loci were convex, with no evidence of a hump near the mode I axis. The failure loci for the wet T800/924 were relatively linear and, unlike the dry case, there was some evidence of a hump close to the G\textsubscript{1} axis.

Figures 3-17 and 3-18 compares the average failure loci for crack length and material type respectively. Firstly, for T800/5245 (Figure 3-17), there was an increase in toughness with crack length for mode I dominated and mixed-mode loadings, particularly for wet laminates. However, for mode II dominated loading, there was no significant effect of crack length on toughness. For T800/924, there was a negligible effect of crack length on toughness, the largest effect being under mode I dominated loading.

As can be seen in Figure 3-18, there as a clear difference in toughness between T800/924 and T800/5245 tested under the same conditions. In dry material at short
Mixed-Mode Tests

crack lengths, the toughnesses were similar, except under mode II dominated loading where T800/924 was the tougher. However, at large crack lengths, particularly under mixed-mode and mode I dominated loading, T800/5245 was the tougher system. In wet laminates at short crack lengths, T800/924 was the tougher except under mode I dominated loading. However, at large crack lengths, T800/5245 was significantly tougher, particularly under mode I dominated loading. The toughnesses of the two materials were only comparable under mode II dominated loading.

An example of the $G_T$ versus crack length curves for dry T800/5245 and T800/924 at 75% mode I loading is shown in Figure 3-19. Clearly, crack length had an important effect on the toughness in T800/5245 but little effect in T800/924. This is illustrated further in the R-curve results (Equation 3-10) which are plotted against mixed-mode ratio in Figure 3-20. Unfortunately, there was a large degree of scatter in all these results which limited the conclusions which could be drawn. The cause of this scatter was unclear but it may have been due to the inherent errors in $G_i$ and $G_{II}$ being compounded when substituted into Equation 3-10. Also, one of the main contributors to the R-curve effect is the mechanism of fibre bridging; this makes a large contribution to the toughness and only a small variation in fibre bridging would result in relatively large variations in R.

Firstly, T800/5245 exhibited a very large R-curve effect, particularly under mode I dominated loading, although there was a sharp drop in the R-curve under pure mode I loading. In wet laminates, at 87.5% mode I the toughness doubled over only 20mm of crack growth. As the proportion of mode II increased, R dropped and, for wet laminates, this trend continued up to high mode II loading ratios. There was no discernible effect of moisture on the R-curve in T800/5245.

The T800/924 exhibited a much smaller R-curve effect than T800/5245 and it was impossible to distinguish any effect of moisture. The overall variation in R with mixed-mode ratio was also similar to T800/5245; a maximum under mode I dominated
Mixed-Mode Tests

loading, with a sharp drop at pure mode I loading, and a steady fall in R with increasing mode II component.

3.4.3 Test Results for Multidirectional T800/924

The results of the tests on the 0°/90° ply interface specimens are shown in comparison with the 0°/0° ply interface results in Table 3-14. Since this material exhibited little effect of crack length on toughness, the values from the unstable tests are also given which have been averaged over a range of crack lengths (35mm to 45mm). The variation in $G_T$ versus crack length is shown in Figure 3-21 for mixed-mode ratios of 87.5%, 75% and 50% mode I for both 0°/0° (red) and 0°/90° (blue) ply interfaces.

Under 87.5% and 75% mode I loading there was little difference between the 0°/0° and 0°/90° ply interface results. Although there was a small increase in toughness with crack length for the 0°/0° ply interface, there was no effect of crack length at the 0°/90° ply interface. For the 0°/0° ply interface specimens, the curves were quite straight and exhibited little oscillation with crack length. However, the 0°/90° ply interface specimens exhibited considerable oscillation. At 50% mode I loading the 0°/90° ply interface was clearly the tougher and the two sets of results did not overlap.

Since there was little overall variation in toughness with crack length for this material, particularly for the 0°/90° ply interface specimens, the toughnesses were averaged over all crack lengths (30mm to 60mm). In both 0°/0° and 0°/90° ply interfaces the variation in toughness with crack length was typically below 10%, which was of the same order as the specimen variation. By using the averaged toughness, the results from the unstable tests could also be included, as shown in Table 3-15 and Figure 3-22. In this Figure the individual results are shown as points and the average of each set of points is shown as a line; the failure locus. The shape of the loci were very different. The 0°/0° ply interface locus, as discussed in the previous Section, was almost linear and had a slightly concave region under mixed-mode conditions. However, the 0°/90° ply interface locus was very convex, exhibiting a hump at the 50% mode I region. Comparing the two sets of data, under mode I or mode II dominated loading, the 0°/0°
and 0°/90° ply interfaces exhibited similar toughnesses. However, under mixed-mode loadings of between 75% and 25% mode I, the 0°/90° ply interface was up to 42% tougher.

### 3.4.4 Specimen Compliance Calculations

For mode I testing (DCB), the results can be checked by back-calculating the specimen stiffness during test, and comparing this to the known stiffness from the material property tests. For the MMB test method an expression was derived for the specimen stiffness, $E_{11}$, in terms of the applied forces, end displacements, and specimen and rig geometries (Appendix A):

$$E_{11} = \frac{P P_0 (K - K_0)}{(\delta P_0 - \delta_0 P)} \quad (3-11)$$

where $P$ and $\delta$ are the load and deflection, $P_0$ and $\delta_0$ are the load and deflection at the start of crack growth and:

$$K = \frac{(M + N)}{3} a^3 + \left(M x_1 h + 0.42 N x_1 h\right) a^2 + \left(M + 0.42^2 N\right) (x_1 h)^2 a \quad (3-12)$$

$$M = \frac{6}{w h^3} \left(1 - \frac{c + b}{2l}\right) - \frac{c}{b} \quad (3-13)$$

$$N = \frac{9}{2 w h^3} \left(1 - \frac{c + b}{2l}\right) + \frac{c}{b} \quad (3-14)$$

The stiffness of selected specimens was calculated using these expressions and the results are tabulated in Table 3-16. These values exhibited a high degree of scatter, particularly at large crack lengths. However, due to the high scatter and predicted values being much lower than the experimental values, this approach was abandoned.

### 3.5 Fractographic Analysis

#### 3.5.1 Introduction

After testing, the fracture surface morphology was studied. By characterising the fracture morphology under controlled conditions, the failure modes and loading conditions in the embedded delamination tests (Chapter 4) could be ascertained. Also, understanding of the detailed damage mechanisms during mixed-mode delamination...
growth is a basis for the finite element models developed in Chapter 5. Finally, through understanding the delamination growth processes, their effect on the failure criteria could be determined. This ensures that the failure criteria developed for delamination growth have some physical basis, which improves confidence when using them for design of composite structures.

3.5.2 Fracture Surface Morphology for MMB Specimens

3.5.2.1 Unidirectional Specimens

Low magnification micrographs of the fracture surfaces at different mixed-mode ratios are shown in Figures 3-23 and 3-24. These micrographs are of dry T800/5245 and T800/924 specimens at a magnification of 1000x and a surface tilt of 7°. Two micrographs were taken at different locations to illustrate the variation in surface morphology within each specimen. A number of duplicate specimens were also examined to quantify the variation in surface morphology between specimens tested at nominally identical conditions; this variation was negligible compared with the changes in surface morphology with mixed-mode ratio. To illustrate the surface morphology in more detail, in particular the cusp tilt and shape, further micrographs (Figure 3-25 to 3-26) were taken at magnifications of 5000x and a tilt of 65°. Background information on the fracture morphology of delamination is discussed in Section 1.6.

The fracture morphology of the dry T800/5245 laminates are shown in Figures 3-23 and 3-25. Firstly consider the 100% mode I fracture surface shown at the top of Figure 3-23. The surface was undulated and had numerous bundles of loose and broken fibres (fibre bridging). The matrix fracture was quite brittle, exhibiting feathering and riverlines. As the proportion of mode II increased, aligned scars and some very shallow shear cusps appeared. The latter (Figure 3-25), were thin, irregular and poorly formed. On a few of these features, riverlines in the matrix had developed from the fibre/matrix interfaces to form serrated feet.

Under 50% mode I loading the fracture surface was flatter and there were only small regions of fibre bridging. The matrix failure was still brittle but dominated by more
Mixed-Mode Tests

upright cusps. As can be seen in Figure 3-25, many of these cusps exhibited serrated feet. As the proportion of mode II fracture increased (25% mode I loading), the failure of the matrix was dominated by thicker and regularly shaped cusps which were more upright but had limited serrated feet. There were also loose pieces of matrix and fibres covering the surface. Under pure mode II loading, the degree of debris had increased significantly and the surface was dominated by very upright regular shear cusps. However, these cusps had smooth edges with no evidence of serrated feet.

Under pure mode I loading the fracture surfaces of wet T800/5245 exhibited extensive fibre bridging, but the appearance of the matrix was very different from the dry case; dimpled and rough, suggesting plasticity. There was no evidence of riverlines or feathering but the surface was covered in indistinct opposing scarps. As a mode II component was introduced, there was more fibre bridging and the matrix failure was more brittle, dominated by aligned scarps and shallow cusps. Under 50% mode I loading, there was a decrease in the degree of fibre bridging. The surface was dominated by irregular cusps which were less distinct than those in the dry laminates. As the mode II component increased further there was little fibre bridging and the matrix failure was dominated by irregular but upright shear cusps. Under pure mode II loading the fracture surface was covered in debris and shear cusps which were numerous, block-like and occasionally loose and broken.

The fracture surfaces for dry T800/924 are shown in Figures 3-24 and 3-26. Under pure mode I loading there was limited fibre bridging; much less than observed in T800/5245. The matrix fracture was covered in numerous opposing and some aligned scarps. As the mode II component was introduced (75% mode I) there was still little fibre bridging and the matrix fracture was plastic; covered in aligned scarps and shallow cusps.

Under 50% mode I loading there was no fibre bridging and the matrix fracture exhibited evidence of plastic deformation. The surface was covered in shallow cusps which varied in shape and were dominated by extensive serrated feet (Figure 3-26). As the mode II component increased further (25% mode I loading) there was a large amount of matrix
debris and upright block-like cusps, although the number of serrated feet had reduced. Under pure mode II loading there was much surface debris and the matrix between the fibres had fractured as numerous, and erect, block-like cusps.

For wet T800/924, under pure mode I loading there was some fibre bridging and the matrix fracture was plastic; riverlines were indistinct and rare. As the mode II component was introduced there was an increase in degree of fibre bridging and the surface was covered in aligned scarps and shallow cusps. Under 50% mode I loading there was little fibre bridging and the matrix fracture was less plastic. The surface was covered in shallow irregular cusps and had limited serrated feet. As the mode II component increased further, the amount of surface debris had increased, the matrix had a rough appearance and there were numerous upright cusps. Under pure mode II loading there was a large amount of surface debris. The surface was covered in numerous, erect cusps which were regularly shaped.

### 3.5.2.2 Multidirectional Specimens

The upper and lower fracture surfaces from the 0°/90° MMB specimens are shown in Figure 3-27; the upper fracture surfaces (0° ply) are shown on the left whilst the lower fracture surfaces (90° ply) are shown on the right. The magnification was 1000x with a surface tilt of 7°. In Figure 3-28, high magnification images of the shear cusps from the lower surface are shown, at a magnification of 5000x with a surface tilt of 65°.

Under mode I dominated loading (87.5% mode I), the fracture surfaces were similar to those from the 0°/0° specimens (Figure 3-24). The fibres were separated by aligned scarps and occasional ribs\(^{13}\) extending perpendicular to the fibres. The matrix fracture was quite brittle (cleavage) although there was little evidence of feathering or riverlines; this indicated some degree of plasticity. As the proportion of mode II loading increased, the ribs become more clearly defined and numerous, in some regions having the same spacing as the 90° fibres. On the lower surface the impression of the ribs developed as deep cracks in the interply resin, extending to the 90° fibres on the lower ply. Shear cusps were more prevalent on the lower surface (matrix dominated) and these become
more upright and numerous as the mode II component increased. The impression of these cusps (scallops\textsuperscript{13}) developed between the 0° fibres on the upper surface.

Under mode II dominated loading (25% and 0% mode I), the 0° fibres on the upper surface were quite devoid of resin, although there were isolated islands of resin fracture, similar to those in Figure 3-12. On the matching lower surface, the fracture was dominated by closely spaced shear cusps, interspersed with deep cracks which extended to the 90° fibres, forming a grid pattern.

**3.5.3 Cusp Angles**

An important finding of the fractographic analysis was the variation in cusp shape and the tilt angle with mixed-mode ratio. It was postulated that this parameter could be used to ascertain the mixed-mode loading ratio. To quantify the cusp tilt, this angle needed to be defined. This was complicated by the degree of plasticity which can occur during cusp generation and Figure 3-29 illustrates the variety of tilt angles which could be chosen. \( \alpha \) is the angle of the front face of the cusp while \( \beta \) is the angle of the back face of the cusp; both were measured one third of the way up from the cusp base. \( \gamma \) was the angle of the cusp centreline and \( \delta \) was the angle at the cusp base. From examination of numerous surfaces generated at all mixed-mode ratios, the tilt angle of the back face of the cusp (\( \beta \) in Figure 3-29) was found to be the most consistent and was chosen as the definition of the cusp angle. The average \( \beta \) for the unidirectional material was then determined and these values are shown in Tables 3-17 and 3-18. A minimum of 10 and a maximum of 35 measurements were taken for each mixed-mode ratio, depending on the frequency of the cusps. No values were measured for pure mode I fracture since cusps were not observed on these failure surfaces.

The scatter in the cusp angles was generally below 10% except at high mode I ratios, which was attributed to the difficulties in measuring such small angles. Under mode I dominated loading, the angle was quite low, was not material dependent and was only weakly dependent on the mixed-mode ratio. As the proportion of mode II loading increased, so did the dependence of the tilt angle on the mixed-mode ratio. Under mode
II dominated loading, the cusp angle was material dependent and the tilt angle was strongly dependent on mixed-mode ratio.

Table 3-18 shows the variation in cusp angle with percentage mode I loading for both 0°/0° and 0°/90° ply interfaces (dry T800/924). Under mode I dominated loading, for a given ratio, the cusps on the 0°/90° specimens were the more upright. However, under mixed-mode and mode II dominated loading, for a given ratio, the angles were similar.

3.6 Failure Criteria

3.6.1 Introduction

In Section 1.5, the range of mixed-mode delamination criteria used in the literature were reviewed. In this Section, the mixed-mode data generated with the MMB tests was used to assess these criteria; to identify those which best modelled the experimental data. The results of this assessment were used in Chapter 5 to predict initiation of mixed-mode delamination growth from embedded defects.

Of the sixteen criteria reviewed in Section 1.5, all but five were assessed (Table 1-2). The simplest criteria, such as $G_1$, $G_{11}$ and $G_C$ (Equations 1-13, 1-14 and 1-15) were excluded since the literature had shown that they very poorly modelled experimental data. Parametric criteria, such as the bilinear criteria (Equations 1-26 and 1-27) were excluded due to difficulties in directly comparing the ‘goodness of fit’ of these criteria to those of the other criteria. Only the general form of the Interaction Criterion (Equation 1-22) was investigated.

3.6.2 Details of the Curve Fitting

The data shown in Tables 3-10 to 3-13 and 3-15 were used as the basis for assessing the failure criteria. For each mixed-mode ratio, the average $G_1$ and $G_{11}$ was determined for each set of data at a given crack length, as well as the standard deviations, $\sigma_1$ and $\sigma_{11}$, for the mode I and mode II components respectively. For the unidirectional data, the unstable results were excluded (0% mode I) and, for the multidirectional data the results were averaged over all crack lengths.
The goodness of fit\(^{14}\) between the predicted set of data from each criterion, \(G_1^p\) and \(G_{II}^p\) and the experimental data \(G_i\) and \(G_{II}\) was given by \(\chi^2\);

\[
\chi^2 = \frac{1}{n} \sum \left[ \frac{(G_i - G_i^p)^2}{\sigma_i^2} + \frac{(G_{II} - G_{II}^p)^2}{\sigma_{II}^2} \right]
\]  

(3-15)

where \(n\) was the number of mixed-mode ratios considered.

Consider a criterion \(G_{II}^p = f(G_i^p)\), which has parameters \(A, B\) and \(C\). The optimum values of these parameters were determined by minimising \(\chi^2\); that is the values of \(A, B\) and \(C\) which give the values of \(G_1^p\) and \(G_{II}^p\) closest to \(G_i\) and \(G_{II}\). However, since there was inherent scatter in both \(G_i\) and \(G_{II}\), using the criterion in the form \(G_{II}^p = f(G_i^p)\) was problematic. It was simpler to convert each criterion into the form \(G_T = f(t)\) where \(t = G_i/G_{II}\) and \(G_T = G_i + G_{II}\), since the experimental values of \(t\) had negligible scatter. Therefore the optimum values of the parameters \(A, B\) and \(C\) were determined using the criteria in the form \(G_T^p = f(t)\) where;

\[
G_i^p = \frac{tG_T^p}{(t+1)}
\]

\[
G_{II}^p = \frac{G_T^p}{(t+1)}
\]

(3-16)

All the criteria assessed are shown in the form \(G_T = f(t)\) in Table 3-19. This method could be used if \(G_T\) was an explicit function of \(t\). However, for some criteria the expressions were implicit; could not be put in the form \(G_T = f(t)\). In these instances, the Newton-Raphson iteration method was employed. Consider a criterion:

\[
f(G_T^p, t) = 0
\]

(3-17)

For a given value of \(t\), this function can be expressed as the function \(f(x)\) where;

\[
f(x) = 0 \quad \text{when} \quad x = G_T^p
\]

Therefore, to determine \(x\) for \(f(x) = 0\), the iteration expression is given as\(^{14}\):

\[
x_{i+1} = x_i - \frac{f(x_i)}{f'(x_i)}
\]

(3-18)
By using a starting value of $x_{i=1}$, the value of $x$ for $f(x)=0$ can be determined through successive iterations, thus finding a value of $G_T^p$ for each value of $t$.

For example, consider the Power Law criterion (Equation 1-17):

$$\left(\frac{G_{I}}{G_{IC}}\right)^m + \left(\frac{G_{II}}{G_{IIC}}\right)^n = 1 \quad (3-19)$$

as a function of $G_T$ and $t$, this is:

$$\left(\frac{G_{I}t}{G_{IC}(1+t)}\right)^m + \left(\frac{G_{I}}{G_{IIC}(1+t)}\right)^n - 1 = 0 \quad (3-20)$$

Clearly this cannot be expressed as $G_T=f(t)$. So, for a given value of $t$, this converts to a general form $f(x)$, where $f(x)=0$ when $x=G_T$:

$$f(x) = Cx^m + Dx^n - 1 \quad (3-21)$$

where

$$C = \left(\frac{t}{G_{IC}(1+t)}\right)^m \quad \text{and} \quad D = \left(\frac{1}{G_{IIC}(1+t)}\right)^n$$

Therefore;

$$f'(x) = mCx^{m-1} + nDx^{n-1}$$

thus;

$$x_{i+1} = x_i - \left[\frac{Cx_i^m + Dx_i^n - 1}{mCx_i^{m-1} + nDx_i^{n-1}}\right] \quad (3-23)$$

A starting value of $x_{i=1}$ was given by rearranging the Linear criterion (Equation 1-16):

$$x_{i=1} = \left[\frac{G_{IC}G_{IIC}(t+1)}{tG_{IIC} + G_{IC}}\right] \quad (3-24)$$

For each value of $t$, $x$ was iterated over 100 times and converged to a constant value, such that $f(x)=0$ where $x_{i=\infty}=G_T$.

The curve fitting and goodness of fit tests were conducted using a Microsoft Excel spreadsheet with the ‘solver’ function and a macro which automated the process. The minimisation of $\chi^2$ through optimisation of each criterion was conducted using a quasi-
Newton gradient search method\textsuperscript{176}. For the curve fitting operations the same starting values of the parameters were used for all the data sets.

Once the optimum values of the parameters and the minimum $\chi^2$ for each criterion had been determined, the different criteria could be ranked for each set of data. Two forms of ranking were used. Firstly, the minimum $\chi^2$ values, which gave a measure for how well each criterion fitted the overall shape of the failure locus. The second method was to compare the predicted mode I toughness ($G_{IC}$) from each criterion with the experimental values. This could only be used for the unidirectional results since no experimental value of $G_{IC}$ could be determined for the multidirectional results. Therefore, for the $0^\circ/90^\circ$ ply interface laminates, the experimental and predicted values of $G_{IC}$ were compared.

In addition to ranking over each set of data, the criteria were ranked for each material and overall for all the $0^\circ/0^\circ$ ply interface results. These overall rankings were determined by assigning each criterion with a ranking number (e.g. best fit=1, etc.) and summing these values over all the data sets. This gave a number for each criterion by which they could then be ranked (i.e. the lowest number = best overall fit).

### 3.6.3 Results of the Curve Fitting

The optimum values of the parameters and the values of $\chi^2$ and $G_{IC}$ for each criterion are tabulated in Appendix B. The ranking of the criteria for each data set are shown in Tables 3-20, 3-21, 3-22 and 3-23, and illustrated in Figure 3-30.

Firstly, for unidirectional T800/5245, the ranking by $\chi^2$ and $G_{IC}$ are shown in Tables 3-20 and 3-22. For the $\chi^2$ ranking, a measure of how well the criterion fitted the shape of the loci, the best criteria were the Exponential K, Exponential Hackle and General Interaction. The poorest fits were given by the K, Linear and Ramkumar criteria. There were no clear trends in the criteria with crack length or moisture, except that the minimum value of $\chi^2$ increased as the moisture content and crack length increased. The region which was most difficult to model was the hump close to the mode I axis.
Mixed-Mode Tests

For the prediction of $G_{IC}$, the Power, Ramkumar and Linear criteria gave the best results and the Benzeggagh, Exponential Hackle and Exponential K criteria gave the worst predictions. As before, there was little consistency in the ranking as the moisture content and crack length changed.

Overall for T800/5245, the General Interaction and Power criteria were the best models of the experimental results (Figure 3-30). However, particularly at large crack lengths and moisture contents, all the criteria were quite poor and did not model the experimental results accurately.

For unidirectional T800/924, the ranking by $\chi^2$ and $G_{IC}$ are shown in Table 3-21 and 3-22. The ranking exhibited better consistency with changes in crack length and moisture content than that for T800/5245. For the $\chi^2$ rankings, the General Interaction, Power and Benzeggagh criteria gave the best predictions and the Ramkumar, Linear and K were the poorest. Unlike for T800/5245, the best criteria for $\chi^2$ were generally the best criteria for predicting $G_{IC}$. The General Interaction, Benzeggagh and Kinloch criteria gave the closest predictions to $G_{IC}$ whilst the Ramkumar, Linear and Power criteria gave the poorest predictions.

For a given moisture content and crack length, the $\chi^2$ for each criterion were lower in T800/924 than in T800/5245. The very different nature of the two materials meant that giving a recommendation of the best overall criterion was difficult. However, both for $\chi^2$ and $G_{IC}$, the General Interaction criterion (Equation 1-22) gave the best overall predictions for unidirectional materials.

The rankings for the 0°/90° ply interface results are shown in Table 3-23. Unlike the unidirectional results (Table 3-22), the values in Table 3-23 were determined from five rather than eight mixed-mode ratios. Furthermore, the unidirectional results included pure mode I whilst the 0°/90° interface results included pure mode II.
As would be expected, the 0°/0° ply interface results in Table 3-23 exhibited similar rankings for $\chi^2$ as the 0°/0° ply interface results in Table 3-21; the General Interaction and Power criteria were the best models whilst the Ramkumar and Hackle were the poorest. For the $G_{IIc}$ rankings, the General Interaction, Power and Benzeggagh criteria gave the closest predictions of $G_{IIc}$ whilst the Linear and Hackle gave the worst.

The failure locus of the 0°/90° ply interface was more non-linear than the 0°/0° ply interface loci (Figure 3-22). The criteria with the lowest $\chi^2$ were the Exponential K, General Interaction and the Power criteria; rankings similar to T800/5245. The highest $\chi^2$ were given by the Hackle and K criteria, which could not model the hump in the 0°/90° ply interface locus. For the $G_{IIc}$ rankings, the General Interaction, Exponential K and Exponential Hackle criteria gave the closest predictions whilst the Power, Yan and Hackle criteria gave the poorest. Overall, the General Interaction criterion gave the best model for the 0°/90° ply interface failure locus.
4. Experimental Delamination Studies

4.1 Preliminary Studies

4.1.1 Aims of the Current Phase

The final phase of the work was the study of delamination growth from embedded inserts. Experimental studies are reported in Chapter 4 whilst modelling is reported in Chapter 5. To study delamination growth, a specimen was designed to meet the following criteria:

(i) Negligible bending up to in-plane compressive strains in excess of 6000 με.
(ii) The delaminated surface could be clearly observed.
(iii) A large gauge area in which to grow the damage.
(iv) Relatively inexpensive and simple to manufacture.

The first criterion was to ensure that the damaged region was only loaded in compression and did not experience any bending loads due to specimen buckling. A design strain of 6000 με was chosen from reference to previous studies\textsuperscript{1,26}. The second criterion was to allow monitoring of the damage growth using shadow Moiré interferometry. The third criterion was to ensure that significant delamination growth could occur before the influence of the edge conditions became important. The second and third criteria compromised the first since these constraints limited the maximum working strains. The final criterion was to reduce costs and would consequently increase the number of parameters which could be investigated.

4.1.2 Choice of Specimen Type

Most previous studies into damage growth under compressive loading have used monolithic specimens in conjunction with an antibuckling guide. Such a fixture clamps the specimen edges and sides, inhibiting buckling and ensuring the damage only experiences in-plane compressive loading. A commonly used test piece for such studies is the Boeing compression after impact test specimen\textsuperscript{8,77} and some exploratory tests were conducted using this test method which repeated some previous work\textsuperscript{1,8}. Laminates were manufactured containing single plane embedded defects between plies close to the
surface. From these laminates, Boeing specimens were cut-out and tested under compressive loading until failure. The damage growth from the defects were monitored using Moiré interferometry.

These tests highlighted a number of problems which had to be resolved before the main study could be conducted. Some delamination growth occurred during post-curing which was partly attributed to volatile material or moisture at the defect plane. A further effect was the interaction between the unbalanced stacking sequence of the delaminated plies and residual thermal stresses which generated opening forces at the defect boundary. In subsequent tests this problem was partly overcome by drilling a 1mm diameter hole through to the delaminated region before post-curing to allow escape of any exuded gases. Another problem was the criticality of the specimen length; if the specimen was too short, the loading plate interfered with the side supports whilst, if it was too large, premature failure occurred.

A major problem with these specimens was the low maximum working strain. Above an applied strain of about $4500\mu e$, global buckling occurred, introducing out-of-plane forces which affected the behaviour of the damaged region. Ultimately, global buckling led to specimen failure at an applied strain of $6000\mu e$. A further limitation was the small specimen width. In previous studies, delamination growth from similar embedded defects led to damage widths greater than 100mm at applied strains of $6000\mu e$; it was not possible to achieve such damage widths using the Boeing specimen design. Furthermore, when the damage front was within 20mm of the specimen edge, unstable axial growth occurred; in a large structure, such discontinuities would be relatively rare. Finally, the sides of the loading fixture obscured the Moiré grating, resulting in a reduced working width of about 75mm, further limiting the maximum damage extent.

Clearly, the anti-buckling fixture approach was inadequate for the test requirements discussed in Section 4.1.1; a different method was required to stabilise the laminates. Some work had previously been conducted using sandwich stabilised panels and it was decided to pursue this approach. Sandwich stabilised panels have a number of
advantages over anti-buckling fixture stabilised laminates. In the latter, buckling is suppressed by clamping of the specimen edges whilst, in the former, it is suppressed by increasing the second moment of area of the specimen. Consequently, in the sandwich panel, the buckling stress can exceed the in-plane strength whilst allowing large gauge areas and shadowing of the grating is negligible. However, honeycomb panels are expensive to manufacture and the in-plane stiffness reduction on one face due to local buckling of the delaminated plies could precipitate global instabilities.

4.1.3 Panel Design

The sandwich panel was designed to the following criteria, which were developed from those discussed in Section 4.1.1.

(i) Withstand applied compressive strains in excess of 6000µε without panel buckling or failure.

(ii) The effect of the stiffness loss on one face due to delamination buckling should be minimised.

(iii) The gauge area should be a minimum of 200 x 200mm to allow significant growth from a 50mm wide insert.

(iv) The panel skins should withstand skin wrinkling or dimpling.

The details of the panel design are given in Appendix C. Due to availability, a 50mm thick Nomex honeycomb (Hexcel HRH-10-3/16-3.0) was chosen, the properties of which are shown in Table 4-1. For consistency with the mixed-mode testing (Chapter 3), the skins of the panel were manufactured from Hexcel T800/924 with a lay-up of \([(+45^\circ/-45^\circ/0^\circ/90^\circ)_3]_s\); a thickness of 3mm. The panel design and strain gauge positions are shown in Figure 4-1 and was predicted to have Euler buckling and face wrinkling strains of 6633µε and 9545µε respectively.

Manufacture and testing procedures of this initial design are described in Section 4.1.4 and 4.1.5 respectively. Upon testing, the panel started to buckle at an applied strain of 3000µε and failed at a strain of 4866µε. Failure analysis indicated that the failure was due to poor out-of-plane support of the skin and subsequent core crippling of the panel ends. To eliminate this failure mode, a number of aspects of the design were modified. Firstly, aluminium honeycomb, rather than Nomex, was used since this had a higher
out-of-plane stiffness \( (E_z) \). Secondly, within the panel ends, chopped glass fibre/epoxy endblocks were used to improve the load introduction and support on the skins.

Aeroweb 4.5-1/8-10(5052) honeycomb was chosen as the new core material (Table 4-1); the core stiffness \( (E_c) \) was an order of magnitude greater than that of the Nomex core. Due to availability, a thickness of 40mm rather than 50mm was used but otherwise the skin lay-ups and panel geometries were the same. The buckling and face wrinkling strains of the new design (Figure 4-2) were predicted to be 53017\( \mu \varepsilon \) and 36568\( \mu \varepsilon \) respectively.

4.1.4 Manufacture

To validate the panel designs (Figures 4-1 and 4-2), skin laminates with dimensions, 350mm x 300mm, were fabricated according to the manufacturer’s specification. These were accurately trimmed into plates, 290mm x 200mm in size. The face of each plate which was adjacent to the bed-plate during cure, was prepared for bonding by cleaning with acetone, abrasion using a grit-blaster and finally cleaning again. For the Nomex sandwich panel, a 290mm x 200mm core was sandwiched between two laminates. For the aluminium honeycomb sandwich panel, blocks of chopped glass-fibre/epoxy, of dimensions 50mm x 200mm x 40mm, were also bonded at either end of the core, which was shorter (200mm x 200mm). The skins were bonded using Hexcel 312 film adhesive which was cured at 120°C for one hour, under a pressure of 30psi after the temperature had exceeded 70°C.

After fabrication, the ends of the panels were accurately mounted in closed aluminium channels (250mm x 100mm x 50mm) which were filled with chopped glass/epoxy composite. Once mounted, the ends of the panel were ground flat to give an overall length of 300mm ± 0.1mm. The panel was then strain gauged at the positions shown in Figures 4-1 and 4-2; these were to monitor the load uniformity over the skin and highlight any panel buckling.
4.1.5 Testing and Failure Analysis

The panels were tested in compression at a stroke rate of 0.015mm/s using a Schenck 1000kN servohydraulic test machine. The panels were initially loaded up to an applied strain of 1500με, at which point the load uniformity was checked. If all the strain readings were within 10% of the average, the test was continued until the panel failed.

The result of testing the Nomex honeycomb sandwich panel has been described in Section 4.1.3. For the improved design (aluminium honeycomb sandwich panel) the load/displacement and strain/displacement responses were linear and coincident (i.e. no buckling) up to failure, which occurred at an applied load of 675kN (10952με). Figure 4-3 shows the front face of the panel after testing; this consisted of a compression failure of the skin which had initiated from the right edge, 80mm from the top of this face, and had extended across the panel width at a slight angle to the horizontal. This face had buckled inwards, locally crushing the honeycomb. The failure of the back face consisted of a saw-tooth fracture along the base and delamination of the entire face between plies 3 and 4 (0°/90° interface). The laminate beneath had failed in compression adjacent to the initiation site on the front face.

The sequence of failure was deduced to have been as follows. In-plane compressive failure of the front face had initiated at the right edge and had begun to propagate across the panel width. This led to buckling of the right side due to lack of the support on the front face. This generated twisting about the loading axis, diverting the front face fracture from the horizontal and also promoting delamination of the back face. Finally, the overall buckling of the structure led to core crushing and fibre fracture of the backface.

To conclude, the aluminium honeycomb sandwich panel design (Figure 4-2) met the main requirements discussed in Section 4.1.1. In fact, the design allowed delamination growth up to applied strains in excess of 9000με before effects such as buckling and panel failure became important. The significant over-design of this structure should overcome any unbalancing effects due to changes in stiffness during delamination.
buckling. However, the main problem was the high unit cost, which is primarily due to
the high tolerances required in the panel length. This limited the number of parameters
which could be studied in the test programme discussed in the following Section.

4.2 Honeycomb Sandwich Panel Studies

4.2.1 Introduction and Programme Details

To characterise delamination growth from embedded inserts a number of parameters
were studied;
(i) Ply interface of the embedded defect
(ii) Embedded defect shape
(iii) Embedded defect size
(iv) Specimen variation

Manufacturing costs limited the number of specimens which could be tested to nine so,
with reference to the results of Chapter 2, the following test programme was formulated.

Two ply interfaces were studied; 0°/90° and +45°/-45°, which are the commonest ply
interfaces at which delaminations occurred during impact. The embedded delaminations
were close to the surface, since this is the most critical case (‘thin-film delamination’). For
the panel design shown in Figure 4-2, the 0°/90° and +45°/-45° ply interfaces
chosen were between plies 3 and 4, and, 5 and 6, from the outer surface of the front face
respectively. The interface between plies 1 and 2 (+45°/-45°) was considered to be
prone to surface splitting.

Two initial defect shapes were chosen; circles and ellipses. The former was to simulate
the overall shape of impact damage and was a commonly studied damage shape
(Section 1.3.3). An ellipse was typical of the damage lobes due to impact and the major
axis was aligned perpendicular to the applied load since this was the severest case
(Section 1.3.3).

The initial defect sizes were chosen with reference to the damage extents observed in
Chapter 2. Circular initial defects, with diameters of 35mm and 50mm were chosen;
Experimental Delamination Studies

damage areas of 962mm² and 1964mm² respectively. This was a similar range in areas observed in the 15J impacts on the stiffened panel in Chapter 2 (Table 2-1). The elliptical defects, 35 x 50mm and 50 x 71mm, were chosen to compliment the circular defects. Each ellipse had the same aspect ratio and the damage area of the smaller ellipse was approximately half that of the larger defect.

Nine panels (Table 4-2) were manufactured; panels A to D had defects at the 0°/90° (3/4) ply interface while panels E to H had defects at the +45°/-45° (5/6) ply interface. A further panel, identical to panel B, was manufactured (panel I) to characterise the effect of specimen variation.

4.2.2 Experimental Details

4.2.2.1 Manufacture

For the back faces of the panels two laminates, of dimensions 400mm x 1400mm, were manufactured from Hexcel T800/924 with a stacking sequence of [(+45°/-45°/0°/90°)3]. For the delaminated skins, nine laminates of dimensions 400mm x 300mm were manufactured with the same lay-up; inserts were placed at the centre of each plate, at the interfaces and with sizes shown in Table 4-2, such that the insert plane was closest to the uppermost surface. The laminates were cured according to the manufacturer’s specification, after which a 1mm diameter hole was drilled through the delaminated plies to the insert plane. The laminates were then post-cured at 180°C for four hours. After fabrication, the laminates were ultrasonically scanned and trimmed into plates with dimensions 250mm x 320mm (0° plies parallel to the longer length), ensuring that the inserts were at the centre of each plate.

The manufacture of the panels was as described in Section 4.1.4 and each panel was ultrasonically scanned after manufacture, to ensure a high quality of bonding. Strain gauges were then bonded onto the panels and a central band on the front face was lightly abraded and sprayed white.
4.2.2.2 Testing and Instrumentation

Each panel was tested in a 1000kN servohydraulic test machine in compression at a rate of 0.005mm/s and the strains, applied displacement and loads were recorded using a data logger. Testing was stopped after significant unstable damage growth had occurred or when the delamination had reached the edges of the panel. The damage growth was monitored using out-of-plane shadow Moiré interferometry. A glass grating (Graticules Limited SAG4), 150mm x 200mm in size, was held at a uniform distance of 3mm from the panel surface, using the fixture shown in Figure 4-4. A wedge (80mm x 10mm and 2mm at the highest end) was used to calibrate the fringe spacing; this was attached to the panel and the fringe spacing measured during testing (as shown in Section 4.2.2.3).

To generate and monitor the fringes, the instrumentation shown in Figure 4-5 was used. The grating was illuminated by a projector at an angle $\phi$ to the normal of the panel surface and approximately 1.5m distant from the panel face. The fringes were monitored using a video camera normal to the surface, approximately 1m distant, and the signal was fed into an image enhancement system. A second camera was used to monitor the load reading and both these signals were fed into a video mixer, allowing the two images to be displayed simultaneously, and recorded. After testing, the panels were ultrasonically scanned to determine the extent of the delamination growth and the strain gauge results analysed using a Microsoft Excel spreadsheet.

4.2.2.3 Image Analysis

The Moiré fringe spacing was calculated using two methods; test geometry and the wedge. For the first method the out-of-plane displacement ($z$) equivalent to each fringe was given by\(^{15}\);

$$z = \frac{s}{\tan \phi}$$  \hspace{1cm} (4-1)

where $s$ was the pitch of the grating (0.125mm) and $\phi$ was the illumination angle; the angle between the projector and the camera. From knowledge of the test set-up, the cosine rule was used to determine the value of $\phi$. The wedge method entailed relating the spacing of the fringes on the wedge to the known wedge gradient.
After testing, selected images from the test were captured and stored as TIF files. These were enhanced using Corel Photopaint and, with reference to the original insert size, the longitudinal and transverse damage extents were determined.

4.2.2.4 Failure Analysis

To characterise the damage growth mechanisms, the delaminated plies were cut away to expose the fracture surfaces, being careful to minimise any post-failure damage; any that was introduced was clearly marked and recorded. To allow inspection using electron microscopy, the fracture surfaces were cut in two at the mid-width; this line was chosen since it was away from the observed delamination initiation sites. However, the fracture surfaces from the larger defects (panel D and H) were too large to easily examine using this technique.

The fracture surfaces were gold sputter coated and examined using a Hitachi S450 Scanning Electron Microscope (SEM) at magnifications of between 20x and 20000x. Micrographs of the shear cusps were taken at selected locations, at a surface tilt angle of 65° and magnifications of between x1000 and x5000. Up to three of these images were captured for each location, each image containing between five and twenty shear cusps. Using the method explained in Section 3.5.3, average tilt angles and coefficients of variation, $C_V$, were determined. From this, and comparing with the morphology of the 0°/90° ply interfaces generated in Section 3.5.3, the proportion of mode I loading at each location was inferred. Only proportions of mode I between 0% and 87% (cusp angles of between 76° and 23° respectively) could be determined since this was the range of mixed-mode tests conducted for this ply interface.

4.2.3 Experimental Results

4.2.3.1 Testing Details

A photograph of a panel in the test machine is shown in Figure 4-6. For all the tests the illumination angle was 35° which generated a fringe spacing equivalent to a height change of 0.179mm ± 0.02mm. From measurements of the fringes on the wedge, the
average fringe spacing was calculated to be equivalent to a 0.202mm ± 0.02mm height change. However, it was felt that first result may be sensitive to errors in the illumination angle, so the value given by the wedge calculation (0.202mm) was used in the subsequent analyses. In all the tests, the strain gauge results indicated that the delamination growth was under pure in-plane compressive loading and no panel bending or buckling had occurred.

4.2.3.2 0°/90° Ply Interface Delaminations

The damage development in panels A, B, C, D and I which contained inserts at the 0°/90° (3/4) ply interfaces were all similar, and examples are shown in Figures 4-7 and 4-8 (panels B and D respectively). Before loading the delaminated region was visible as a faint ring. As the load was introduced, the delaminated region became larger and elliptical, with the peak height increasing until, at an applied strain of about 2000µε, there was initiation of growth at the lateral boundaries of the defect. The delamination blister developed into a skewed lozenge, with lobes growing on the right side, from above the major axis of the ellipse and, on the left side, from below the major axis; almost parallel to the -45° ply direction. Secondary growth later developed, again initiating from the lateral boundaries of the damage but propagating parallel to the +45° ply. At an applied strain of about 6000µε, the damage growth had led to the development of a rectangular blister. Subsequently, the corners of this region extended, parallel to the angle plies, leading to the formation of a dog-bone shaped blister. At an applied strain of about 7500µε, rapid growth of these corner lobes occurred, followed by splitting of the surface plies along the lobe boundaries. There was also some evidence of longitudinal damage growth, parallel to the loading direction (0°), from the longitudinal boundary of the insert, although the fringes associated with this were quite difficult to resolve.

Tests on the panels containing defects at the 0°/90° (3/4) ply interface were stopped when massive longitudinal damage growth occurred, at an applied strain of between 8100µε and 9100µε.
The initiation strains for delamination growth are summarised in Table 4-2, and the delamination heights and the increase in the lateral dimension (width) with applied strain are shown in Figures 4-9 and 4-10 respectively.

In all the panels, the delamination height (Figure 4-9) increased linearly with applied strain over most of the test although, close to failure there was a discontinuity and a rapid increase in height. There was an effect of initial defect length on the variation in height, splitting the data into two sets of curves. At a given applied strain, the height of the damage in panels A and C (defects of 35mm in length) was about 0.5mm less than that in panels B, D and I (defects of 50mm in length). The longer defects also exhibited a change in gradient, at a lower strain (7300µε) than the shorter defects (7900µε). There was no effect of initial delamination width or area on the variation in height.

The increase in delamination width with applied strain is shown in Figure 4-10 and was similar for all the panels. There was a slow increase in width at the start of testing which rapidly increased as damage growth was initiated until, at failure, the curves were almost vertical. Although there was no clear effect of initial defect width, length or area on the damage width, the initial defect shape did affect the behaviour. For the circular initial defects (panels A, B and I) the curves were continuous up to failure, whilst the elliptical initial defects (panels C and D) exhibited a discontinuity at the same applied strain as was observed with the height variation (Figure 4-9). Furthermore, after initiation, the delamination growth from the larger initial defect (panel D) grew faster than that from the smaller defect (panel C). Prior to the discontinuity, at a given applied strain, this difference in width was about 8mm but close to failure this difference was as much as 30mm.

There was no clear effect of initial defect shape or size on the increase in delamination length with applied strain. Generally, the blister length decreased as the load was applied until, close to failure, rapid longitudinal growth occurred.
The initiation strains for delamination growth are tabulated in Table 4-2. There was no evidence of delamination initiation from the load/displacement or load/strain curves and therefore, these initiation strains were determined by measuring where the increase in width against applied strain (Figure 4-10) intersected the x-axis. It should be noted that there is a significant error (at least 10%) associated with these values due to the flat nature of the curves. Furthermore, the Moiré interferometry only resolved to half a fringe (about 0.1mm) which introduced more error.

The initiation of the growth ranged between 1850με and 3350με, with an average initiation strain of 2390με. In general, taking the error into account, there was no clear trend in initiation strain with initial defect size. However, the larger elliptical insert (panel D) started growing at a significantly lower strain (1850με) than the smaller elliptical insert in panel C (3350με).

A comparison between the delamination development in panels B and I gave an indication of the effect of specimen variation. The damage development was very similar, although the failure events such as surface cracking were 200με earlier in panel I compared to those events in panel B. The damage height and width behaviour (Figures 4-9 and 4-10) indicated little difference. The only major difference was in the initiation strains; 2400με for panel B and 1950με for panel I, but this could be attributed to the large error associated with these values.

4.2.3.3 +45°/-45° Ply Interface Delaminations

The damage development in panels E, F, G and H containing defects at the +45°/-45° [5/6] ply interface was similar for all cases but quite different from those for 0°/90° [3/4] ply interface defects. Examples are shown in Figures 4-11 and 4-12 for panels F and H respectively. Prior to loading the disbonds were barely visible on the surface, but as the load was applied the delamination blister started to rise. The blister was elliptical, with the major axis at about 105° (in a clockwise direction) to the loading direction. At an applied strain of between 2950με and 4150με, delamination growth initiated on the defect boundary at opposing points, 100° to the loading direction (about 5° to the major
axis of the elliptical blister). As the load increased, the delamination extended laterally from these points, developing into a flattened and skewed ellipse. The lateral extent of the blisters were quite sharp, irregularly shaped and the growth was slip-stick, with the left and right lobes usually differing in size. At an applied strain of about 6000με some splitting of the surface plies developed. The tests were stopped when the damage growth started to reach the edges of the panels; an applied strain of between 6000με and 6900με.

The initiation strains for delamination growth are tabulated in Table 4-2, and the delamination heights and widths with applied strain are shown in Figures 4-13 and 4-14 respectively.

Firstly, in all the panels, the delamination height (Figure 4-13) increased linearly for the entire test. At a given applied strain, the peak height was proportional to the insert area; doubling of the insert area increased the peak height by 50%. Finally, there was no indication of when initiation of damage growth occurred from these curves.

The increase in damage width with applied strain for the panels containing inserts at the +45°/-45° ply interfaces is shown in Figure 4-14. After initiation of delamination growth, the width increased linearly for most of the test. The curves could be grouped into two pairs; at a given applied strain, the damage from the 50mm long inserts (panels F and H) had undergone about 20mm more growth than that from the 35mm long inserts (panels E and G). The curves for the shorter inserts (panels E and G) also changed gradient at a higher strain than the longer inserts (panels F and H). This may imply that the growth from the shorter inserts had initiated at a higher applied strain than from the longer inserts.

The increase in damage length with applied strain for the panels containing inserts at the +45°/-45° ply interfaces showed no clear trends with defect shape and size since the damage was shorter than the insert for the entire test.
The delamination growth initiation strains (Table 4-2) ranged from 2950µε to 4150µε; an average value of 3350µε. Although there was no clear trend in these values with insert size, these initiation strains were on average 40% greater than the initiation strains from the inserts in the 0°/90° ply interfaces.

**4.2.3.4 Effect of Ply Interface of Embedded Defect**

Examples of comparisons in damage width with applied strain are shown in Figures 4-15 and 4-16, for 50mm circular and 50mm x 71mm elliptical defects, respectively. Independent of defect shape and size, delamination growth grew earliest from the shallower defects (0°/90° ply interface). However, the growth from the defect at the +45°/-45° ply interface was more rapid and the curves in Figures 4-15 and 4-16 significantly diverged after initiation. The curves in Figure 4-15 demonstrate the excellent repeatability in the behaviour between identical defects (panels B and I). However, it was difficult to distinguish the initiation strains in these defects since the curves were so shallow.

Examples of the variation in damage height with applied strain are shown in Figures 4-17 and 4-18 for 50mm circular and 50mm x 71mm elliptical defects respectively. For a given applied strain, the shallower defects (0°/90° ply interface) was between 0.5mm and 1.0mm higher than the deeper defects (+45°/-45° ply interface). Again, there was excellent repeatability between nominally identical defects.

**4.2.4 Failure Analysis Results**

**4.2.4.1 0°/90° Ply Interface Delaminations**

The damage growth from all the panels containing inserts at the 0°/90° [3/4] ply interfaces were similar and examples are shown in Figures 4-19 and 4-20 for panels I and C respectively. The inserts are the dark disks and the straight dark lines on the surfaces were saw cuts introduced during dissection.

A simplified example, illustrating the ply interfaces and important features from Figure 4-19 (panel I) is shown in Figure 4-21. In general, the fracture surfaces exhibited
rotational symmetry such that the similar types of failure surface were diagonally opposite. In the centre of the damage, bounded by 0° ply splits A-A and B-B tangential to the insert, the failure was within the 0°/90° ply interface (between plies 3 and 4); referred to as a type (i) fracture surface. On this surface features were observed, referred to as ‘tide-marks’\textsuperscript{13}, which radiated from the insert.

Beyond the central type (i) fracture, the failure surface was quite complicated. In the bottom-left and top-right regions, failure had occurred within a 0°/-45° ply interface (between plies 2 and 3); referred to as a type (ii) surface. These regions were bounded by the 0° ply splits A-A and B-B, and -45° ply splits C-C and D-D, which were also tangential to the insert. In the top-left and bottom-right regions, the failure had occurred within a +45°/-45° ply interface (between plies 1 and 2); referred to as a type (iii) surface. These regions were smaller in area than the type (ii) surfaces.

In panels C and D (Figure 4-20), the appearance of the fracture surfaces was slightly different; adjacent to the insert the failure surface was only type (i). Above and below the insert, tide marks were visible and those close to the insert were bounded by 0° ply splits tangential to the insert edge. From this, it was deduced that the lateral delamination close to the insert had occurred prior to the type (i) surfaces adjacent to the insert. Closer examination of the matching upper fracture surfaces indicated that there were type (ii) and (iii) surfaces above the longitudinal type (i) surfaces and these type (ii) and (iii) surfaces had been generated first.

The fracture surfaces were examined in more detail using electron microscopy. The locations examined in panel I are used as an example, and the sites examined on the upper left surface are labelled in Figure 4-22. Note that this surface is a mirror image of the left side shown in Figure 4-21. All the panels with defects at the 0°/90° ply interface exhibited similar surface morphologies.

An example of the type (i) surface is shown in Figure 4-23, which was from a site close to the insert at its axial boundary (site (1) in Figure 4-22). The surface was covered in
upright shear cusps; indicative of mode II dominated failure. The cusps were aligned perpendicular to the fibres (0°) and were tilted towards the mid-span of the panel; indicating that the upper surface had moved towards the insert during failure (the upper surface had bent outwards30). The morphology of the type (i) surface diagonally opposite site (1) was identical to that in Figure 4-23.

Towards the lateral boundary of the insert (Figure 4-24; site (2) in Figure 4-22), the cusps were flatter, indicative of an increase in the mode I component. The surface of the defect region is shown in Figure 4-25 (site (3) in Figure 4-22). The white lines on the surface were 0° splits in ply 3 and as these approached line A-A they reduced in spacing. Similar splitting was also noted on the diagonally opposite side of the insert.

The type (ii) surface at the -45°/0° [2/3] interface was quite different from the type (i) surface. Adjacent to the insert (Figure 4-26; site (4) in Figure 4-22) the surface partly extended above the insert and the failure was close to the resin layer between plies 2 and 3. Close to the insert the surface was covered in cusps aligned parallel to the 0° ply. However, away from the insert, the cusps were aligned parallel to the -45° plies and were quite flattened; indicative of mode I dominated fracture (Figure 4-27; site (5) in Figure 4-22). This surface exhibited river lines which indicated fracture parallel to the -45° ply, away from the insert. Close to and at the lines C-C and D-D, there was extensive matrix splitting parallel to the -45° ply (Figure 4-28; site (6) in Figure 4-22).

The type (iii) surfaces (+45°/-45° [1/2] ply interface) were similar in appearance to the type (ii) surfaces. The cusps were orientated parallel to the +45° plies and were quite flattened; indicative of a high proportion of mode I fracture, although, distant from the insert, the proportion of mode II increased. Close to the lines A-A and B-B, the shear cusps were flattened and orientated parallel to the 0° ply. Adjacent to this region, along the edge of line A-A, a line of fibre failures was observed (Figure 4-29; site (7) in Figure 4-22). These failures were within ply 2 (-45°) and, inferring from the witness marks on adjacent fracture surfaces, had occurred after the generation of the type (ii) surface but prior to the generation of the type (iii) and (i) surfaces. These fibre bundles
Experimental Delamination Studies

are shown in more detail in Figure 4-30; these were in-plane shear failures perpendicular to the fibre direction (the opposing fracture surface had moved away from line C-C, parallel to the +45° ply, when the type (iii) surface had been generated).

In all the panels with defects at the 0°/90° ply interface, the cusps were examined in detail to determine the mixed-mode conditions. In general, for all the surfaces examined, the scatter (Cv) in the average cusp tilt angles at each site was about 10%. However, at locations with a high proportion of mode I or mode II or close to changes in fracture plane, the scatter was higher. This was attributed to rapid changes in loading conditions at these sites.

The mixed-mode distribution for the damage growth in all the panels containing inserts at the 0°/90° [3/4] ply interfaces exhibited some similarities. Figure 4-31 shows the mixed-mode distribution for the damage growth in panel I (50mm diameter insert at 0°/90° [3/4] interface) where each number corresponds to the proportion of mode I at each location. Firstly, the type (i) regions (0°/90° [3/4] ply interface failure) were predominantly pure mode II fracture, particularly at the longitudinal boundaries of the insert. The cusps angles were often greater than observed on surfaces tested under pure mode II loading in Section 3.5.3. In Figure 4-31 and subsequent figures, these regions were labelled as 0% mode I. Although the majority of the type (i) surface was mode II failure, towards the transverse boundaries of the insert the proportion of mode I increased and these regions exhibited a large degree of scatter in the cusp angles. At the intersection between the insert edge and lines A-A and B-B, mode I fracture dominated.

The mixed-mode distribution in the type (ii) regions (-45°/0° [2/3] ply interface failure) was very different from the type (i) regions. Directly adjacent to lines A-A, the failure was mode II dominated parallel to the 0° ply. However, beyond this region the failure was mode I dominated parallel to the -45° ply. Finally, the type (iii) surfaces exhibited large changes in mixed-mode ratio. Close to the boundary lines A-A, B-B, C-C and D-D, the mode I component was dominant (80% mode I). However, the scatter was high and as the delamination grew from the insert, the mode II component increased until, at
the damage boundary, the failure was predominantly mode II. The sequence of failure and the damage mechanisms deduced from the fractographic evidence are discussed in Section 6.3.3.

Although the fracture surfaces for all the panels were similar in appearance, the mixed-mode distributions showed some differences with those from panel I (Figure 4-31). For growth from the smaller circular defect (panel A), the proportion of mode II failure on the type (ii) and (iii) surfaces increased, compared with those from the larger circular defect (panels B and I). Similarly, for the damage growth from the smaller elliptical defect (panel C; Figure 4-32), mode II fracture was more dominant on the type (ii) and (iii) failure surfaces, particularly away from the insert. Finally, the damage from the two panels containing 50mm diameter circular defects (panels B and I) were compared. In general, the mixed-mode distribution was similar, particularly close to the insert. The distribution at the type (i) surfaces was almost identical but the distributions at the type (ii) and (iii) surfaces differed slightly in the proportion of mode I.

4.2.4.2 +45°/-45° Ply Interface Delaminations

The damage growth in all the panels with inserts placed at the +45°/-45° [5/6] ply interfaces were quite similar and examples are shown in Figures 4-33 and 4-34 for panels E and G respectively. Unfortunately, some of the fracture surfaces were contaminated by white paint from the panel surface during dissection.

A simplified diagram of a typical fracture surface (panel E) is shown in Figure 4-35. Extending diagonally from the insert were two triangular fracture surfaces at the +45°/-45° [5/6] ply interface; referred to as type (iv) surfaces. These areas were bounded by +45° ply splits, A-A and B-B, which were almost tangential to the insert boundary. Laterally from the insert, beyond these type (iv) surfaces, were regions of failure within the 90°/+45° [4/5] ply interface; referred to as type (v) surfaces. These upper surfaces were examined in detail, part of which (left side of panel E) is shown in Figure 4-36. There was extensive matrix splitting in the 90° ply on the type (v) surface and the interface below (0°/90° [3/4] ply interface) had partly delaminated. Subsequently, on the
upper right surface, the 0°/90° ply interface surface was exposed (Figure 4-37) by cutting along line B-B. The new surface was referred to as a type (vi) fracture and was bounded by a central region of 90° plies attached to the surface. A simplified diagram of these surfaces after the removal of the 90° plies is shown in Figure 4-38.

The fracture surfaces were examined in more detail at the locations shown in Figure 4-39 (panel E). Firstly consider the type (iv) surface close to the insert at point (1) in Figure 4-39 (Figure 4-40) which was close to the site at which delamination growth had initiated. There was evidence of +45° ply splitting at the insert which extended into the type (iv) surface; this splitting was densest adjacent to line A-A. The type (iv) surface was dominated by resin cleavage, growing parallel to the +45° ply, and there were few cusps. From the continuity of the fracture morphology, it was inferred that this surface had been generated prior to the +45° ply splitting.

Around the insert boundary, towards its longitudinal extent (Figure 4-41; point (2) in Figure 4-39), shallow cusps developed, although the growth direction was still parallel to the +45° ply. Further out from the insert edge, towards the line A-A, the surface was undulated and the shear cusps were flatter; an increase in the mode I component.

The type (v) surface close to the insert edge is shown in Figure 4-42 (point (3) in Figure 4-39). The central region was originally beneath the insert and was mode II dominated failure parallel to the +45° ply between plies 4 and 5 (90°/+45° ply interface), with the matching surface moving to the left. Further along line A-A at the insert boundary, this shear failure terminated in a line of fibre failures of ply 5 (Figure 4-43; point (4) in Figure 4-39). These were in-plane compression failures which were consistent with the observed shear direction in Figure 4-42. Outboard of line A-A, the delamination growth direction changed again, extending parallel to the 90° ply, and consisted of shallow cusps, resin cleavage and fibre bridging typical of mode I dominated failure.

Finally, close to the central band of 90° plies, the type (vi) surface was predominately resin cleavage and riverlines indicative of mode I dominated growth (Figure 4-44; point
(5) in Figure 4-39). This surface had initiated from the 90° ply splits in the type (v) surface and had grown parallel to the 0° ply (ply 3). As the delamination extended the surface morphology changed significantly (Figure 4-45; point (6) in Figure 4-39). The cusps became more upright; indicative of an increase in the mode II component.

The mixed-mode distribution for damage growth in all the panels containing inserts at the +45°/-45° [5/6] ply interfaces were relatively similar. The mixed-mode distribution for the damage growth (panel E) is shown in Figure 4-46; due to damage introduced during exposure of the type (vi) surfaces, not all the failure surfaces could be examined in detail. The type (iv) regions were mode I dominated failure parallel to the +45° ply (72% to 88% mode I), but towards the longitudinal boundaries of the insert, mode II failure became dominant. At the type (v) surface adjacent to the line A-A, there was a narrow band of pure mode II failure, parallel to the +45° ply. However, as the damage extended from the insert, the failure became mode I dominated; over most of this surface the proportion of mode I failure was between 81% and 88% but, towards the longitudinal boundaries, the proportion of mode II increased. Finally, the type (vi) surfaces close to the 90° ply splitting were mode I dominated. However as the damage grew away from these splits the proportion of mode II increased; at the longitudinal boundaries, the failure was almost pure mode II. Towards the lateral boundaries of the damage, the mode I component increased.

The general mixed-mode distributions for the other panels with inserts at the +45°/-45° ply interfaces were similar to those of panel E. For the damage growth from the larger circular defect (panel F), there was generally a higher mode II component, particularly at the type (iv) fracture surfaces, compared with that of the smaller circular defect (panel E). However, as shown in Figure 4-47, for the damage growth from the smaller elliptical defect (panel G), mode I failure was more dominant, particularly for the type (iv) surfaces.
5. **Finite Element Delamination Studies**

5.1 **Introduction and Objectives**

In the previous Chapter, delamination was investigated in controlled experiments using defects of various shapes and sizes, positioned at different depths, between the plies of different orientations. The aim of this part of the work was to predict the observations described in Chapter 4. Although the experimental studies gave a valuable insight, they do not in themselves give a predictive tool. To promote damage tolerant design and improve certification procedures, realistic models of delamination behaviour must be developed. Such predictive tools would improve confidence in using composites and lead to improved future designs.

In the work described in this Chapter, the defect size was kept constant (50mm diameter), but two delamination depths were investigated; between plies 5 and 6 (+45°/-45°) and between plies 3 and 4 (0°/90°) corresponding to panels E and B (and I) respectively from Chapter 4. The particular aims were to predict;

a. The delamination blister shape and height

b. Applied strain and locations for initiation of delamination growth

c. Applied strain and locations for ply splitting

Models were developed using a commercial software package but simplifications were made, as detailed in Section 5.2.1, to make the models manageable.

5.2 **Description of the Finite Element Model**

5.2.1 **Mesh Geometry**

The finite element code ABAQUS Standard (HKS Limited) was used in the modelling. The mesh geometry, after the validation phase had been completed (Section 5.2.3), is shown in Figure 5-1. The elements were eight noded shell elements (S8R180) with reduced integration; shell elements, rather than 3D brick elements, were chosen to ensure the model was manageable. The whole model was 200mm x 200mm in area and the mesh was one element deep, the thickness varying with stacking sequence; each ply
was 0.125mm thick. At the centre was a region 50mm in diameter; the embedded defect and delaminated plies (shown in red in Figure 5-1). The entire mesh was flat except for the defect which had a sinusoidal half-wave perturbation with a peak 0.1mm. This negated the need for an instability calculation to determine the blister buckling mode.

One edge of the model (Figure 5-1) was clamped whilst an in-plane displacement, parallel to the 0° ply, was applied to the opposite edge. The outer area of the model was constrained in the z-axis and rotations about the x- and y- axes were prevented whilst the delaminated region was free to displace and rotate in all directions. Located 10μm beneath the mesh was a rigid surface with contact elements (IRS180) which was used to model the sublamine beneath the delaminated plies. The contact elements had zero stiffness unless the delamination impinged upon the rigid surface, at which point the element stiffness would be infinite, allowing a reaction to be transmitted into the delaminated plies. This ensured that the delamination always remained above the rigid surface although, in reality, some of the in-plane loads from the delaminated region would have been shed onto the sublamine below, which was not modelled.

The material’s properties used in the model were the same as those used in Chapter 3 (Table 3-7); based on dry Hexcel T800/924 tested at room temperature. The stacking sequences used are shown in Table 5-1. The in-plane displacement was applied in increments using a non-linear algorithm (Riks Method181). This technique was used because the contact effects and large deflections of the blister meant the model would be non-linear. Results were generated after each increment up to a displacement well in excess of the expected delamination growth strain; typically delamination growth was achieved after twelve increments, after which the model was stopped. However, due to the non-linear nature and contact effects, each model took in excess of fifty hours to run on a SunSparc IPX workstation. Upon completion, the strain and location on the defect boundary for initiation of delamination growth was determined (Section 5.2.2).
5.2.2 Characterisation of Damage Initiation

It was established that ABAQUS alone would not be able to provide all of the requirements to predict initiation of delamination growth. Hence, a series of routines (written in Floating Point Systems Ltd IDL) were created which read the data from an ABAQUS results file and calculated whether growth had initiated. Figure 5-2 shows a flow chart for implementation of the growth routine. Firstly, a mesh was created using the pre-processor DISPLAY III and was then manipulated to add boundary conditions, material's data, loading conditions and the required output parameters. The model was then run with the ABAQUS/STANDARD solver. The output from the simulation was then put through an IDL routine to calculate the mode I and mode II energy release rates ($G_I$ and $G_{II}$) at the delamination boundary using the virtual crack closure (VCC) method.

Referring to Figures 5-3 and 5-4, the strain energy release rates were defined as:

$$ G_I = \frac{F_I \delta_I}{2} \quad (5-1) $$

where

$$ \delta_I = \delta^b_z $$

$$ F_I = P^a_x $$

and

$$ G_{II} = \frac{1}{2} \left| \left( F_{lix} \delta_{lix} + F_{liy} \delta_{liy} \right) \right| \quad (5-4) $$

where

$$ \delta_{lix} = \left( \delta^b_x - \delta^a_x \right) \cos \phi $$

$$ \delta_{liy} = \left( \delta^b_y - \delta^a_y \right) \sin \phi \quad (5-5) $$

$$ F_{lix} = P^a_x \cos \phi $$

$$ F_{liy} = P^a_y \sin \phi \quad (5-6) $$

$F_I$ and $F_{II}$ are the mode I and II forces at a node, $\delta_I$ and $\delta_{II}$ are the mode I and II displacements at a node. $P^a_x$, $P^a_y$, $P^a_z$ are the x, y and z forces at node ‘a’, $\delta^a_x$ and $\delta^a_y$ are the x and y displacements at node ‘a’ and, $\delta^b_x$ and $\delta^b_y$ are the x and y displacements at node ‘b’, $\phi$ is the angle around the defect boundary (clockwise) where $\phi=0$ at the apex.

For each iteration, $G_I$ and $G_{II}$ at each site on the delamination boundary was read into a spreadsheet to analyse the results and failure criteria were employed to determine if
growth had occurred. The criteria used are based on those shown in Table 1-2 and the parameter values for each criterion are given in Appendix B. A number of criteria were not used; the Mode I (Equation 1-13), Mode II (Equation 1-14) and $G_C$ (Equation 1-15) were considered too inaccurate and the Bilinear (Equations 1-26 and 1-27) was too difficult to use in this context. Parameters chosen for the criteria were developed from a number of different sets of MMB test results; this was to investigate the effect of MMB data on the predictions. Data from dry $0^\circ/0^\circ$ ply interfaces at crack lengths of $a=40\text{mm}$ and $a=60\text{mm}$ were used, as well as data from the wet $0^\circ/0^\circ$ ply interface data at a crack length of $a=60\text{mm}$. Finally, dry $0^\circ/90^\circ$ ply interface data (averaged over all crack lengths) was also employed. After the failure criteria had been applied, linear interpolation\textsuperscript{14} was used between iterations to accurately determine the delamination initiation strain. This gave an indication of the variation between using different criteria and identified from which site delamination had initiated.

In addition to delamination growth, a second damage mechanism was identified during the experimental investigation; ply splitting. To monitor this failure mode, the transverse strain ($\varepsilon_{22}$) in each ply was measured and splitting was deemed to have occurred when this exceeded the ultimate transverse tensile strain of the ply.

5.2.3 Mesh Convergence

Before running the models and analysing the results, a convergence test was conducted to determine the optimum mesh density. If the mesh was too coarse, the results would be incorrect whilst, if the mesh was too dense, the model would be unmanageable. To test the convergence five meshes of different densities were investigated; the coarsest (Mesh 2) had 172 elements whilst the densest (Mesh 6) had 960 elements.

Preliminary models had indicated that the transverse strain ($\varepsilon_{22}$) at the lowermost ply in the delaminated material could be used to gauge the mesh convergence. The strain at node 15001, which had the same physical location in all the models, was investigated; the transverse strain at a given applied strain (4600µε) was measured at this node for
each mesh (Table 5-2). This data was curve fitted to the Craighead expression\(^{182}\) which is an actuarial method for projecting catastrophes:

\[
\varepsilon_{C} = \varepsilon_{\infty} \left[1 - e^{-\left(\frac{x}{b}\right)^S}\right]
\]

(5-7)

where \(\varepsilon_{C}\) is the predicted transverse strain, \(x\) is a measure of the mesh density and \(\varepsilon_{\infty}, b\) and \(S\) are constants. In the limit as the mesh density tends to infinity, the transverse strain \((\varepsilon_{C})\) tends towards the continuum result \(\varepsilon_{\infty}\). All three constants in Equation 5-7 \((\varepsilon_{\infty}, b\) and \(S\)) were determined using the \(\chi^2\) method, as used in Chapter 3\(^{14}\). The following expression was minimised by optimising the values of the constants \((\varepsilon_{\infty}, b\) and \(S\)); the minimum value of the left side gave the best fit of Equation 5-7;

\[
\sum \chi^2 = \sum \left[\varepsilon_{FE} - \varepsilon_{C}\right]^2
\]

(5-8)

where \(\varepsilon_{FE}\) was the predicted strain by the model at a mesh density \((x)\) and \(\varepsilon_{C}\) was the strain given by Equation 5-7.

As shown in Table 5-2, the continuum result \((\varepsilon_{\infty})\) was 2498\(\mu\varepsilon\). Meshes 5 and 6 predicted strains which were within 10% of this and, since 10% is the typical experimental error associated with composites, Mesh 5 (700 elements) was chosen for further study.

5.3 Model Predictions

In this Section the angles quoted are at a clockwise rotation with respect to the upwards loading direction; \(0^\circ\) is the apex of the defect. In all the images red corresponds to maxima and blue to minima. It should be noted that the colours in each image are not directly comparable, but indicate the relative magnitudes within each image.

5.3.1 Defect at a 0°/90° Ply Interface; Model #1

The development of the delamination blister prior to initiation of growth in Model #1 is shown in Figure 5-5. The loading direction for each image was vertical and the applied strain is also shown. As the load was increased, the originally circular blister flattened at the longitudinal extent (0° and 180°) forming a flattened ellipse. The major axis of the ellipse was almost perpendicular to the loading direction; (95° and 275°) and at initiation of growth the centre of the blister had risen about 1.6mm.
This blister shape was reflected in the magnitude of the mode I (peel) and mode II (shear) components around the defect boundary (Figure 5-6). In this Figure, the magnitudes of the mode I and II components are plotted against position around the blister's boundary. Upon loading, the mode I and II components were greatest at the lateral (90° and 270°) and longitudinal (0° and 180°) extents of the defect respectively. The mode II component was the greater of the two, but both were considerably less than that required to initiate growth. As the load increased, the mode I peaks increased rapidly and at initiation of delamination growth, mode I dominated.

The strains predicted for initiation of delamination growth in Model #1 are shown in Table 5-3. Using the coupon data set most similar to the conditions at the defect boundary; the 0°/90° ply interface, the predicted strain varied from 2720µε (Exponential K Criterion) to 3310µε (Hackle Criterion); an average of 2954µε with a spread in the predictions of 20.0%. The spread is defined as the difference between the maximum and minimum results, normalised by the average. Growth was predicted to have initiated at the lateral extent of the defect (90° and 270°), coincident with the major axis of the blister and the peak of the mode I component. However, it should be noted some criteria gave indeterminate results at sites where the mode I component was negative.

There was evidence that the data set used for the failure criteria had a significant effect on the predictions, particularly when using complicated criteria. When using data from the 0°/0° ply interface coupons, the spread between different criteria was below 11%, but the 0°/90° ply interface data had a spread of almost twice this. For a given criterion, the 0°/90° ply interface data gave the lowest (most conservative) prediction; 2954µε, whilst the dry 0°/0° ply interface data, at a crack length of a=60mm, gave the highest; 3168µε. Also, for the 0°/0° ply interface data, the Ramkumar criterion consistently gave the most conservative (lowest) result, but for the 0°/90° ply interface data, the Exponential K criterion was the most conservative. There was one anomalous result; the General Interaction criterion, when using dry 0°/0° ply interface data at a crack length of a=40mm, gave a very high prediction (4530µε) but it is not clear why this occurred.
The second mechanism was predicted by the model (Figure 5-7); splitting of the uppermost ply at the defect plane (0° ply). The maxima for the ply splitting was coincident with the predicted growth sites (90° and 270°) and initiated at a very low applied strain (840µε); clearly the first failure event.

5.3.2 Defect at a +45°/-45° Ply Interface; Model #2

The development of the delamination blister in Model #2 prior to initiation of delamination growth is shown in Figure 5-8. As the load was increased the blister flattened longitudinally, forming a flattened ellipse but, unlike the previous model, the blister rotated such that, at initiation of growth, the major axis of the ellipse was at 105°. At delamination initiation the peak height had risen to about 1.1mm; 45% lower than in the previous model.

The shape of the blister was also reflected in the magnitude of the mode I and II components (Figure 5-9). Upon loading, the mode I and II components were greatest at the lateral and longitudinal extents of the delamination respectively. The mode II component was the larger but both were considerably less than that required to initiate delamination growth. As the load increased, the mode I peaks increased and shifted to the 105° and 285° positions (the major axis of the delamination blister) and at initiation of delamination growth the mode I peak was significantly greater than the mode II peak.

The predicted delamination initiation strains for Model #2 are shown in Table 5-4. For the 0°/90° ply interface data set, this varied from 3220µε (Exponential Hackle Criterion) to 4360µε (Hackle Criterion); an average of 3883µε with a spread in predictions of 29.4%. Growth was predicted to have initiated at the boundary of the major axis of the blister (105° and 285°). It should be again noted that some of the criteria were indeterminate for regions where the mode I component was negative.

As with the previous model, the coupon data had some effect on the prediction of delamination growth, particularly when using complicated failure criteria. The 0°/0° ply
Finite Element Delamination Studies

interface data gave a significantly lower spread between different criteria than the $0^\circ/90^\circ$ ply interface data. Overall, for a given criterion, the spread between data sets in this model was greater than for the previous model and, as with the previous model, the $0^\circ/90^\circ$ ply interface data gave the lowest (most conservative) prediction; $3883\mu e$, whilst the dry $0^\circ/0^\circ$ ply interface data at a crack length of $a=60mm$ gave the highest result; $4255\mu e$. The most conservative criterion using the $0^\circ/0^\circ$ ply interface data was again the Ramkumar criterion whilst, for the $0^\circ/90^\circ$ ply interface data, the Exponential Hackle was the most conservative.

Ply splitting was again predicted to have occurred (Figure 5-10). The peak in the transverse strain was at about 75% of the radius from the centre of the blister, at about $50^\circ$ and $230^\circ$. At an applied strain of $3190\mu e$, the strain at this point exceeded the material strength, suggesting ply splitting was the first event, although this was dependent on which particular delamination failure criterion was chosen.

5.4 Comparison with Experimental Results

The above predictions are compared with the experimental results in Table 5-5. Figure 5-11 shows a comparison between the experimental and predicted blister shapes at an applied strain of $3000\mu e$. In both models, the blister shape, orientation and height exhibited excellent agreement with the experimental results. However, there was poor agreement (Table 5-5); the observed delamination initiation strains were 36% and 23% greater than the predicted values for Models #1 and #2 respectively.

There was good correlation between the experiments and predictions of the ply splitting (Figure 5-12); this Figure shows the location of the predicted and observed splitting. For the defect at the $0^\circ/90^\circ$ ply interface (Model #1), the fractographic observations indicated that the splitting was at the $90^\circ$ and $270^\circ$ sites on the boundary, as was predicted. The predicted strain for the splitting was low and the fractographic evidence had indicated that the splitting had occurred prior to the delamination growth. For the defect at the $+45^\circ/-45^\circ$ ply interface (Model #2), the splitting was observed at the same location (75% from the centre of the defect) as was predicted.
6. Discussion

6.1 Delaminations in Real Structures

In this Section, the results from each of the three phases of the work (impact studies, mixed-mode studies and embedded delamination studies) are discussed. Then, the relationships between these sets of results are discussed.

6.1.1 Background

6.1.1.1 Damage Mechanisms

Before discussing the results of the impact studies on the skin-stringer panels, the damage mechanisms and parameters which affect the relative proportions of the damage processes in impacted plain laminates and structures will be briefly considered. The results from Chapter 2 will then be discussed in terms of these mechanisms and parameters.

When a structure is impacted, the energy from the impact is absorbed by a variety of mechanisms; damage in the laminate, elastic response, local deformation of the impactor and rebound energy of the impactor. The energy absorbed through damage can be further split into components; permanent deformation of the impacted surface, friction between the fracture surfaces, matrix cracking, delamination and fibre fracture. The last three contributions are the dominant damage mechanisms for energy absorption in coupons.

Generally, the first fracture event during an impact is the formation of transverse cracks within the plies, caused by through-thickness shears generated by the out-of-plane impact forces. The energy required to generate these cracks has been shown to be about 10kJ/m². Transverse cracks predominantly occur within blocked plies and near the free surfaces of the laminate.
Delamination is the dominant process during impact and dissipates the majority of the imparted energy\textsuperscript{170,183}; under certain conditions the total delamination area is directly related to the total impact energy\textsuperscript{172,62,170,183}. Delaminations are usually initiated by opening forces at transverse cracks\textsuperscript{4}, which can be predicted using a 'strength of materials' based criterion\textsuperscript{42,43}. Delamination growth is driven by interlaminar shear stresses (mode II) and opening stresses (mode I)\textsuperscript{170}; the former (mode II) is the larger component and is related to the bending of the laminate during impact\textsuperscript{30}. There are two aspects to delamination; initiation and growth. Delaminations preferentially occur at orthogonal ply interfaces (i.e. +45°/-45° and 0°/90° ply interfaces) due to the large difference in stiffness across these interfaces, promoting interlaminar forces\textsuperscript{172,167,170,42,43,168,9,183}. Delamination growth typically requires between 300 and 600J/m\textsuperscript{2} to propagate\textsuperscript{28,183}. Preferential growth directions lead to the characteristic 'peanut' or 'butterfly' shaped lobes which are orientated parallel to the lowermost plies of the delaminated interfaces\textsuperscript{170,62}. Through the thickness, the delamination and through-thickness cracks generally form a cone of damage with the greatest extent at the backface\textsuperscript{4,183}.

Fibre fracture can be a large energy absorbing mechanism and is attributed to the high through-thickness forces generated during impact\textsuperscript{172,42,183}; typically it dissipates between 30 and 100kJ/m\textsuperscript{2}. However, this mechanism has the greatest effect on the residual strength since it can introduce damage to the load-bearing plies\textsuperscript{42,168}.

6.1.1.2 Impact Damage Parameters in Plain Laminates

In plain laminates the relative proportions of the different damage modes are controlled by a variety of parameters; impactor conditions, material properties, stacking sequence and laminate geometry\textsuperscript{183}.

The impactor conditions (energy, mass and velocity) have an important effect on the damage state. Generally, as the energy increases, so does the amount of damage formed\textsuperscript{42,183}. However, for a given impact energy, low mass/high velocity impacts (hail, runway debris) are more critical than high mass/low velocity impacts (dropped tools)\textsuperscript{184,169,185}. This is due to the local response of the laminate; at high velocities
dynamic effects dominate, and the proportion of material which has time to respond to the impact is smaller. At lower velocities, a larger area of material can respond to the impact, leading to more energy being absorbed in elastic deformation of the material. Consequently, there is greater absorption as damage in the high velocity case.

The impact velocity, and to a certain extent the energy, also controls the damage mechanisms which occur. At low velocities, delamination and transverse cracks dominate and the damage area is proportional to the impact energy\textsuperscript{42,183}; the impact can be successfully modelled as static indentation\textsuperscript{1,24,183,184,169}. The dynamic forces produced are not high enough to generate fibre fracture\textsuperscript{186}.

As the impactor velocity increases, the dynamic forces become higher, leading to fibre fracture and a reduction in delamination. Ultimately, at high velocities, very high forces are generated, leading to penetration with little delamination. Parameters such as impactor shape, which can affect the degree of penetration, can be important\textsuperscript{24,185}.

The material properties, particularly the matrix dominated properties, have the largest effect on impact damage\textsuperscript{183}. In general, tougher materials have better impact damage resistance than brittle systems, such as thermoset composites. As toughness increases, the degree of transverse cracking and delamination, and thus the damage area, falls\textsuperscript{187,167,183}. Ultimately, in very tough materials, the damage consists of fibre fracture and plastic deformation of the front face\textsuperscript{9,26,183}. The shear toughness (G\textsubscript{11}) has been successfully related to the impact resistance and residual compressive strength\textsuperscript{43,171}. However, other parameters such as fibre surface treatment, moisture content\textsuperscript{167} and fibre stiffness and strength\textsuperscript{183} can also affect the impact damage resistance.

The stacking sequence can have a large effect on the impact damage\textsuperscript{187}, which has important implications for design of damage tolerant structures. Delaminations occur within orthogonal ply interfaces\textsuperscript{172,167,170,183} and transverse cracking within blocked plies, which has lead to the current design practice of dispersing similar plies\textsuperscript{167}. To improve the residual strength it is current practice to position angle plies on the surface.
Discussion
to reduce damage to the load-bearing axial plies$^{167,171,9,4}$. In general, matrix dominated
stacking sequences (‘soft’ laminates), which contain a high proportion of off-axis plies,
are more damage tolerant than fibre dominated stacking sequences (‘hard’ laminates),
which contain a high proportion of load-bearing plies$^{9,171,183}$.

The effect of laminate geometry has been extensively studied and is particularly
important at low velocities. In thin or wide laminates the dominant energy absorbing
mechanism is elastic response; much of the energy is elasticity dissipated away from the
impact site$^{42}$. As the laminate get thicker or the span shorter, energy is increasingly
absorbed through the generation of damage. In addition, the flexural stiffness of the
laminate will rise$^{30}$, reducing the local curvature during impact, thus promoting contact
forces and through-thickness shear$^{62}$. Fibre fracture develops at the front face which
absorbs large amounts of energy, leading to a reduction in the delamination extent$^{172}$.
There is a secondary effect; as the thickness increases so does the proportion of impact
energy utilised in displacing the laminate. In thick or short laminates, the impact energy
is almost entirely absorbed through fibre fracture; the damage is close to the front face
and there is often a single shear delamination close to the mid-plane$^{9,43,172}$.

6.1.1.3 Impact Damage Parameters in Structures
Although impact damage in plain laminates has been extensively characterised, in
structures a number of additional parameters are important. Overall structural geometry
and proximity of the impact site to the substructure significantly affect the structural
response and the extent to which the different damage mechanisms occur$^{30}$. At a stringer
foot the change in thickness can generate twisting forces during impact which promotes
damage development$^{30}$. For ply-drop-offs, which exhibit similarities with stringer feet,
the damage follows the line of greatest stiffness$^{167}$. In general, impact in a bay between
stringers generates the greatest amount of damage$^{188}$ since locally, only the skin can
absorb the incident energy. However, when the impact is over a stringer both the skin
and the stringer can absorb the energy.

A further important parameter is the dynamic response of the entire structure$^{185}$. In large
structures, energy can be absorbed over a wide area, which can lead to damage
development well away from the impact site. For example, impacts on panels with adhesively bonded stringers can lead to extensive skin/stringer debonding. The dynamic response is strongly controlled by the boundary conditions; clamped constraints lead to more damage than simply-supported constraints. The structural response is also related to the relative mass of the impactor and structure.

6.1.2 Effect of Impact Location on the Damage Area

Firstly, consider the effect of the impact site on the damage extent (Figure 2-6). The reduction in damage area as the impact site approached the stringer can be explained by considering the change in the damage mechanisms which dominate the energy absorption, and by examining the variation of damage area with peak force and displacement (Figures 2-7 and 2-8 respectively).

At the impact site in the bay there is a relatively low flexural stiffness, so the peak force is low and the peak deflection is large, leading to high curvatures and subsequently high interlaminar stresses. Therefore, transverse cracking and delamination dominate the energy absorption. As the impact site approaches the stringer, the local flexural stiffness increases, which increases the contact forces at the impact site. Therefore, the dominant mechanism changes to fibre fracture at the front face and there is a reduction in the delamination extent. In addition, the increase in local thickness, close to and over the stringers, increases the energy absorbed through elastic deformation; over the stringer centreline most of the energy is absorbed in elastic deformation. Ultimately, at the centreline, the very high forces lead to very limited delamination and transverse cracking, and some fibre fracture at the front face.

There was a discrepancy in these trends; the damage area of site B (in the bay, close to the stringer) was greater than that of the impact in the bay (site A). This was attributed to two factors. Firstly, site B was further from the end constraints than all the other sites which would have led to greater deflections and higher interlaminar stresses. Secondly, the local change in stiffness in the region of the impact site may have lead to twisting which would have promoted delamination growth. The evidence for the second factor
Discussion

was strengthened by the greater extent of the delaminations towards the stringer side of the impact site.

The results from the impact on the plain panel are also shown in Figures 2-6, 2-7 and 2-8. In Figure 2-6, the ‘distance from the stringer’ for the plain panel impact was chosen as 50mm since this was the radius of the support ring. The damage areas in the plain panel impact and the impact in the bay (impact site A) were similar. However, the constraint of the support ring in the plain panel led to higher peak forces and lower maximum deflections than would have been expected in the stiffened panel. This indicates that the plain panel constraints were greater than those in the stiffened panel, so less energy was absorbed through elastic response and more as damage.

6.1.3 Effect of Impact Location on the Damage Distribution

Although the damage area gave an indication of the energy absorbing processes, the distribution of the damage through the thickness gives a clearer picture of the dominant mechanisms. The impact damage in the plain laminate and the bay of the stiffened panel were fairly typical of low velocity impact damage in thin laminates (Section 6.1.1). There was negligible fibre fracture and the damage was mainly delamination and transverse cracking in a pyramidal distribution. The delaminations formed ‘peanut’ shaped lobes and were generally at orthogonal ply interfaces. The transverse cracking was predominantly on the back surface and within the 90° plies in the mid-thickness. However, compared with the plain laminate impact, there was less damage at the bay impact; this was attributed to a greater degree of energy absorption in the stiffened panel through structural response.

As the impact site approached the stringer, the damage distribution changed, although impact site B was slightly anomalous, with delaminations ‘attracted’ to the region of the stringer foot. This could be attributed to the local stiffness change in this region which promoted twisting and led to delamination growth towards the stiffest region; the stringer foot as noted above.
When the impact site was either at the stringer foot (impact site C), or the stringer centreline (impact site D), a larger proportion of the impact energy was absorbed elastically by the sub-structure such as the stringer web and cap. Also, the flexural stiffness increased, raising the contact forces and thus changing the dominant damage mode to fibre fracture at the front face. The undamaged region below the front face was attributed to the through-thickness compression stress wave associated with the impact, which would have eliminated delamination initiation directly beneath the impact site. For impact site C there was the added complication of ply-drop-off in the stringer foot, which clearly promoted delamination and transverse cracking. The change in thickness in this region probably generated opening forces at the backface.

Although the delaminations at impact site C predominantly had a $90^\circ$ ply lowermost, the largest delaminations were elliptical with the major axis parallel to $0^\circ$ plies. This apparent discrepancy is an artifact of the sectioning direction being parallel to the $90^\circ$ ply. The greater extent of damage along the stringer, the direction of greatest stiffness, was attributed to delaminations growing parallel to the $0^\circ$ plies. Finally, for the impact directly over the stringer centreline (impact site D), there was only limited delamination, and those which did occur were solely within $0^\circ/90^\circ$ ply interfaces. This was due to most of the damage being absorbed by the sub-structure and by fibre fracture, which was evident from the damage being predominantly close to the front face.

Based on these findings, the choice of embedded delamination size, shape and location was discussed in Section 4.2.1.

6.2 Mixed-Mode Tests

6.2.1 Discussion of Test Method

The MMB test method was quite successful for characterising the two materials and two ply interfaces under investigation, and generally the scatter was below 10%. However, high scatter did not necessarily imply poor test procedures and could be attributed to the complicated damage mechanisms during fracture (Section 6.2.2.1). The tests highlighted the importance of specimen preparation and ensuring that the hinges were correctly
Discussion

aligned. This problem could be partly overcome by using end-blocks or clamps\textsuperscript{84} rather than hinges since these can be more accurately attached to the specimen\textsuperscript{89}.

The testing was particularly prone to scatter under mode II dominated loading because of crack instability and sensitivity to rig geometry. The former could be reduced by lowering the loading rate although this could introduce creep effects. Alternatively, a stable mode II method, such as the End Loaded Split (ELS) or the 4ENF test method\textsuperscript{69} could be employed to characterise pure mode II fracture. However, using a second test method not based on the MMB would introduce inconsistencies. Sensitivity to rig geometry could be reduced by shortening the upper span, but this reduced the maximum crack length.

Prior to testing, all the specimens were precracked, to ensure that conditions at the crack tip during testing were identical. This also aided in reducing problems such as tearing in the starter insert. One unfortunate consequence was that the initial crack tip conditions are slightly different from those which would occur from the embedded defects described in Chapter 4. However, the conditions in the mixed-mode test specimens were similar to those at the boundary of in-service damage, such as from impact.

The $0^\circ/90^\circ$ ply interface specimens introduced further problems with the test method. In particular, as was discussed in Section 3.2.7, the inserts along the edge led to difficulties in monitoring the crack front position. However, through optimising the specimen stacking sequence to improve the transverse bending stiffness, the significance of this was reduced.

There was some question over the validity of the data reduction schemes for the multidirectional specimens, in particular the validity of Equations 1-9 and 1-10. These expressions are based on unidirectional calculations, and the slightly different stacking sequences of the two arms of these MMB specimens could introduce error in the analysis. Unfortunately, the NASA data reduction scheme (Equations 1-7 and 1-8) has a similar problem; the parameter $\lambda$ is dependent on the stiffnesses of the sublaminates.
Discussion

This possible source of error should be considered when analysing the multidirectional results but it is felt that the general findings from these tests, such as damage growth mechanisms, are still valid. Finally, the weight of the lever (8 N), was considered to have a negligible effect on the results.

A further effect of changing the specimen stacking sequence and geometry was to change the residual stresses. In particular, through-thickness residual stresses could have a significant effect on the local mixed-mode loading conditions at the crack tip. However, it is difficult to quantify these stresses and they would be highly dependent on the thickness of the resin interlayer at the mid-plane. Fortunately, the lay-up of the $0^\circ/90^\circ$ ply interface specimens was similar to that of the embedded defect specimens under investigation in Chapter 4; so the residual stresses would be expected to have been similar. However, the difference in the residual stresses could introduce a further complication when comparing the toughesses of $0^\circ/0^\circ$ and $0^\circ/90^\circ$ ply interfaces.

6.2.2 Discussion of Failure Analysis

6.2.2.1 Discussion of General Failure Mechanisms

To interpret the test results and failure loci it is prudent to discuss the toughness mechanisms evident from the fractographic analyses.

In a delaminated laminate under mode I dominated loading, the dominant failure mechanism was fibre bridging, where tows of fibres spanned the opening crack faces. To open these crack faces further these fibres had to be broken which required enormous amounts of energy and greatly enhanced the toughness. Fibre bridging is mainly attributed to divergence at the crack front and migration into the plies either side of the interface. As the crack length increased, the number of bridged fibres increased which further enhanced toughness; this mechanism was responsible for the R-curve effect. The degree of fibre bridging was controlled by a number of factors such as the nesting of the plies, fibre/matrix bond strength and fibre stiffness. It is evident from both the fractographic analysis and R-curve results that the degree of fibre bridging was greatest at 87.5% mode I loading, although the reason for this was unclear.
As the mode II component increased the degree of fibre bridging fell. This was attributed to the shear contribution modifying the process zone ahead of the crack tip, such that it remained within the central interply region. In brittle epoxy systems the matrix failure changed from relatively smooth cleavage fracture to scarps and cusps. This resulted in a local increase in the amount of matrix fracture per unit surface area, increasing the global toughness. As the cusp shape became more contorted (more fractured area) and serrated feet are formed, the laminate toughness was further enhanced. In tougher systems the cusps were poorly formed and consequently there was less fractured matrix per unit surface area. However, plasticity contributed to the toughness through micromechanisms such as ductile drawing, plastic deformation and cavitation\(^6\). Finally, under mode II dominated loading, contact between the crack faces led to frictional forces, rotation of the cusps and the generation of surface debris. These were significant toughness enhancing mechanisms.

At multidirectional ply interfaces further mechanisms occurred\(^2\). Consider the interply zone at the specimen mid-plane (Figure 6-1). At 0°/0° ply interfaces, the thickness of the central interply zone varied across the specimen width, but will be relatively constant along the crack growth direction. However, at 0°/90° ply interfaces, this interply zone exhibits significant variation in thickness in the crack propagation direction. The measured toughness is an average across the specimen width and is very dependent on this interply thickness\(^1\). Consequently, at 0°/90° ply interfaces, larger variations in toughness occur along the specimen length than in 0°/0° ply interfaces.

6.2.2.2 Discussion of Cusp Angles

A very significant observation was the variation in cusp tilt angle with mixed-mode ratio (Tables 3-17 and 3-18); under mode II dominated loading there was a strong relationship between the mixed-mode ratio and cusp angle. However, as the mode I component increased, there were fewer cusps and the tilt angle became less sensitive to the loading conditions; the mixed-mode ratio could only be deduced approximately
from the surface morphology. There was little variation in cusp angle between specimens tested under the same mixed-mode loading conditions.

The mechanism by which cusps are generated gives an insight into this relationship. Purslow\textsuperscript{13} gave a quantitative explanation for the formation of cusps under pure mode II loading. From this, a simple model was developed and is shown in detail in Appendix D. This lead to the following expression relating the cusp tilt angle, $\beta$, and the mixed-mode ratio ($G_1/G_{II}$).

$$\frac{G_1}{G_{II}} = A \cot^2 \beta$$

(6-1)

where $A$ is a constant. The average values of $\beta$ were substituted into this expression and the results are shown in Table 6-1 and Figure 6-2. The non-linear nature of these results indicate that the model needs to be refined. Under mode I dominated loading the combination of the low sample size and fibre bridging may have introduced errors. Under mode II dominated loading, the cusp angle may have been modified by frictional forces between the crack faces leading to post-failure plastic deformation and rotation of the cusps. Another refinement would be to include the effect of the bimaterial interface (fibre/matrix interface) and residual stresses which may have modified the local mixed-mode ratio at the crack tip.

These tests have illustrated that the fracture morphology of delaminated laminates can be used to glean information about the loading and environmental conditions during failure. This has important implications for post-mortem failure analysis. The matrix plasticity and definition of the crow feet are indicators of the moisture content at failure. The variation in cusp tilt and shape with mixed-mode ratio could provide a link between predictive models and experimental observations. This technique could be an indispensable tool in the post-mortem analysis of failed components.

6.2.3 Discussion of Failure Criteria

The two materials studied exhibited very different failure loci. The loci for T800/5245 were non-linear and, under certain conditions, exhibited a large hump near the mode I
Discussion

axis, whilst the loci for T800/924 were linear. This indicated that there was a greater interaction between the mode I and II components in T800/5245 than in T800/924.

For unidirectional T800/5245, none of the criteria studied could model the failure loci accurately; the hump close to the mode I axis was particularly difficult to model. In general, criteria which predicted the locus shape ($\chi^2$ ranking) poorly predicted the pure mode I toughness ($G_{IC}$ ranking). However, all the criteria were conservative over most of the mixed-mode spectrum, so they could be used for design. Overall, the General Interaction and Power criteria, the former having some physical basis, gave the best results for T800/5245.

For unidirectional T800/924 most of the criteria modelled the failure loci quite well and gave lower minimum values of $\chi^2$ than for T800/5245. This was attributed to the linear nature of the loci of T800/924 which made was easier to model. The General Interaction criterion was clearly the best model, giving the lowest values of $\chi^2$ and the best predictions of $G_{IC}$.

The criteria which best modelled the toughness of the $0^\circ/90^\circ$ ply interface in T800/924 were generally the same as those which were best fitted for $0^\circ/0^\circ$ T800/5245. This is attributed to the non-linearity of the loci for the $0^\circ/90^\circ$ ply interface results, caused by the complex mechanisms at this ply interface. For these results, the Exponential K, Exponential Hackle and General Interaction criteria best modelled the failure locus.

Overall, the General Interaction and Power criteria were the best models of all the failure loci. The General Interaction criterion was physically based and quantifies the interaction between the mode I and II components, which gives confidence when using this criterion. The Power criterion is entirely empirical, but can model a wide range of loci. The Linear criterion, which is the most commonly used in industry, was one of the poorest models, particularly for predicting the shape of the failure loci. For the data from T800/5245 and $0^\circ/90^\circ$ T800/924, the Linear criterion will be conservative, so its
use in design can be justified. However, for unidirectional T800/924 this criterion can be non-conservative, leading to problems with its use.

6.2.4 Effect of Crack Length on Failure Loci

The effect of crack length on the failure loci (averaged $G_I$ versus averaged $G_{II}$) for T800/5245 and T800/924 are shown in Figure 3-17. Firstly, for T800/5245 the high degree of fibre bridging led to an increase in toughness with crack length. This was greatest under mode I dominated loading (87.5% mode I), particularly in wet laminates. It was noted that 'quilting' of the insert was significant in these laminates, which may have further increased the degree of fibre bridging. As the proportion of mode II increased, there was a reduction in the degree of fibre bridging, reducing the effect of crack length. However, this reduction in toughness was compensated by the formation of cusps and serrated feet, so the failure loci were almost linear under these conditions. Finally, under mode II dominated loading, there was an increase in toughness (the loci flattened), which was attributed to the generation of surface debris. In T800/924 crack length had much less effect on the toughness, which was attributed to the limited degree of fibre bridging in this material.

6.2.5 Effect of Moisture on Failure Loci

Generally moisture only had a limited effect on the failure loci. The wet laminates exhibited more fibre bridging and matrix plasticity than the dry laminates, particularly in T800/5245. In this material, the increase in fibre bridging may have been due to an increase in matrix plasticity or a weakening of the fibre/matrix bond\textsuperscript{148}. As the proportion of mode II increased, the degree of fibre bridging fell and matrix effects became important. The matrix was relatively brittle in the dry laminates, leading to a greater toughening contribution from mechanisms such as serrated feet and cusps. In the wet laminate, the matrix was less brittle so these mechanisms were not prevalent, leading to the dry laminates being slightly the tougher.

Although the moisture content in T800/924 was twice that of T800/5245, in the former the dry and wet loci were almost coincident. This, and the similarity in fracture
Discussion

morphology between the wet and dry cases, indicated that absorbed moisture only had a limited effect on the toughness of T800/924.

6.2.6 Effect of Material Type on Failure Loci

The effect of resin type on the failure loci (averaged $G_1$ versus averaged $G_{11}$) are shown in Figure 3-18. Generally, the fracture processes in T800/5245 were more brittle than in T800/924. However, there was significantly more fibre bridging in T800/5245 which is attributed to the high degree of nesting in this system. In dry laminates, at short crack lengths, the superior resin toughness in T800/924 had a comparable toughening contribution to fibre bridging in T800/5245, so the toughnesses were similar. However, at large crack lengths, fibre bridging dominated and T800/5245 was the tougher, particularly under mode I dominated loading.

In T800/5245 moisture led to a reduction in the formation of serrated feet whilst in T800/924 it led to an increase in matrix plasticity. For short crack lengths under mode I dominated loading, the toughening effects of fibre bridging (T800/5245) and matrix deformation (T800/924) were similar so the loci were coincident. However, as the mode II component increased, fibre bridging reduced and consequently T800/924 was the tougher. Generally at large crack lengths, fibre bridging was dominant and T800/5245 was the tougher. Only under mode II dominated loading, when fibre bridging was more limited, did the superior matrix plasticity of T800/924 give it a comparable toughness to T800/5245.

6.2.7 Effect of Ply Interface on Failure Loci

Figure 3-22 showed the loci for $0^\circ/0^\circ$ and $0^\circ/90^\circ$ ply interfaces in T800/924. In the latter, further mechanisms contributed to the fracture morphology and toughness, as discussed in detail by Singh and Greenhalgh.

In Figure 3-14, the cusp formation mechanism at $0^\circ/0^\circ$ ply interfaces was illustrated. In $0^\circ/90^\circ$ ply interfaces, a further mechanism made a significant contribution to the toughness; deep $90^\circ$ transverse cracking or 'ribs'. Under mode II dominated loading, these features had a similar spacing to the $90^\circ$ fibres, to give a grid-like fracture
Discussion

morphology. During loading, tensile microcracks form at the interface between the 90° fibres and the interply resin layer (Figure 6-3). The initiation mechanism is unclear, but the angle of these cracks was orthogonal to the resolved tensile stress. Under mode I dominated loading, these cracks were at quite a shallow angle, and consequently only a limited number could extend far enough through the interply zone to intersect with the main fracture plane. However, as the mode II component increased, these cracks were steeper, and only needed to extend a small distance to intercept the main fracture plane; an increase in the number of ribs. Under mode II dominated loading, all these cracks could extend across the short distance to the main fracture plane. The initiation and growth of these secondary cracks generated more fracture surface, leading to an increase in toughness.

In the light of this rib mechanism, the difference between the failure loci for 0°/0° and 0°/90° ply interfaces (Figure 3-22) can be explained. Under mode I dominated loading there were few ribs, so the toughnesses of the two interfaces was similar. Similarly, under mode II dominated loading, although the ribs were numerous, the distance they extended was relatively short, so the contribution to the toughness was small. However, under mixed-mode loading, the ribs were relatively numerous and they extended a significant distance through the interply zone. This could partly account for the enhanced toughness of the 0°/90° ply interface under these loading conditions.

At a given loading condition, the 0°/90° ply interface had cusps at a greater angle than on the 0°/0° ply interface. This could be related to differences in residual stresses between the two interfaces which would lead to a difference in local mixed-mode conditions. In addition, the bimaterial interface may have led to local rotation of the crack plane, as discussed elsewhere\textsuperscript{70,71}. At a given mixed-mode condition the greater tilt angle in the 0°/90° ply interface would have lead to a higher toughness due to an increased fracture area.
6.3 Experimental Delamination Studies

6.3.1 Discussion of Testing and Analysis

The design of the delamination specimen, using a honeycomb sandwich panel, proved to be very successful. There would have been significant limitations with using an anti-buckling guide to support the laminate. Most notably, the working strains were too low (below 4500$\mu$e), the maximum damage extent was limited and the sides of the guide obscured the Moiré grating. The honeycomb sandwich design allowed delaminations to be grown at strains in excess of 9000$\mu$e; exceeding current design strains for present and future composite structures. Furthermore, it was demonstrated that the performance of these panels was not reduced by the buckling of the delaminated plies and the subsequent loss of stiffness to one face. The large width of the sandwich panels allowed considerable damage growth to occur before the sides of the panel were reached. Finally, the delaminated surfaces were clearly visible and the fringes were not obscured by test fixtures.

Unfortunately, there were some problems associated with this specimen design, in particular the high cost of manufacture. This was attributed to the high tolerances in the specimen length which are required to ensure uniform loading; it is not clear how this cost could be reduced. A further consideration is the loading conditions on the delaminated region. Because the sandwich construction eliminated any bending loads on the delaminated region, the delamination grew under pure in-plane loading. In-service structures, particularly aircraft components which have thin skins, often experience a bending component. However, although the loading conditions in these tests were relatively simplistic, the subsequent damage growth was complex; this justifies using such a simplified specimen to understand delamination growth. In further work, the effect of bending could be introduced and, based on the results of the current studies, characterised. Recent work\textsuperscript{27} conducted on damage growth in structural elements indicates that very similar failure processes occur in realistic structures.
Moiré interferometry is a powerful experimental technique for characterising damage growth such as delamination. The fringes are easy to generate and allow a real-time monitoring of the damage extent. However, the resolution can be quite limited, particularly for monitoring shallow gradients such as at the delamination boundary during initiation of growth. Furthermore, this method will not indicate if in-plane delamination growth occurs, such as from pure mode II failure. For large blisters, the height of the disbond during growth can be such that it impinges with the grating, leading to ‘white-out’ of the centre of the damage, as shown in Figure 4-8.

The analysis of the Moiré data can be time consuming and would be improved by using a more sophisticated and automated image analysis system. The difficulty in resolving the fringes at shallow surfaces can lead to errors in measuring the damage extent. Comparing identical panels (B and I) illustrated the difficulties encountered in determining when damage growth initiated. However, these errors were small for subsequent growth of the damage and overall the results were quite consistent. Methods such as acoustic emission would be more conducive to detecting initiation of delamination growth.

Fractographic analysis of the damage growth proved to be very successful. The mechanisms were comprehensively studied and well understood. However, the cusp angle measurement and the subsequent mapping of the mixed-mode distributions were not so successful. In the controlled coupon tests (Section 3.5.3), the measurement of the cusp angles was quite consistent and the scatter low. However, the measurements from cusps on ‘real’ delaminated surfaces exhibited a larger scatter which was attributed to a number of factors.

Firstly, on mode I dominated surfaces the cusp tilt angle was small, leading to errors in measurement. The limited number of cusps on these surfaces accentuated this error, leading to the high scatter in the average angle. For mode II dominated surfaces there were numerous cusps and the tilt angles were large. However, some cusps had tilt angles greater than those observed from pure mode II fracture in coupons. This suggested that
some rotation of the cusps may have occurred during fracture which was attributed to a compressive mode I component. Such loading would have led to the surfaces remaining in contact after fracture, leading to post-failure rotation of the cusps. Currently, there is no test method available for characterising such compressive mode I components (Section 1.4) although such conditions clearly occur in practice, particularly at the longitudinal boundary of the damage.

The final area in which a large degree of scatter in cusp angle was observed was at changes in fracture plane, such as ply splitting. Within such regions the local stress fields were complex and rapidly changed as the delamination grew; close to splitting there were large out-of-plane forces, promoting mode I loading, but away from the splitting the out-of-plane forces rapidly diminished and mode II loading dominated. Such changes occurred over relatively small distances leading to excessive scatter in the cusp angles. This effect introduced difficulties when comparing similar locations in different panels. Subsequently, it is recommended that there is only justification for using the mixed-mode distributions deduced from cusp angles to compare trends.

6.3.2 Delamination Growth Mechanisms

The general mechanisms by which delaminations initiate and grow were discussed in the Chapter 1 (Figure 1-2). The key aspect of this mechanism is the buckling of the delaminated plies which led to delaminating forces at the defect boundary. The blister shape was always elliptical with the major axis transverse to the loading direction. This can be explained by considering the forces around the delamination boundary\(^8\) which are illustrated in Figure 6-4. At any point on the insert boundary there was an outwards bending moment on the delaminated plies (Figure 6-4a). However, the direct stress on the delaminated region varied around this boundary. Parallel to the loading axis (Figure 6-4b), there was a compressive stress due to the applied load, albeit somewhat relieved by the defect buckling. This tended to close the crack, thereby reducing the effect of the bending moment. Therefore, the delaminated plies were in contact with the sublamine beneath the defect and mode II dominated any crack extension. In contrast, transverse to the applied load there was a tensile stress on the delaminated plies due both to the
Discussion

out-of-plane deflection and to the lateral Poisson expansion of the sublamine (Figure 6-4c). This tensile stress reinforced the bending moment and promoted opening of the crack tip; any subsequent crack growth was dominated by mode I fracture. These differing loading conditions at the longitudinal and lateral boundaries of the blister lead to the observed elliptical shape of the disbond and the observed maxima for the mode I and II components on the lateral and longitudinal insert boundaries respectively.

The stacking sequence of the delaminated plies was a further factor which contributed to the blister shape. If the stacking sequence of these plies was almost balanced, such as for the defects at the $0^\circ/90^\circ$ ply interface, the blister will be aligned perpendicular to the loading direction. However, if the delaminated plies were not balanced, as for the defects at the $+45^\circ/-45^\circ$ ply interface, shear and twist coupling terms were introduced into the stiffness matrix. This coupling, combined with the applied bending moments on the delaminated plies lead to rotation of the blister and consequently, rotation of the locations of the mode I and II maxima.

The overriding factor in controlling the delamination behaviour was the ply orientations at the defect plane; locally, the damage always grew parallel to the uppermost ply, leading to complex fracture morphology, as shown in Figure 6-5. This was an identical mechanism to that in the $0^\circ/90^\circ$ ply interface MMB specimens discussed in Section 3.1.3.5. As shown in Figure 3-13, the resolved stress $\sigma$, leads to migration of the fracture plane upward towards the uppermost ply of the delaminated interface. If this ply is oblique to the growth direction (Figure 3-14b) cracks will extend between the fibres, leading to ply splitting. However, if the crack growth direction is parallel to the upper ply (Figure 6-3), the fracture plane remains parallel to this ply. This growth mechanism dominated the behaviour of the delamination blisters and can be used to explain the fracture morphology shown in Figure 6-5.

6.3.3 Defects at the $0^\circ/90^\circ [3/4]$ Ply Interface

Firstly, consider the damage growth from the inserts at the $0^\circ/90^\circ [3/4]$ ply interface; a micrograph illustrating all the failure processes is shown in Figure 6-5. From studying
Discussion

the Moiré results and fractographic analysis, the sequence of damage growth mechanisms, which was independent of the initial defect size, was deduced (Figure 6-6). The first event was buckling of the delaminated plies above the defect plane (Figure 6-6a). The blister was symmetrical, with the major axis perpendicular to the loading direction, which was attributed to the stacking sequence of the delaminated plies (+45°/-45°/0°) being almost balanced. The first fracture event was splitting of the upper ply (0°) at the lateral boundaries of the blister (Figure 6-6b), which was attributed to the outward bending moment (Figure 6-4c) introducing high tensile strains perpendicular to the 0° fibres. Furthermore, since this ply was at a free surface, the lack of in-plane support by adjacent plies promoted splitting. These splits were densest at the defect boundary because this region underwent the greatest degree of bending. Opening forces at these splits initiated multiple, local, delaminations at the ply interface above, -45°/0°, leading to the generation of the type (ii) surface (Figure 6-6c). These delaminations subsequently converged and extended away from the insert, parallel to the -45° ply. Close to the splits, the lack of out-of-plane constraint promoted mode I dominated fracture. However, away from the insert where the plies were more constrained, the mode I component was reduced.

A similar split-delamination interaction occurred during generation of the type (iii) surfaces (Figure 6-6d). Due to the high bending strains at the blister boundary, splits, tangential to the insert edge, developed in the -45° ply. These lead to delamination between plies 1 and 2 (+45°/-45°) which extended from the defect, parallel to the +45° ply (Figure 6-6e). The larger mode II component away from the insert was attributed to the increased out-of-plane constraint introduced by the adjacent plies. Subsequently (Figure 6-6f), this may have led to the high loads required to generate the -45° fibre fractures along lines A-A and B-B (Figure 4-29). This fibre fracture has important implications for modelling the damage processes since locally this type of failure requires very high forces.

The opening forces at the lateral edges of the insert also led to longitudinal extension of the damage at the original defect plane (Figure 6-6g), between the disbond edge and the
Discussion

lines A-A and B-B (Figure 4-29). Initially, this growth was mode I dominated but, as it extended longitudinally, the mode II component increased, as shown in Figure 6-4b. The overall extension of these three surfaces (type (i), (ii) and (iii)) gave the observed development of the damage from elliptical, to rectangular and finally to a dog-bone shape.

Towards the end of the test, surface splitting developed at the edge of the blister in the uppermost +45° ply (Figure 6-6h). This splitting was attributed to a combination of in-plane shear parallel to the surface fibres and tensile strains perpendicular to these fibres, both of which were due to the deformations of this ply during formation of the type (iii) surfaces. Subsequently, these splits alleviated the local bending forces at the crack tip, arresting the lateral damage growth. However, damage continued to develop at the [3/4] ply interface (Figure 6-6g) as mode II increasingly dominated fracture; this was not monitored by the Moiré interferometry. This failure became increasingly rapid, eventually leading to unstable longitudinal damage growth, forming the type (i) surface.

6.3.4 Defects at the +45°/-45° [5/6] Ply Interface

From the Moiré results and the fractographic analysis, the sequence of mechanisms for the damage growth from the inserts at the +45°/-45° [5/6] ply interfaces was deduced (Figure 6-7). The first event was buckling of the blister (Figure 6-7a) but, unlike the defects at the 0°/90° ply interface, the stacking sequence of the delaminated plies (+45°/-45°/0°/90°/+45°) was unbalanced, leading to rotation of the blister. Subsequently, the maxima for the mode I and mode II components at the insert boundary were rotated by about 15° to the 90° and 0° ply directions respectively.

Because the blister was five, rather than three, plies thick, and one of the plies was orientated at 90°, the transverse bending stiffness was greater than in the defects described in the previous Section. Therefore, for a given applied strain, the bending moment which caused ply splitting was less than that for the defects at the 0°/90° ply interface. Consequently, the first failure event was delamination growth at the +45°/-45° [5/6] ply interface which initiated close to the lateral boundary of the insert. This grew
as a mode I dominated failure parallel to the uppermost ply (+45°), generating the type (iv) surface (Figure 6-7b). However, splitting soon developed in the upper ply (+45°); Figure 6-7c. The splitting was attributed to a combination of transverse tensile and in-plane shear; the mode II fracture observed parallel to the +45° ply (Figure 4-42) was evidence of the latter contribution.

The +45° ply splits initiated delamination growth in the ply interface above (90°/+45°) and led to the generation of the type (v) surface (Figure 6-7d). This delamination was mode I dominated and grew quite rapidly, extending almost parallel to the 90° ply; this is discussed in more detail in the next Section.

As the type (v) surface extended laterally, the type (iv) surface also extended from the insert boundary, parallel to the +45° ply. This was observed in the Moiré images (Figures 4-11) as a pair of slight humps at the top left and bottom right of the insert boundary. As these delaminations extended, the aspect ratio of the blister increased, leading to high longitudinal curvatures. Subsequently, large tensile strains developed across the 90° ply (perpendicular to the fibres) leading to splits developing in this ply (Figure 6-7e). These splits subsequently initiated delamination in the ply interface above, 0°/90°, generating the type (vi) surface (Figure 6-7f).

As shown in Figure 6-7g, the growth direction and mixed-mode conditions at the type (vi) surfaces were very different from those of the type (iv) and (v) surfaces. Close to the 90° splits which initiated the type (vi) surface, mode I dominated. However, away from these splits, the failure was dominated by mode II and grew parallel to the upper ply (0°), which was attributed to the mechanisms illustrated in Figure 6-4. In the early stages of the tests, the three types of fracture surface developed simultaneously. However, later in the test only the type (v) (mode I dominated growth parallel to the 90° ply) and type (vi) (mode II dominated growth parallel to the 0° ply) grew until the test was stopped.
The growth mechanisms from defects at +45°/-45° ply interfaces (Panels E, F, G and H) were very similar to those observed from previous work on delamination growth from inserts at +45°/90° ply interfaces. Simultaneous growth was observed at the +45°/90° (mode I dominated failure parallel to the 90° ply) and 0°/90° (mode II dominated parallel to the 0° ply) ply interfaces. This indicates that the delamination growth mechanisms are relatively material independent but strongly controlled by the local ply orientations.

6.3.5 Effect of Ply Interface on Damage Growth

As was shown from the mechanisms described in the previous Sections, the ply interface in which the initial defect was located dominated the subsequent damage growth. This effect can also be seen by comparing the damage widths for similar defects at different ply interfaces (Figures 4-15 and 4-16).

Firstly, consider the delamination growth initiation strains tabulated in Table 4-2; there was a large error associated with these values so it is difficult to deduce any clear trends. Generally, for a given insert geometry, the delamination growth from the inserts at the 0°/90° ply interfaces initiated at lower applied strains than those from the +45°/-45° ply interfaces. This difference was as much as 75% for growth from the smaller insert (panels A and E), although for the small elliptical insert (panels C and G) the initiation strains were similar. The effect on initiation strain can be explained by considering the damage height prior to growth (Figures 4-17 and 4-18). At a given applied strain, the shallower defects had greater out-of-plane displacements than the deeper defects due to the greater transverse bending stiffness of the deeper inserts. Therefore, at the shallower defect there were greater opening displacements at the lateral boundary of the insert, leading to the lower initiation strain for damage growth. The growth from the defect at the 0°/90° ply interface may have been further promoted by the development of 0° splits which would have acted as initiation sites.

Once delamination growth had initiated, the effect of ply orientation was very different; the damage growth from the 0°/90° ply interfaces was limited while that from the
+45°/-45° ply interfaces extended rapidly. This can be explained by considering the conditions which control delamination growth. In the panels containing defects at the 0°/90° ply interface, the driving force for delamination (mode I) was a maximum transverse to the loading direction. However, delaminations preferentially grow parallel to the uppermost ply (Figure 3-14) and, in this instance, the upper ply directions were at 45° to this driving force; 0°/-45° and -45°/+45° ply interfaces. Since the driving force and the preferential growth directions were not coincident, the growth rate was reduced. Furthermore, as these surfaces were generated and the crack developed a longitudinal component, the proportion of mode II increased. Since toughness increases with increasing mode II component, this mechanism further retarded delamination extension.

In the panels containing defects at the +45°/-45° ply interface, after the damage growth had initiated, it migrated into an adjacent ply interface; +45°/90°. At this interface, the main driving force and the upper ply direction (90°) were almost coincident, resulting in rapid delamination growth and the formation of the type (v) surface. It should be noted that if the shear coupling of the delaminated plies had not been present, the elliptical blister would not have been rotated and subsequently, the driving force and uppermost ply directions would have been exactly coincident. This would have led to very rapid delamination extension.

6.3.6 Effect of Initial Defect Size on the Damage Growth

Although the main factors controlling the damage growth mechanisms were the defect depth and ply interface, the geometry of the initial defect did have a limited effect on the growth processes.

Firstly, consider the damage growth from defects at the 0°/90° ply interfaces. The initial defect length affected the blister height (Figure 4-9); the longer the defect, the greater the height. By definition, for a given applied strain, the reduction in length of the blister was directly proportional to the initial length. Therefore, the curvature of these regions was the same and thus, the longer defect exhibited the greater height change. If the
defects had been much longer, there may have been secondary buckling modes prior to damage growth which could have complicated the growth processes considerably.

There was no clear trend in the initiation strains with initial defect size, although there was an indication that the larger defects initiated at a lower initiation strain than the smaller defects. This agreed with the trends observed in the literature (Section 1.3).

The initial defect shape had a small effect on the damage width for defects at the 0°/90° ply interfaces (Figure 4-10). The elliptical defects exhibited a sharp change in slope late in the test whilst the circular defects did not. It is not clear why this occurred, but it may be related to the formation of large type (i) surfaces adjacent to the insert in the panels containing elliptical defects.

The damage growth processes were relatively independent of the initial defect size. There was some evidence for a larger mode II component in the damage associated with the larger defects; this may be attributed to the greater difference in strain between the blister and the base laminate in the larger defects. This strain difference would result in an increased interlaminar shear between the laminae.

For the damage growth from defects at the +45°/-45° ply interfaces, the initial defect shape and length affected the damage height and width respectively. Firstly, the larger the defect area, the greater the damage height. For the defects at the 0°/90° ply interface, the blister was aligned with the loading direction and so height was only controlled by the initial length. However, for the +45°/-45° ply interface defects, the rotation of the elliptical blister may have led to contributions from both the insert length and width to the damage height.

For the defects at the +45°/-45° ply interface, the initiation strain was inversely proportional to the insert length (Table 4-2) and the damage height (Table 4-2 and Figure 4-13). The defect length influenced the degree of longitudinal bending of the delaminated plies, which in turn influenced the degree of lateral bending of these plies.
Discussion

The greater the lateral bending, the greater the opening forces at the lateral boundary, promoting delamination growth and reducing the initiation strain. A similar effect may have occurred for the shallower defects although the growth in this instance was dominated by ply splitting at the boundary.

The initial defect length also affected the increase in width (Figure 4-14); the longer defects exhibited greater increases in damage width. This again was attributed to the greater bending in the longer insert generating larger opening displacements at the lateral delamination boundary.

For the panels with inserts at the +45°/-45° ply interface, the damage growth processes were relatively independent of the initial defect size. For the circular inserts (panels E and F) there was some evidence of a greater mode II component in the larger defects which was again attributed to the difference in strain between the blister and the base laminate. However, for the elliptical insert (panel G), there was an overall increase in the mode I component compared with the smaller circular defect (panel E); the reason for this was unclear.

6.3.7 Effect of Specimen Variation on the Damage Growth
For the damage development in nominally identical panels (B and I), the development of the blister and the damage mechanisms were almost identical. However, the initiation strain and the mixed-mode distributions differed. Firstly, the difference in the initiation strains was mainly attributed to the inherent error in measuring these values. Similarly, the difference in the mixed-mode distribution was primarily due to the difficulty in comparing equivalent locations on different specimens. This reinforces the recommendation that mixed-mode distributions should only be used to indicate trends.

6.4 Finite Element Delamination Studies

6.4.1 General Aspects of the Model
Before discussing the results of the modelling, the simplifications in the models are considered. The largest simplification was to use 2D shell elements; when using shell
Discussion

elements, the aspect ratio (thickness/width) should be no more than 0.1\textsuperscript{189}. However, some the elements exceeded this aspect ratio and in such instances the element behaviour could lead to increased shear stiffness. This would have led to errors in the displacements and may have contributed to the over-prediction of the delamination initiation strains.

Another simplification was to model the sublamine beneath the defect plane as a rigid surface. This greatly reduced the processing time but meant that load shedding by the delaminated plies could not be modelled. This may have affected the mixed mode conditions; the simplification would reduce the mode I component and increase the mode II component. The deeper the delamination plane, the greater the effect of this simplification. Attempts were made to model the sublamine but this lead to problems with model size and processing time.

Another aspect which is fundamental to realistically predicting delamination growth is modelling of ply splitting and migration of the delamination plane. In the models reported here, there was no scope to allow the delamination to migrate between the layers. To model delamination migration is very difficult and time-consuming, requiring sophisticated through-thickness interface and three-dimensional brick elements.

6.4.2 Delamination Development and Mixed-Mode Conditions

Firstly, consider prediction of the delamination blister shape and the mixed mode conditions around the boundary. As discussed in Section 6.3.2 (Figure 6-4), these were basically controlled by bending moment and coupling effects which were in turn controlled by the stiffness of the delaminated plies. Prediction of stiffness dominated properties is generally not very mesh sensitive\textsuperscript{189}, and this may explain why there was excellent agreement between the predicted and experimental results. It was concluded that the models were relatively successful at predicting the conditions prior to damage growth.
6.4.3 **Initiation of Ply Cracking and Delamination**

Although the prediction of the blister shape, mixed mode distribution and ply splitting was good, the prediction of initiation of delamination growth was poor. The delamination growth was mode I dominated and the difference between the predicted and experimental strains was dependent on ply interface. In Model #1 (defect at the $0^\circ/90^\circ$ ply interface), the predicted strain was 36% greater than that observed experimentally. This discrepancy was attributed to incorrect modelling of the growth mechanisms; ply splitting at the growth site and migration of the delamination. The splitting would have acted as an initiation site, reducing the delamination initiation strain. For Model #2 (defect at the $+45^\circ/-45^\circ$ ply interface), the defect was deeper, so the load shedding onto the sublaminate would have been more important, promoting mode I loading and reducing the delamination initiation strain. However, the splitting and delamination initiation sites were not coincident in this instance, so the splitting would have been less important. This may explain the better correlation (23%) between the predictions and experiments.

Ply splitting in the uppermost ply of the delaminated interface was both predicted by the model and observed experimentally. As discussed in Section 6.3.3, this mechanism was attributed to curvature of the delaminated plies generating tensile strains in the ply directly above the defect plane. This mechanism was essentially controlled by the stiffness of the delaminated plies and there was little contribution from the sublaminate. The model gave a good prediction of the location and, as far as could be ascertained, the strain at which the splitting initiated. In Model #1, the splitting was very localised which implied that the mesh density was adequate.

6.4.4 **Effect of Coupon Data**

The choice of toughness data had a large effect on the predicted delamination initiation strains; this was attributed to a number of factors. Firstly, for a given criterion, the delamination strain generally increased rapidly with criterion failure index. Therefore, if the material was slightly tougher, this could result in a large change in the failure index at a given applied $G$. In addition, the $0^\circ/90^\circ$ ply interface laminates exhibited different
failure mechanisms from the unidirectional laminates, which may have had an effect. Despite the variations between different toughness data sets, all predicted that failure initiated at the same sites; this correlation is attributed to the dominance of the mode I component at the initiation site (Figures 5-6 and 5-9).

One noticeable factor was that there was less spread between criteria using the unidirectional toughness data than using the $0^\circ/90^\circ$ ply interface data. This could be attributed to the fewer mixed-mode ratios tested on the latter, which would consequently have introduced more errors. This problem may be resolved by characterising the failure locus at more points for this ply interface. In addition, for a given criterion, there was little effect of moisture or crack length on the predicted delamination initiation strains. This is attributed to the limited effect of these factors on the toughness of this particular material, as identified in Chapter 3.

Finally, an important finding was that the $0^\circ/90^\circ$ ply interface data which predicted the lowest initiation strains. This completely conflicts with a popular philosophy for fracture toughness data; $0^\circ/0^\circ$ ply interface results are considered to be the most conservative (Section 1.4.5.1). The fact that $0^\circ/90^\circ$ ply interfaces exhibit different mechanisms to conventional specimens and have lower results than $0^\circ/0^\circ$ interfaces indicates that it is important to characterise these interfaces.

6.4.5 Effect of Failure Criterion

One aspect on which there is currently much debate is which failure criterion best models the delamination fracture toughness. The results of the present work indicated that, for a given loading condition, the scatter between criteria for the prediction of delamination initiation strains can be significant; the spread in the predicted strains can be as much as 30%. This is significantly greater than typical experimental error (10%). For the $0^\circ/0^\circ$ ply interface data sets, the Ramkumar criterion consistently gave the most conservative results, with a commonly used criterion, the Linear criterion, also very conservative. However, for the $0^\circ/90^\circ$ ply interface data set, which was more realistic, these criteria were not the most conservative. It should be remembered that these results
Discussion

were for a particular case, in which mode I loading dominated. It is suggested that the choice of criterion should be governed by their physical basis, which can justify their use.

Most of the criteria studied were unable to characterise the fracture when the mode I component was negative; there was fractographic evidence (Chapter 4) that this condition does occur. It could be suggested that if the mode I component is negative, it should be set to zero; effectively producing pure mode II. However, closing the faces of the crack front would lead to very different conditions from those under pure mode II loading since effects such as friction, and consequently the formation of debris, would significantly contribute to the toughness. Of the eleven criteria used, only four could deal with a negative mode I; the Linear, General Interaction, Ramkumar and Benzeggagh criteria. The other criteria used an index in the expression which was indeterminate when the mode I component was negative. This work shows that realistic criteria should be able to include a negative mode I component and, as a consequence, mixed-mode tests should be developed which can characterise this failure mode.

The Power Law criterion, which is empirical and has little physical basis, gave poor results for all the models, although it was not clear why. Results from the coupon tests had shown that this criterion was a poor model of the failure locus of T800/924. Of the eleven criteria considered, the Linear and General Interaction gave the most consistent results, the latter proving to be preferable due to its physical justification (Chapter 3) and it was one of the most conservative when used with a realistic coupon data set (0°/90° ply interface).
7. Conclusions

Coupon and embedded delamination tests were conducted to investigate delamination initiation and growth. The coupon tests (MMB) were used to generate mixed-mode failure loci and controlled fracture surfaces at unidirectional and multidirectional ply interfaces. For the embedded delamination tests, the geometry and location of the defects were chosen from characterising realistic impact damage in stiffened structures. Finally, finite element models were developed to compare with the experimental studies. From this work the following conclusions were made.

1. The geometry and extent of the impact damage in composite structures is dependent upon the local structural geometry and overall structural response, and cannot be directly extrapolated to the results of impacts on plain laminates.

2. The damage area from 15J impacts on stiffened panels ranged from 110mm² (over the stringer) to 1750mm² (in the bay, close to the stringer). Most of the damage was circular but near structural details the damage shape was elliptical. Delamination favoured orthogonal ply interfaces; +45°/-45° and 0°/90°.

3. Of the ten mixed-mode test methods reviewed, the mixed-mode bending test (MMB) was the most reliable and consistent for generating controlled fracture surfaces and failure loci.

4. The effect of moisture, crack length and ply orientation on the delamination toughness of two materials was characterised using the MMB test. In one material (T800/5245) the toughness increased significantly with crack length and moisture, which was attributed to fibre bridging effects.

5. By fitting the toughness data to a range of failure criteria from the literature, the best model for toughness was identified. These criteria were then used to predict delamination growth in a finite element model. In general, physically based criteria gave the most consistent and reliable results.

6. Examination of the fracture morphology could be used to deduce the mixed-mode loading conditions. In particular, the tilt of shear cusps was directly related to the proportion of mode I loading. These findings were used to characterise the
Conclusions

mixed-mode loading conditions and detailed mechanisms around embedded defects.

7. From inspection of the fracture surfaces, the shape and form of the failure loci were explained in terms of micromechanisms such as fibre bridging and matrix deformation.

8. The delamination toughness of $0^\circ/90^\circ$ ply interfaces was successfully characterised and the additional mechanisms observed directly related to those which occurred at embedded defects.

9. For delamination growth from embedded inserts, the defect geometry and extent only had a limited effect on the damage mechanisms. The defect location (depth) and stacking sequence of the delaminated plies dominated the delamination buckling, initiation and subsequent growth.

10. The delamination blister shape was controlled by stacking sequence and bending moment effects. The blister shape consequently controlled the direction of the main driving force (mode I) for delamination initiation and growth.

11. Delamination from single plane defects did not remain in-plane but migrated, via ply splits, through the plies towards the surface. Upon reaching an interface in which the uppermost ply and main driving force (mode I) directions were approximately coincident, the delamination propagated within this interface.

12. Finite element modelling of embedded defects successfully predicted the blister shape and sites of delamination initiation and ply splitting. However, prediction of the delamination initiation strain using the failure criteria developed from the coupon tests was not successful.

13. From the findings of this work, qualitative prediction of delamination growth from arbitrary defects at any ply interface can be conducted. This leads to basic stacking sequence rules for designing damage tolerant structures. For example, plies transverse to the main loading directions should not be placed close to the surfaces, but beneath the expected defect plane.
8. **Implications and Recommendations**

A number of implications and recommendations have been developed from the investigation of delamination in coupons and from embedded defects.

Firstly it is recommended that, when considering impact damage in structures, the local structural geometry (*e.g.* stringer details) and overall structural response are considered. These parameters control the extent and type of damage formed during impact. Consequently, it is problematic to predict impact damage extent from tests conducted on plain coupons. When investigating impact damage in real structures, tests on structural elements are essential and may be more informative than tests on plain laminates.

Impact damage tends to occur at orthogonal ply interfaces; $0^\circ/90^\circ$ and $+45^\circ/-45^\circ$. It is recommended that these ply interfaces should be avoided when designing damage tolerant stacking sequences. In addition, delaminations due to impact preferentially extend parallel to the lowermost ply of an interface. This should be considered when ensuring impact damage does not extend to critical features.

For a given impact energy, the largest and most extensive damage was in the panel bays. For impact directly over the stiffener most of the energy was absorbed though elastic structural response and the damage extent was significantly reduced. The implication of this is that impacts near, but not over, structural features may be the most critical. It is recommended that this is considered when designing damage resistant structures.

Of all the mixed-mode test methods reviewed and investigated, the mixed-mode bending test (MMB) was the most consistent and reliable. It is recommended that this test should be developed as a standard for characterising mixed-mode delamination toughness, particularly for non-zero ply interfaces. This test could be developed further, by using shortened fulcrum lengths, to characterise negative mode I components. Such loading occurs in practice and is thought to have an effect on toughness.
Implications and Recommendations

For the mixed-mode tests conducted on unidirectional laminates, the effect of moisture and crack length on the toughness was very material dependent. However, the effect of ply interface may be general to most epoxy based fibre-reinforced composites. Non-zero ply interfaces exhibit additional mechanisms which have been observed to occur in real structures. The implication of this is that characterising $0^\circ/0^\circ$ ply interfaces alone for design purposes is invalid, and it is recommended that other more realistic ply interfaces are considered. In fact, although $0^\circ/0^\circ$ ply interface based toughness tests are useful for ranking materials, their use in generating toughness values for design is highly questionable since they may not always be conservative.

The dominating mechanism for delamination growth is that the crack is forced to grow parallel to the uppermost ply. Consequently, unlike in isotropic materials, the local ply orientations dictate the crack growth, forcing the damage to propagate along certain directions. The implication of this is that, to fully characterise delamination growth, only $0^\circ/-90^\circ$ ply interfaces need to be characterised. In the commonly used $\{0^\circ/\pm45^\circ/90^\circ\}$ family of lay-ups, only the delamination resistance of $0^\circ/90^\circ$ and $0^\circ/+45^\circ$ ply interfaces need to be characterised. Investigating delamination growth at coupons with ply interfaces such as $+45^\circ/-45^\circ$ are unrealistic and have consequently proved to be very difficult to characterise. It is recommended that this effect is studied in other materials than those investigated in this work, to identify if these delaminating mechanisms are general.

This work has demonstrated that the fracture morphology can be directly related to the failure loci. It is recommended that when developing failure criteria, the fracture surfaces are examined since this gives a good physical basis on which to develop criteria. Most of the current criteria are empirical (curve fits) and have little physical basis. However, for a given coupon data set, the predictions for the delamination initiation strain varied significantly with criterion; there was no particular criterion which always gave the most conservative results. Therefore, it is difficult to recommend a particular criterion for design of composite structures, especially since these criteria were only demonstrated for one particular case. It is thought that using empirical criteria
for design is questionable and possibly unsafe. The use of physically based criteria, which attempt to model the conditions at fracture, are more justified. It is recommended that future developments in delamination criteria are based on these factors and should also model negative mode I (closing) effects.

This work demonstrated that the fracture morphology could be used to glean the mixed-mode loading and environmental conditions during failure. This has important implications for post-mortem analysis of failed structures and can be used to deduce information about the failure.

When designing defect tolerant laminates, it is more important to consider the expected defect depth than the geometry and extent. The depth and stacking sequence of the plies above the defect dominate the delamination buckling, initiation and growth. Eliminating the formation of the delamination blister, by using surface layers resistant to bending, can significantly increase the delamination initiation strain. However, to reduce blister growth, it is recommended that the designer ensures that the maxima of the main driving force (usually mode I) and the ply directions do not become coincident as the delamination migrates through the layers. This can be done simply by tailoring the stacking sequence; for example, under uniaxial loading, the number 90° plies should be minimised and those which are required should be placed deep within the laminate. By ensuring these conditions prevail, the delamination will continue to migrate towards the surface rather than extending in-plane, and will eventually be arrested by the formation of surface splits. The delamination growth will then be stable up to strains well in excess of current design levels.

Modelling of delamination growth in composite structures has proved to be a very demanding on computer resources and requires large amounts of processing power which were attributed to the modelling of non-linear and contact effects. Modelling approaches need to be developed which can reduce these requirements.
Implications and Recommendations

All current models used to predict delamination growth assume the fracture toughness, at a given loading condition, to be constant; identical initial defects under identical loading behave in an identical manner. However, due to the inhomogeneous nature of composites and their sensitivity to local inhomogeneities, coupon tests typically exhibit 10% error. To develop fully realistic and reliable models of delamination in composite structures, a stochastic approach to the toughness is recommended. This is particularly pertinent for modelling components under cyclic loading or when predicting the stability of the delamination growth.

The results of the finite element modelling showed that the simplification of using a rigid surface to model the sublaminate had limitations and modified the mixed-mode conditions at the crack front. For modelling thicker laminates or defects beneath structural features it is recommended that 3D brick rather than 2D shell elements are used. In the current models, the element dimensions were close to the workable limit for shell elements and this led to some errors attributed to inaccurate modelling of through-thickness properties.

Finally, to fully model delamination behaviour in composite structures, it is recommended that migration of the delamination plane is realistically modelled. Current finite element codes do not have such a facility; the mechanism by which the delaminations change plane, ply splitting, is usually modelled by degrading the properties of the elements. This would be inadequate for modelling changes in plane and splitting should be modelled as a physical disconnection of adjacent elements. Without such a facility, realistic modelling of delamination growth will not be achieved.
9. Acknowledgements

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References


References


References


References


References


References


References


References


<table>
<thead>
<tr>
<th>TEST METHOD</th>
<th>Mixed-Mode RANGE</th>
<th>EXPERIMENTAL</th>
<th>ANALYTICAL</th>
<th>COMMENTS</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>% Mode I</td>
<td>Ratio Dependance:</td>
<td>Stable Growth</td>
<td>Closed Form Solution</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Lay-up</td>
<td>Geometry</td>
<td>Crack Length</td>
</tr>
<tr>
<td>MMF</td>
<td>5 - 98%</td>
<td>-</td>
<td>✓</td>
<td>-</td>
</tr>
<tr>
<td>VMM</td>
<td>0 - 80%</td>
<td>-</td>
<td>-</td>
<td>✓</td>
</tr>
<tr>
<td>MMB</td>
<td>0 - 85%</td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>SCL</td>
<td>0 - 100%</td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>NSCL</td>
<td>46 - 68%</td>
<td>-</td>
<td>✓</td>
<td>-</td>
</tr>
<tr>
<td>CLS</td>
<td>14 - 37%</td>
<td>✓</td>
<td>✓</td>
<td>-</td>
</tr>
<tr>
<td>DCLS</td>
<td>15 - 40%</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
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<tr>
<td>EDT</td>
<td>22 -90%</td>
<td>✓</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>ARCAN</td>
<td>0 - 75%</td>
<td>✓</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>OAT</td>
<td>5 - 100%</td>
<td>✓</td>
<td>-</td>
<td>-</td>
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</table>
Table 1-2  Mixed-mode failure criteria

<table>
<thead>
<tr>
<th>Criterion</th>
<th>No.</th>
<th>Failure Criterion as f(G₁,G₁₁)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Mode I</td>
<td>1-13</td>
<td>(G_1 \geq G_{1C})</td>
</tr>
<tr>
<td>Mode II</td>
<td>1-14</td>
<td>(G_{11} \geq G_{11C})</td>
</tr>
<tr>
<td>(G_C)</td>
<td>1-15</td>
<td>(G_1 + G_{11} \geq G_C)</td>
</tr>
<tr>
<td>Linear</td>
<td>1-16</td>
<td>(\frac{G_1}{G_{1C}} + \frac{G_{11}}{G_{11C}} \geq 1)</td>
</tr>
<tr>
<td>Power Law</td>
<td>1-17</td>
<td>(\left[ \frac{G_1}{G_{1C}} \right]^m + \left[ \frac{G_{11}}{G_{11C}} \right]^n \geq 1)</td>
</tr>
<tr>
<td>Yan</td>
<td>1-18</td>
<td>(G_1 + G_{11} \geq G_{1C} + \rho \left( \frac{G_{11}}{G_1} \right) + \tau \left( \frac{G_{11}}{G_1} \right)^2)</td>
</tr>
<tr>
<td>(K)</td>
<td>1-19</td>
<td>(G_1 + G_{11} \geq G_{11C} - (G_{11C} - G_{1C}) \sqrt{\frac{G_1}{G_{1C}}})</td>
</tr>
<tr>
<td>Exponential (K)</td>
<td>1-20</td>
<td>(G_1 + G_{11} \geq (G_{11C} - G_{1C}) e^{\eta \sqrt{G_{11}}} + G_{1C})</td>
</tr>
<tr>
<td>General Interaction</td>
<td>1-22</td>
<td>(\left[ \frac{G_1}{G_{1C}} - 1 \right] \left[ \frac{G_{11}}{G_{11C}} - 1 \right] + \beta + \Phi \left( \frac{G_1}{G_1 + G_{11}} \right) \frac{G_1 G_{11}}{G_{1C} G_{11C}} \geq 0)</td>
</tr>
<tr>
<td>Ramkumar</td>
<td>1-23</td>
<td>(\left[ \frac{G_1}{G_{1C}} \right]^2 + \left[ \frac{G_{11}}{G_{11C}} \right]^2 + \frac{G_1 G_{11}}{G_{1C} G_{11C}} \geq 1)</td>
</tr>
<tr>
<td>Hackle</td>
<td>1-24</td>
<td>(G_1 + G_{11} \geq G_{1C} - \Omega + \Omega \sqrt{1 + \frac{G_{11}}{G_1} \left( \frac{E_{11}}{E_{22}} \right)})</td>
</tr>
<tr>
<td>Exponential Hackle</td>
<td>1-25</td>
<td>(G_1 + G_{11} \geq (G_{1C} - G_{11C}) e^{\gamma(1-N)} + G_{11C})</td>
</tr>
<tr>
<td>Bilinear</td>
<td>1-26</td>
<td>(G_1 = \xi G_{11} + G_{1C})</td>
</tr>
<tr>
<td></td>
<td>1-27</td>
<td>(G_1 = \xi G_{11} - \zeta G_{11C})</td>
</tr>
<tr>
<td>Benzeggegh</td>
<td>1-28</td>
<td>(G_1 + G_{11} \geq G_{1C} + (G_{11C} - G_{1C}) \left( \frac{G_{11}}{G_1 + G_{11}} \right)^m)</td>
</tr>
<tr>
<td>Kinloch</td>
<td>1-29</td>
<td>(G_0 \leq \left[ G_1 + G_{11} \right] [\cos^2(\phi - \phi_0) + \sin^2 \omega \sin^2(\phi - \phi_0)])</td>
</tr>
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</table>
### Table 2-1 Maximum forces, deflections and damage areas for impacts in the plain and stiffened panels

<table>
<thead>
<tr>
<th>Site</th>
<th>Description</th>
<th>Energy (J)</th>
<th>Distance from Stiffener (mm)</th>
<th>Peak Force (N)</th>
<th>Peak Deflection (mm)</th>
<th>Damage Area (mm²)</th>
</tr>
</thead>
<tbody>
<tr>
<td>-</td>
<td>Plain laminate</td>
<td>15</td>
<td>50.0</td>
<td>7280</td>
<td>3.9</td>
<td>1552</td>
</tr>
<tr>
<td>A</td>
<td>Over the bay</td>
<td>15</td>
<td>60.0</td>
<td>5800</td>
<td>4.5</td>
<td>1620</td>
</tr>
<tr>
<td>B</td>
<td>In the bay near the stiffener</td>
<td>15</td>
<td>38.0</td>
<td>5400</td>
<td>4.6</td>
<td>1750</td>
</tr>
<tr>
<td>C</td>
<td>Over the stiffener foot</td>
<td>15</td>
<td>12.5</td>
<td>7200</td>
<td>3.0</td>
<td>900</td>
</tr>
<tr>
<td>D</td>
<td>Over the stiffener centreline</td>
<td>15</td>
<td>0.0</td>
<td>12580</td>
<td>2.0</td>
<td>110</td>
</tr>
</tbody>
</table>

### Table 3-1 Stacking sequences of the preliminary MMB specimens

<table>
<thead>
<tr>
<th>Interface</th>
<th>Specimen</th>
<th>Stacking Sequence</th>
</tr>
</thead>
<tbody>
<tr>
<td>0°/0°</td>
<td>Conventional</td>
<td>[0°]₁₀ [0°]₁₀</td>
</tr>
<tr>
<td>0°/0°</td>
<td>Modified</td>
<td>[0°]₁₀ [0°]₁₀</td>
</tr>
<tr>
<td>0°/90°</td>
<td>Modified</td>
<td>[90°/0°]₅ [0°/90°]₅</td>
</tr>
<tr>
<td>0°/90°</td>
<td>Modified</td>
<td>[(0°/90°/+45°/0°)₅ (0°/90°/-45°/0°)₅ (0°/90°/+45°/90°)₅ (0°/90°/-45°/90°)₅]</td>
</tr>
<tr>
<td>+45°/-45°</td>
<td>Modified</td>
<td>[(-45°/0°/+45°)₅ (+45°/0°/-45°)₅ (-45°/0°/+45°)₅ (+45°/0°/-45°)₅]</td>
</tr>
<tr>
<td>0°/+45°</td>
<td>Modified</td>
<td>[(-45°/0°/+45°)₅ (+45°/0°/-45°)₅ (0°/+45°/-45°)₅ (0°/-45°/+45°)₅]</td>
</tr>
</tbody>
</table>

*Revised stacking sequence (Section 3.2)
Table 3-2  Test results from the preliminary MMB specimens

<table>
<thead>
<tr>
<th>Ply Interface</th>
<th>100% Mode I (DCB) a=40mm</th>
<th>100% Mode I (DCB) a=50mm</th>
<th>68% Mode I (MMB) a=40mm</th>
<th>68% Mode I (MMB) a=50mm</th>
<th>0% Mode I</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$G_T$ (Jm$^{-2}$)</td>
<td>$C_v$ (%)</td>
<td>$G_T$ (Jm$^{-2}$)</td>
<td>$C_v$ (%)</td>
<td>$G_T$ (Jm$^{-2}$)</td>
</tr>
<tr>
<td>0°/0°</td>
<td>289.8</td>
<td>14.4%</td>
<td>276.8</td>
<td>15.6%</td>
<td>224.2</td>
</tr>
<tr>
<td>0°/0°†</td>
<td>222.3</td>
<td>8.7%</td>
<td>219.6</td>
<td>7.6%</td>
<td>429.0</td>
</tr>
<tr>
<td>0°/90°†</td>
<td>646.5</td>
<td>14.9%</td>
<td>819.4</td>
<td>21.8%</td>
<td>415.1</td>
</tr>
<tr>
<td>+45°/-45°†</td>
<td>777.3</td>
<td>35.7%</td>
<td>726.6</td>
<td>36.5%</td>
<td>786.4</td>
</tr>
<tr>
<td>0°/+45°†</td>
<td>454.6</td>
<td>20.6%</td>
<td>446.9</td>
<td>5.7%</td>
<td>864.2</td>
</tr>
</tbody>
</table>

* Conventional specimen, † Modified specimen

Table 3-3  Predicted engineering properties of the MMB specimens

<table>
<thead>
<tr>
<th>Ply Interface</th>
<th>$E_{xx}$ (GPa)</th>
<th>$D_x$ (GPa)</th>
<th>$D_y$ (GPa)</th>
<th>$D_C^{35,106}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>0°/0°†</td>
<td>168.0</td>
<td>126.0</td>
<td>7.1</td>
<td>0.0121</td>
</tr>
<tr>
<td>0°/90°†</td>
<td>142.1</td>
<td>101.0</td>
<td>32.6</td>
<td>0.00331</td>
</tr>
<tr>
<td>+45°/-45°†</td>
<td>67.5</td>
<td>50.1</td>
<td>20.8</td>
<td>0.00499</td>
</tr>
<tr>
<td>0°/+45°†</td>
<td>67.5</td>
<td>51.7</td>
<td>20.5</td>
<td>0.474</td>
</tr>
<tr>
<td>0°/90° †</td>
<td>62.7</td>
<td>84.0</td>
<td>84.0</td>
<td>0.140</td>
</tr>
</tbody>
</table>

† Averaged between two sublaminates, * Revised lay-up
Table 3-4  Effect of rig geometry on toughness tests conducted at 50% mode I (0°/0° ply interface in T800/5245)

<table>
<thead>
<tr>
<th>Fulcrum b (mm)</th>
<th>Crack Length = 35mm</th>
<th></th>
<th>Crack Length = 45mm</th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Average $G_T$ (J/m$^2$)</td>
<td>Cv (%)</td>
<td>Average $G_T$ (J/m$^2$)</td>
<td>Cv (%)</td>
</tr>
<tr>
<td>55mm</td>
<td>201.8</td>
<td>18.6%</td>
<td>232.2</td>
<td>18.8%</td>
</tr>
<tr>
<td>80mm</td>
<td>205.0</td>
<td>17.3%</td>
<td>255.9</td>
<td>14.3%</td>
</tr>
<tr>
<td>% Difference</td>
<td>-1.6%</td>
<td></td>
<td>-10.2%</td>
<td></td>
</tr>
</tbody>
</table>

Table 3-5  Validation test results from the 0°/90° ply interface MMB specimens ($a=50$mm)

<table>
<thead>
<tr>
<th>Specimen</th>
<th>% Mode I</th>
<th>Orientation</th>
<th>$G_T$ (J/m$^2$)</th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>N1</td>
<td>100% I</td>
<td>-</td>
<td>573.7</td>
<td></td>
</tr>
<tr>
<td>N2</td>
<td>100% I</td>
<td>-</td>
<td>786.9</td>
<td></td>
</tr>
<tr>
<td>N3</td>
<td>100% I</td>
<td>-</td>
<td>699.6</td>
<td></td>
</tr>
<tr>
<td>N4</td>
<td>100% I</td>
<td>-</td>
<td>689.1</td>
<td></td>
</tr>
<tr>
<td></td>
<td>100% I</td>
<td>Average (C_r)</td>
<td>725.2 (7.4%)</td>
<td></td>
</tr>
<tr>
<td>N5</td>
<td>75% I</td>
<td>90° uppermost</td>
<td>747.7</td>
<td></td>
</tr>
<tr>
<td>N7</td>
<td>75% I</td>
<td>0° uppermost</td>
<td>253.3</td>
<td></td>
</tr>
<tr>
<td>N8</td>
<td>75% I</td>
<td>0° uppermost</td>
<td>233.4</td>
<td></td>
</tr>
<tr>
<td>N9</td>
<td>75% I</td>
<td>0° uppermost</td>
<td>214.4</td>
<td></td>
</tr>
<tr>
<td></td>
<td>75% I</td>
<td>Average (C_r)</td>
<td>233.7 (5.2%)</td>
<td></td>
</tr>
<tr>
<td>N10</td>
<td>0% I</td>
<td>0° uppermost</td>
<td>418.5</td>
<td></td>
</tr>
<tr>
<td>N11</td>
<td>0% I</td>
<td>0° uppermost</td>
<td>364.7</td>
<td></td>
</tr>
<tr>
<td></td>
<td>0% I</td>
<td>Average (C_r)</td>
<td>391.6 (9.7%)</td>
<td></td>
</tr>
<tr>
<td>N12</td>
<td>0% I</td>
<td>0° uppermost  (precrack with 90° uppermost)</td>
<td>880.6</td>
<td></td>
</tr>
</tbody>
</table>
Table 3-6  MMB rig geometries for the mixed-mode tests

<table>
<thead>
<tr>
<th>% Mode I</th>
<th>Lower Span 2L (mm)</th>
<th>Fulcrum b (mm)</th>
<th>Lever Length c (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0% Mode I</td>
<td>110.0</td>
<td>65.0</td>
<td>16.7</td>
</tr>
<tr>
<td>12.5% Mode I</td>
<td>110.0</td>
<td>65.0</td>
<td>24.2</td>
</tr>
<tr>
<td>25% Mode I</td>
<td>110.0</td>
<td>65.0</td>
<td>28.8</td>
</tr>
<tr>
<td>37.5% Mode I</td>
<td>110.0</td>
<td>65.0</td>
<td>33.8</td>
</tr>
<tr>
<td>50% Mode I</td>
<td>110.0</td>
<td>80.0</td>
<td>27.3</td>
</tr>
<tr>
<td>62.5% Mode I</td>
<td>110.0</td>
<td>80.0</td>
<td>32.5</td>
</tr>
<tr>
<td>75% Mode I</td>
<td>110.0</td>
<td>80.0</td>
<td>41.4</td>
</tr>
<tr>
<td>75% Mode I†</td>
<td>110.0</td>
<td>55.0</td>
<td>91.7</td>
</tr>
<tr>
<td>87.5% Mode I</td>
<td>110.0</td>
<td>80.0</td>
<td>65.1</td>
</tr>
<tr>
<td>100% Mode I</td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
</tbody>
</table>

† Rig geometry for precracking

Table 3-7  Results of the material property tests

<table>
<thead>
<tr>
<th>Property</th>
<th>T800/5245</th>
<th></th>
<th>T800/924</th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Average</td>
<td>Cv (%)</td>
<td>Average</td>
<td>Cv (%)</td>
</tr>
<tr>
<td>$E_{11}$ (GPa)</td>
<td>157.6</td>
<td>2.5%</td>
<td>155.2</td>
<td>6.8%</td>
</tr>
<tr>
<td>$E_{22}$ (GPa)</td>
<td>8.9</td>
<td>3.9%</td>
<td>8.6</td>
<td>2.7%</td>
</tr>
<tr>
<td>$G_{12}$ (GPa)</td>
<td>12.1</td>
<td>6.3%</td>
<td>7.4</td>
<td>5.1%</td>
</tr>
<tr>
<td>$\sigma_{11}$ (MPa)</td>
<td>1856.0</td>
<td>4.0%</td>
<td>1982.9</td>
<td>1.9%</td>
</tr>
<tr>
<td>$\sigma_{22}$ (MPa)</td>
<td>31.1</td>
<td>4.1%</td>
<td>48.7</td>
<td>12.3%</td>
</tr>
<tr>
<td>$\sigma_{12}$ (MPa)</td>
<td>91.5</td>
<td>2.9%</td>
<td>113.0</td>
<td>10.1%</td>
</tr>
<tr>
<td>$\varepsilon_{11}$ (µε)</td>
<td>11777</td>
<td>1.5%</td>
<td>12776</td>
<td>4.9%</td>
</tr>
<tr>
<td>$\varepsilon_{22}$ (µε)</td>
<td>3506</td>
<td>0.5%</td>
<td>5681</td>
<td>9.6%</td>
</tr>
<tr>
<td>$\varepsilon_{12}$ (µε)</td>
<td>7541</td>
<td>3.4%</td>
<td>15273</td>
<td>5.0%</td>
</tr>
<tr>
<td>$v_{12}$</td>
<td>0.315</td>
<td>7.0%</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>$v_{21}$</td>
<td>0.012</td>
<td>58.7%</td>
<td>-</td>
<td>-</td>
</tr>
</tbody>
</table>
Table 3-8  Results of the material flexural property tests

<table>
<thead>
<tr>
<th>Property</th>
<th>T800/5245 (DRY)</th>
<th>T800/5245 (WET)</th>
<th>T800/924 (DRY)</th>
<th>T800/924 (WET)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Average</td>
<td>Cv (%)</td>
<td>Average</td>
<td>Cv (%)</td>
</tr>
<tr>
<td>$E_F$ (GPa)</td>
<td>129.9</td>
<td>4.0%</td>
<td>131.0</td>
<td>2.4%</td>
</tr>
<tr>
<td>$\sigma_F$ (MPa)</td>
<td>1501.6</td>
<td>2.0%</td>
<td>1463.9</td>
<td>2.5%</td>
</tr>
<tr>
<td>$\varepsilon_F$ (µε)</td>
<td>11558.9</td>
<td>3.8%</td>
<td>11179.2</td>
<td>3.3%</td>
</tr>
</tbody>
</table>

Table 3-9  Comparison between $G_{IC}$ and $G_{IIIC}$ results and values given in the literature

<table>
<thead>
<tr>
<th>Material</th>
<th>$G_{IC}$ (J/m²) a=40mm</th>
<th>$G_{IC}$ (J/m²) a=60mm</th>
<th>Published $G_{IC}$ (J/m²)</th>
<th>$G_{IIIC}$ (J/m²) a=40mm</th>
<th>$G_{IIIC}$ (J/m²) a=60mm*</th>
<th>Published $G_{IC}$ (J/m²)</th>
</tr>
</thead>
<tbody>
<tr>
<td>T800/5245 DRY</td>
<td>218 (9%)</td>
<td>282 (6%)</td>
<td>140 - 230$^{183}$</td>
<td>519</td>
<td>749</td>
<td>434 - 552$^{97}$</td>
</tr>
<tr>
<td>T800/5245 WET</td>
<td>198 (7%)</td>
<td>250 (6%)</td>
<td>-</td>
<td>626</td>
<td>1040</td>
<td>-</td>
</tr>
<tr>
<td>T800/924 DRY</td>
<td>288 (11%)</td>
<td>276 (11%)</td>
<td>200 - 270$^{183}$</td>
<td>575</td>
<td>630</td>
<td>548 - 582$^{97}$</td>
</tr>
<tr>
<td>T800/924 WET</td>
<td>230 (9%)</td>
<td>222 (6%)</td>
<td>-</td>
<td>582</td>
<td>820</td>
<td>-</td>
</tr>
</tbody>
</table>

* Extrapolated
Cv given in brackets
Table 3-10  MMB test results for unidirectional T800/5245 (DRY) at crack lengths of 40mm and 60mm

<table>
<thead>
<tr>
<th>% Mode I</th>
<th>a=40mm</th>
<th>a=60mm</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$G_T$ (J/m²)</td>
<td>$C_v$ (%)</td>
</tr>
<tr>
<td>100% Mode I</td>
<td>217.7</td>
<td>9.1%</td>
</tr>
<tr>
<td>87.5% Mode I</td>
<td>240.8</td>
<td>17.7%</td>
</tr>
<tr>
<td>75% Mode I</td>
<td>189.8</td>
<td>4.4%</td>
</tr>
<tr>
<td>62.5% Mode I</td>
<td>211.7</td>
<td>14.1%</td>
</tr>
<tr>
<td>50% Mode I</td>
<td>241.6</td>
<td>8.9%</td>
</tr>
<tr>
<td>37.5% Mode I</td>
<td>315.9</td>
<td>0.9%</td>
</tr>
<tr>
<td>25% Mode I</td>
<td>294.2</td>
<td>4.7%</td>
</tr>
<tr>
<td>12.5% Mode I</td>
<td>377.9</td>
<td>17.7%</td>
</tr>
<tr>
<td>0% Mode I</td>
<td>519.2</td>
<td>-</td>
</tr>
</tbody>
</table>

Table 3-11  MMB test results for unidirectional T800/5245 (WET) at crack lengths of 40mm and 60mm

<table>
<thead>
<tr>
<th>% Mode I</th>
<th>a=40mm</th>
<th>a=60mm</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$G_T$ (J/m²)</td>
<td>$C_v$ (%)</td>
</tr>
<tr>
<td>100% Mode I</td>
<td>198.3</td>
<td>6.7%</td>
</tr>
<tr>
<td>87.5% Mode I</td>
<td>238.5</td>
<td>6.9%</td>
</tr>
<tr>
<td>75% Mode I</td>
<td>243.0</td>
<td>8.8%</td>
</tr>
<tr>
<td>62.5% Mode I</td>
<td>222.8</td>
<td>10.3%</td>
</tr>
<tr>
<td>50% Mode I</td>
<td>217.5</td>
<td>6.5%</td>
</tr>
<tr>
<td>37.5% Mode I</td>
<td>265.4</td>
<td>8.2%</td>
</tr>
<tr>
<td>25% Mode I</td>
<td>281.0</td>
<td>14.1%</td>
</tr>
<tr>
<td>12.5% Mode I</td>
<td>427.4</td>
<td>8.6%</td>
</tr>
<tr>
<td>0% Mode I</td>
<td>625.5</td>
<td>-</td>
</tr>
</tbody>
</table>
### Table 3-12  MMB test results for unidirectional T800/924 (DRY) at crack lengths of 40mm and 60mm

<table>
<thead>
<tr>
<th>% Mode I</th>
<th>a=40mm</th>
<th>a=60mm</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$G_T$ (J/m²)</td>
<td>$C_v$ (%)</td>
</tr>
<tr>
<td>100% Mode I</td>
<td>287.9</td>
<td>10.9%</td>
</tr>
<tr>
<td>87.5% Mode I</td>
<td>216.2</td>
<td>9.9%</td>
</tr>
<tr>
<td>75% Mode I</td>
<td>204.4</td>
<td>9.5%</td>
</tr>
<tr>
<td>62.5% Mode I</td>
<td>222.8</td>
<td>10.7%</td>
</tr>
<tr>
<td>50% Mode I</td>
<td>236.9</td>
<td>12.0%</td>
</tr>
<tr>
<td>37.5% Mode I</td>
<td>325.3</td>
<td>7.2%</td>
</tr>
<tr>
<td>25% Mode I</td>
<td>355.0</td>
<td>18.3%</td>
</tr>
<tr>
<td>12.5% Mode I</td>
<td>376.9</td>
<td>14.6%</td>
</tr>
<tr>
<td>0% Mode I</td>
<td>547.7</td>
<td>-</td>
</tr>
</tbody>
</table>

### Table 3-13  MMB test results for unidirectional T800/924 (WET) at crack lengths of 40mm and 60mm

<table>
<thead>
<tr>
<th>% Mode I</th>
<th>a=40mm</th>
<th>a=60mm</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$G_T$ (J/m²)</td>
<td>$C_v$ (%)</td>
</tr>
<tr>
<td>100% Mode I</td>
<td>230.2</td>
<td>8.8%</td>
</tr>
<tr>
<td>87.5% Mode I</td>
<td>225.9</td>
<td>8.3%</td>
</tr>
<tr>
<td>75% Mode I</td>
<td>219.7</td>
<td>10.8%</td>
</tr>
<tr>
<td>62.5% Mode I</td>
<td>243.9</td>
<td>7.0%</td>
</tr>
<tr>
<td>50% Mode I</td>
<td>259.2</td>
<td>9.2%</td>
</tr>
<tr>
<td>37.5% Mode I</td>
<td>328.5</td>
<td>11.8%</td>
</tr>
<tr>
<td>25% Mode I</td>
<td>330.0</td>
<td>10.7%</td>
</tr>
<tr>
<td>12.5% Mode I</td>
<td>386.9</td>
<td>8.1%</td>
</tr>
<tr>
<td>0% Mode I</td>
<td>582.4</td>
<td>-</td>
</tr>
<tr>
<td>% Mode</td>
<td>0°/90°</td>
<td>0°/90°</td>
</tr>
<tr>
<td>--------</td>
<td>--------</td>
<td>--------</td>
</tr>
<tr>
<td></td>
<td>G_r (J/m²)</td>
<td>G_r (J/m²)</td>
</tr>
<tr>
<td>87.5%</td>
<td>216.2</td>
<td>210.4</td>
</tr>
<tr>
<td>Mode I</td>
<td>9.9%</td>
<td>11.7%</td>
</tr>
<tr>
<td>75%</td>
<td>204.4</td>
<td>250.6</td>
</tr>
<tr>
<td>Mode I</td>
<td>9.5%</td>
<td>15.8%</td>
</tr>
<tr>
<td>50%</td>
<td>236.9</td>
<td>384.4</td>
</tr>
<tr>
<td>Mode I</td>
<td>12.0%</td>
<td>15.8%</td>
</tr>
<tr>
<td>25%</td>
<td>355.0</td>
<td>402.7</td>
</tr>
<tr>
<td>Mode I</td>
<td>18.3%</td>
<td>8.1%</td>
</tr>
<tr>
<td>0%</td>
<td>547.7</td>
<td>536.8</td>
</tr>
<tr>
<td>Mode I</td>
<td>13.7%</td>
<td>15.9%</td>
</tr>
</tbody>
</table>
Table 3-15  MMB test results for 0°/90° ply interface specimens (dry T800/924) averaged over all crack lengths (0°/0° ply interface results given for comparison)

<table>
<thead>
<tr>
<th>% Mode I</th>
<th>0°/0°</th>
<th>0°/90°</th>
<th>Difference (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$G_T$ (J/m²)</td>
<td>Cv (%)</td>
<td>$G_T$ (J/m²)</td>
</tr>
<tr>
<td>87.5% Mode I</td>
<td>235.4</td>
<td>9.8%</td>
<td>201.7</td>
</tr>
<tr>
<td>75% Mode I</td>
<td>228.4</td>
<td>11.4%</td>
<td>252.6</td>
</tr>
<tr>
<td>50% Mode I</td>
<td>246.3</td>
<td>10.5%</td>
<td>349.7</td>
</tr>
<tr>
<td>25% Mode I</td>
<td>316.9</td>
<td>5.3%</td>
<td>402.7</td>
</tr>
<tr>
<td>0% Mode I</td>
<td>547.7</td>
<td>13.7%</td>
<td>536.8</td>
</tr>
</tbody>
</table>

Table 3-16  Predicted MMB specimen stiffnesses for 50% mode I tests (0°/0° ply interface for T800/5245)

<table>
<thead>
<tr>
<th>Fulcrum b (mm)</th>
<th>Crack Length = 35mm</th>
<th>Crack Length = 45mm</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Average $E_{11}$ (GPa)</td>
<td>Cv (%)</td>
</tr>
<tr>
<td>55mm</td>
<td>106.7</td>
<td>15.3%</td>
</tr>
<tr>
<td>80mm</td>
<td>82.3</td>
<td>23.1%</td>
</tr>
<tr>
<td>% Difference</td>
<td>22.8%</td>
<td></td>
</tr>
</tbody>
</table>
### Table 3-17  Cusp angle $\beta$ against mixed-mode ratio for T800/5245 (0°/0° ply interface)

<table>
<thead>
<tr>
<th>% Mode I</th>
<th>Dry (β (°))</th>
<th>Cv (%)</th>
<th>Wet (β (°))</th>
<th>Cv (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>100% Mode I</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>87.5% Mode I</td>
<td>26</td>
<td>15%</td>
<td>34</td>
<td>20%</td>
</tr>
<tr>
<td>75% Mode I</td>
<td>25</td>
<td>13%</td>
<td>39</td>
<td>10%</td>
</tr>
<tr>
<td>62.5% Mode I</td>
<td>32</td>
<td>13%</td>
<td>36</td>
<td>12%</td>
</tr>
<tr>
<td>50% Mode I</td>
<td>37</td>
<td>10%</td>
<td>45</td>
<td>9%</td>
</tr>
<tr>
<td>37.5% Mode I</td>
<td>40</td>
<td>9%</td>
<td>46</td>
<td>12%</td>
</tr>
<tr>
<td>25% Mode I</td>
<td>44</td>
<td>6%</td>
<td>52</td>
<td>6%</td>
</tr>
<tr>
<td>12.5% Mode I</td>
<td>53</td>
<td>11%</td>
<td>71</td>
<td>8%</td>
</tr>
<tr>
<td>0% Mode I</td>
<td>107</td>
<td>10%</td>
<td>124</td>
<td>4%</td>
</tr>
</tbody>
</table>

### Table 3-18  Cusp angle $\beta$ against mixed-mode ratio for T800/924 (0°/0° and 0°/90° ply interfaces)

<table>
<thead>
<tr>
<th>% Mode I</th>
<th>Dry (0°/0°) (β (°))</th>
<th>Cv (%)</th>
<th>Wet (0°/0°) (β (°))</th>
<th>Cv (%)</th>
<th>Dry (0°/90°) (β (°))</th>
<th>Cv (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>100% Mode I</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>87.5% Mode I</td>
<td>19</td>
<td>26%</td>
<td>21</td>
<td>22%</td>
<td>23</td>
<td>23%</td>
</tr>
<tr>
<td>75% Mode I</td>
<td>17</td>
<td>11%</td>
<td>23</td>
<td>12%</td>
<td>29</td>
<td>16%</td>
</tr>
<tr>
<td>62.5% Mode I</td>
<td>24</td>
<td>10%</td>
<td>30</td>
<td>13%</td>
<td></td>
<td></td>
</tr>
<tr>
<td>50% Mode I</td>
<td>32</td>
<td>11%</td>
<td>34</td>
<td>9%</td>
<td>44</td>
<td>9%</td>
</tr>
<tr>
<td>37.5% Mode I</td>
<td>41</td>
<td>11%</td>
<td>40</td>
<td>12%</td>
<td></td>
<td></td>
</tr>
<tr>
<td>25% Mode I</td>
<td>46</td>
<td>8%</td>
<td>47</td>
<td>10%</td>
<td>50</td>
<td>7%</td>
</tr>
<tr>
<td>12.5% Mode I</td>
<td>55</td>
<td>8%</td>
<td>53</td>
<td>6%</td>
<td></td>
<td></td>
</tr>
<tr>
<td>0% Mode I</td>
<td>76</td>
<td>9%</td>
<td>72</td>
<td>8%</td>
<td>76</td>
<td>6%</td>
</tr>
</tbody>
</table>
Table 3-19  Mixed-mode failure criteria given as a function of $G_T$ and $t$, where $t$ is the mixed-mode ratio $G_1/G_{11}$

<table>
<thead>
<tr>
<th>Criterion</th>
<th>Equation</th>
<th>Failure Criterion as $G_T=f(t)$ where $t=G_1/G_{11}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>Linear</td>
<td>1-16</td>
<td>$G_T = \frac{G_{IC}G_{II}(t+1)}{tG_{II} + G_{IC}}$</td>
</tr>
<tr>
<td>Power Law</td>
<td>1-17</td>
<td><em>No explicit expression</em></td>
</tr>
<tr>
<td>Yan</td>
<td>1-18</td>
<td>$G_T = G_{IC} \frac{\rho}{t} + \frac{\tau}{t^2}$</td>
</tr>
<tr>
<td>K</td>
<td>1-19</td>
<td><em>No explicit expression</em></td>
</tr>
<tr>
<td>Exponential K</td>
<td>1-20</td>
<td>$G_T = (G_{II} - G_{IC})e^{\eta \sqrt{t}}$</td>
</tr>
<tr>
<td>General Interaction</td>
<td>1-22</td>
<td><em>No explicit expression</em></td>
</tr>
<tr>
<td>Ramkumar Interaction</td>
<td>1-23</td>
<td>$G_T = (t+1)\left[\frac{t^2}{G_{IC}^2} + \frac{1}{G_{II}^2} + \frac{t}{G_{IC}G_{II}}\right]^{-\frac{1}{2}}$</td>
</tr>
<tr>
<td>Hackle</td>
<td>1-24</td>
<td>$G_T = G_{IC} - \Omega + \Omega \left[1 + \frac{1}{t} \sqrt{\frac{E_{11}}{E_{22}}}\right]$</td>
</tr>
<tr>
<td>Exponential Hackle</td>
<td>1-25</td>
<td>$G_T = (G_{IC} - G_{II})e^{\eta(1-N)} + G_{II}$</td>
</tr>
<tr>
<td>Benzeggegh</td>
<td>1-28</td>
<td>$G_T = G_{IC} + \frac{(G_{II} - G_{IC})}{(t+1)^m}$</td>
</tr>
<tr>
<td>Kinloch</td>
<td>1-29</td>
<td>$G_T = \frac{G_0}{\left[\cos^2(\phi - \phi_0) + \sin^2(\omega) \sin^2(\phi - \phi_0)\right]}$</td>
</tr>
</tbody>
</table>
### Table 3-20  Ranking of the failure criteria by $\chi^2$ and $G_{IC}$ for the results from the MMB tests on T800/5245 (0°/0° ply interface)

<table>
<thead>
<tr>
<th>Ranking by $\chi^2$</th>
<th>Dry</th>
<th>Wet</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>a=40mm</td>
<td>a=60mm</td>
</tr>
<tr>
<td>1</td>
<td>Power</td>
<td>Exp K</td>
</tr>
<tr>
<td>2</td>
<td>Gen Int</td>
<td>Exp Hackle</td>
</tr>
<tr>
<td>3</td>
<td>Exp Hackle</td>
<td>Benzeggegh</td>
</tr>
<tr>
<td>4</td>
<td>Benzeggegh</td>
<td>Power</td>
</tr>
<tr>
<td>5</td>
<td>Exp K</td>
<td>Gen Int</td>
</tr>
<tr>
<td>6</td>
<td>Ramkumar</td>
<td>Kinoch</td>
</tr>
<tr>
<td>7</td>
<td>Kinoch</td>
<td>Hackle</td>
</tr>
<tr>
<td>8</td>
<td>Yan</td>
<td>Linear</td>
</tr>
<tr>
<td>9</td>
<td>Linear</td>
<td>K</td>
</tr>
<tr>
<td>10</td>
<td>K</td>
<td>Yan</td>
</tr>
<tr>
<td>11</td>
<td>Hackle</td>
<td>Ramkumar</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Ranking by $G_{IC}$</th>
<th>Dry</th>
<th>Wet</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>a=40mm</td>
<td>a=60mm</td>
</tr>
<tr>
<td>1</td>
<td>Power</td>
<td>Ramkumar</td>
</tr>
<tr>
<td>2</td>
<td>K</td>
<td>Kinoch</td>
</tr>
<tr>
<td>3</td>
<td>Linear</td>
<td>Power</td>
</tr>
<tr>
<td>4</td>
<td>Yan</td>
<td>Gen Int</td>
</tr>
<tr>
<td>5</td>
<td>Ramkumar</td>
<td>Linear</td>
</tr>
<tr>
<td>6</td>
<td>Hackle</td>
<td>Hackle</td>
</tr>
<tr>
<td>7</td>
<td>Exp Hackle</td>
<td>K</td>
</tr>
<tr>
<td>8</td>
<td>Exp K</td>
<td>Yan</td>
</tr>
<tr>
<td>9</td>
<td>Benzeggegh</td>
<td>Exp K</td>
</tr>
<tr>
<td>10</td>
<td>Gen Int</td>
<td>Exp Hackle</td>
</tr>
<tr>
<td>11</td>
<td>Kinoch</td>
<td>Benzeggegh</td>
</tr>
</tbody>
</table>
### Table 3-21  Ranking of the failure criteria by $\chi^2$ and $G_{IC}$ for the results from the MMB tests on T800/924 (0°/0° ply interface)

<table>
<thead>
<tr>
<th>Ranking by $\chi^2$</th>
<th>Dry</th>
<th>Wet</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$a=40\text{mm}$</td>
<td>$a=60\text{mm}$</td>
</tr>
<tr>
<td>1</td>
<td>Gen Int</td>
<td>Gen Int</td>
</tr>
<tr>
<td>2</td>
<td>Power</td>
<td>Power</td>
</tr>
<tr>
<td>3</td>
<td>Kinoch</td>
<td>Kinoch</td>
</tr>
<tr>
<td>4</td>
<td>Benzeggegh</td>
<td>Benzeggegh</td>
</tr>
<tr>
<td>5</td>
<td>Exp K</td>
<td>Exp K</td>
</tr>
<tr>
<td>6</td>
<td>Yan</td>
<td>Yan</td>
</tr>
<tr>
<td>7</td>
<td>Exp Hackle</td>
<td>Exp Hackle</td>
</tr>
<tr>
<td>8</td>
<td>Hackle</td>
<td>Hackle</td>
</tr>
<tr>
<td>9</td>
<td>K</td>
<td>K</td>
</tr>
<tr>
<td>10</td>
<td>Linear</td>
<td>Linear</td>
</tr>
<tr>
<td>11</td>
<td>Ramkumar</td>
<td>Ramkumar</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Ranking by $G_{IC}$</th>
<th>Dry</th>
<th>Wet</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$a=40\text{mm}$</td>
<td>$a=60\text{mm}$</td>
</tr>
<tr>
<td>1</td>
<td>Gen Int</td>
<td>Gen Int</td>
</tr>
<tr>
<td>2</td>
<td>Kinoch</td>
<td>Kinoch</td>
</tr>
<tr>
<td>3</td>
<td>Benzeggegh</td>
<td>Power</td>
</tr>
<tr>
<td>4</td>
<td>Exp K</td>
<td>Benzeggegh</td>
</tr>
<tr>
<td>5</td>
<td>Exp Hackle</td>
<td>Exp K</td>
</tr>
<tr>
<td>6</td>
<td>Hackle</td>
<td>Exp Hackle</td>
</tr>
<tr>
<td>7</td>
<td>Yan</td>
<td>Hackle</td>
</tr>
<tr>
<td>8</td>
<td>K</td>
<td>Yan</td>
</tr>
<tr>
<td>9</td>
<td>Linear</td>
<td>K</td>
</tr>
<tr>
<td>10</td>
<td>Ramkumar</td>
<td>Linear</td>
</tr>
<tr>
<td>11</td>
<td>Power</td>
<td>Ramkumar</td>
</tr>
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</table>
Table 3-22  Ranking of the failure criteria by $\chi^2$ and $G_{IC}$ for all the results from the MMB tests on $0^\circ/0^\circ$ ply interface specimens

<table>
<thead>
<tr>
<th>Ranking by $\chi^2$</th>
<th>T800/5245</th>
<th>$0^\circ/0^\circ$</th>
<th>T800/924</th>
<th>Overall</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Exp K</td>
<td>Gen Int</td>
<td>Gen Int</td>
<td></td>
</tr>
<tr>
<td>2</td>
<td>Exp Hackle</td>
<td>Power</td>
<td>Power</td>
<td></td>
</tr>
<tr>
<td>3</td>
<td>Gen Int</td>
<td>Benzeggegh</td>
<td>Exp K</td>
<td></td>
</tr>
<tr>
<td>4</td>
<td>Benzeggegh</td>
<td>Exp K</td>
<td>Benzeggegh</td>
<td></td>
</tr>
<tr>
<td>5</td>
<td>Power</td>
<td>Kinoch</td>
<td>Exp Hackle</td>
<td></td>
</tr>
<tr>
<td>6</td>
<td>Yan</td>
<td>Yan</td>
<td>Yan</td>
<td></td>
</tr>
<tr>
<td>7</td>
<td>Hackle</td>
<td>Exp Hackle</td>
<td>Kinoch</td>
<td></td>
</tr>
<tr>
<td>8</td>
<td>Kinoch</td>
<td>Hackle</td>
<td>Hackle</td>
<td></td>
</tr>
<tr>
<td>9</td>
<td>Ramkumar</td>
<td>K</td>
<td>K</td>
<td></td>
</tr>
<tr>
<td>10</td>
<td>Linear</td>
<td>Linear</td>
<td>Linear</td>
<td></td>
</tr>
<tr>
<td>11</td>
<td>K</td>
<td>Ramkumar</td>
<td>Ramkumar</td>
<td></td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Ranking by $G_{IC}$</th>
<th>T800/5245</th>
<th>$0^\circ/0^\circ$</th>
<th>T800/924</th>
<th>Overall</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Power</td>
<td>Gen Int</td>
<td>Gen Int</td>
<td></td>
</tr>
<tr>
<td>2</td>
<td>Ramkumar</td>
<td>Kinoch</td>
<td>Power</td>
<td></td>
</tr>
<tr>
<td>3</td>
<td>Linear</td>
<td>Benzeggegh</td>
<td>Ramkumar</td>
<td></td>
</tr>
<tr>
<td>4</td>
<td>K</td>
<td>Exp K</td>
<td>K</td>
<td></td>
</tr>
<tr>
<td>5</td>
<td>Hackle</td>
<td>Exp Hackle</td>
<td>Linear</td>
<td></td>
</tr>
<tr>
<td>6</td>
<td>Gen Int</td>
<td>Hackle</td>
<td>Hackle</td>
<td></td>
</tr>
<tr>
<td>7</td>
<td>Yan</td>
<td>Yan</td>
<td>Kinoch</td>
<td></td>
</tr>
<tr>
<td>8</td>
<td>Kinoch</td>
<td>K</td>
<td>Yan</td>
<td></td>
</tr>
<tr>
<td>9</td>
<td>Exp Hackle</td>
<td>Linear</td>
<td>Exp K</td>
<td></td>
</tr>
<tr>
<td>10</td>
<td>Exp K</td>
<td>Ramkumar</td>
<td>Exp Hackle</td>
<td></td>
</tr>
<tr>
<td>11</td>
<td>Benzeggegh</td>
<td>Power</td>
<td>Benzeggegh</td>
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</table>
### Table 3-23
Ranking of the failure criteria by $\chi^2$ and $G_{Ic}$ for the results from the MMB tests on 0°/90° ply interface specimens (dry T800/924)

<table>
<thead>
<tr>
<th>Ranking</th>
<th>$\chi^2$</th>
<th>$G_{Ic}$</th>
<th>$\chi^2$</th>
<th>$G_{Ic}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Gen Int</td>
<td>Power</td>
<td>Exp K</td>
<td>Gen Int</td>
</tr>
<tr>
<td>2</td>
<td>Power</td>
<td>Gen Int</td>
<td>Gen Int</td>
<td>Exp K</td>
</tr>
<tr>
<td>3</td>
<td>Benzeggegh</td>
<td>Benzeggegh</td>
<td>Power</td>
<td>Exp Hackle</td>
</tr>
<tr>
<td>4</td>
<td>Yan</td>
<td>Kinloch</td>
<td>Exp Hackle</td>
<td>Ramkumar</td>
</tr>
<tr>
<td>5</td>
<td>Exp Hackle</td>
<td>Exp K</td>
<td>Benzeggegh</td>
<td>Kinloch</td>
</tr>
<tr>
<td>6</td>
<td>Kinoch</td>
<td>Exp Hackle</td>
<td>Kinoch</td>
<td>Benzeggegh</td>
</tr>
<tr>
<td>7</td>
<td>Exp K</td>
<td>K</td>
<td>Ramkumar</td>
<td>Linear</td>
</tr>
<tr>
<td>8</td>
<td>K</td>
<td>Linear</td>
<td>Linear</td>
<td>K</td>
</tr>
<tr>
<td>9</td>
<td>Linear</td>
<td>Ramkumar</td>
<td>Yan</td>
<td>Power</td>
</tr>
<tr>
<td>10</td>
<td>Ramkumar</td>
<td>Yan</td>
<td>K</td>
<td>Yan</td>
</tr>
<tr>
<td>11</td>
<td>Hackle</td>
<td>Hackle</td>
<td>Hackle</td>
<td>Hackle</td>
</tr>
</tbody>
</table>

### Table 4-1
Core properties and predicted failure strains for the Nomex and aluminium honeycomb sandwich panels

<table>
<thead>
<tr>
<th>Property</th>
<th>Core Material</th>
<th>Hexel HRH-10-3.16-3.0</th>
<th>Aeroweb 4.5-1/8-10(5052)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Core</td>
<td>Nomex</td>
<td>Aluminium (5052)</td>
<td></td>
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<tr>
<td>Thickness</td>
<td>50mm</td>
<td>40mm</td>
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<tr>
<td>Density</td>
<td>0.0529gm/cc</td>
<td>0.07209gm/cc</td>
<td></td>
</tr>
<tr>
<td>Cell Size</td>
<td>4.763mm</td>
<td>3.175mm</td>
<td></td>
</tr>
<tr>
<td>$G$</td>
<td>45MPa</td>
<td>482MPa</td>
<td></td>
</tr>
<tr>
<td>$E_c$</td>
<td>138MPa</td>
<td>1340MPa</td>
<td></td>
</tr>
<tr>
<td>$\varepsilon_{crit}$ (buckling)</td>
<td>-6633με</td>
<td>-53017με</td>
<td></td>
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<tr>
<td>$\varepsilon_{crit}$ (wrinkling)</td>
<td>-9545με</td>
<td>-36568με</td>
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### Table 4-2 Honeycomb sandwich panels containing delaminations

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<tr>
<th>Panel</th>
<th>Insert Size</th>
<th>Insert Shape</th>
<th>Ply Interface</th>
<th>Depth</th>
<th>Insert Area</th>
<th>Initiation Strain</th>
</tr>
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<tbody>
<tr>
<td>A</td>
<td>35mm Ø</td>
<td>●</td>
<td>0°/90°</td>
<td>3/4 Interface</td>
<td>962mm²</td>
<td>-2400με</td>
</tr>
<tr>
<td>B</td>
<td>50mm Ø</td>
<td>●</td>
<td>0°/90°</td>
<td>3/4 Interface</td>
<td>1964mm²</td>
<td>-2400με</td>
</tr>
<tr>
<td>C</td>
<td>35mm x 50mm</td>
<td>●</td>
<td>0°/90°</td>
<td>3/4 Interface</td>
<td>1374mm²</td>
<td>-3350με</td>
</tr>
<tr>
<td>D</td>
<td>50mm x 71mm</td>
<td>●</td>
<td>0°/90°</td>
<td>3/4 Interface</td>
<td>2788mm²</td>
<td>-1850με</td>
</tr>
<tr>
<td>E</td>
<td>35mm Ø</td>
<td>●</td>
<td>+45°/-45°</td>
<td>5/6 Interface</td>
<td>962mm²</td>
<td>-4150με</td>
</tr>
<tr>
<td>F</td>
<td>50mm Ø</td>
<td>●</td>
<td>+45°/-45°</td>
<td>5/6 Interface</td>
<td>1964mm²</td>
<td>-3150με</td>
</tr>
<tr>
<td>G</td>
<td>35mm x 50mm</td>
<td>●</td>
<td>+45°/-45°</td>
<td>5/6 Interface</td>
<td>1374mm²</td>
<td>-3150με</td>
</tr>
<tr>
<td>H</td>
<td>50mm x 71mm</td>
<td>●</td>
<td>+45°/-45°</td>
<td>5/6 Interface</td>
<td>2788mm²</td>
<td>-2950με</td>
</tr>
<tr>
<td>I</td>
<td>50mm Ø</td>
<td>●</td>
<td>0°/90°</td>
<td>3/4 Interface</td>
<td>1964mm²</td>
<td>-1950με</td>
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### Table 5-1  Model descriptions and equivalent experimental results

<table>
<thead>
<tr>
<th>Model</th>
<th>Size</th>
<th>Ply Interface</th>
<th>Model Stacking Sequence</th>
<th>Sandwich Panel</th>
</tr>
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<tbody>
<tr>
<td>#1</td>
<td>50mm Ø</td>
<td>0°/90°</td>
<td>[+45°/-45°/0°]</td>
<td>B and I</td>
</tr>
<tr>
<td>#2</td>
<td>50mm Ø</td>
<td>+45°/-45°</td>
<td>[+45°/-45°/0°/90°/+45°]</td>
<td>F</td>
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### Table 5-2  Transverse strain ($\varepsilon_{22}$) at node 15001 and difference from continuum (Craighead) result

<table>
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<tr>
<th>Mesh Density</th>
<th>$\varepsilon_{22}$</th>
<th>% Difference</th>
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<tbody>
<tr>
<td>2</td>
<td>1078με</td>
<td>51.4%</td>
</tr>
<tr>
<td>3</td>
<td>1729με</td>
<td>27.9%</td>
</tr>
<tr>
<td>4</td>
<td>2113με</td>
<td>15.9%</td>
</tr>
<tr>
<td>5</td>
<td>2318με</td>
<td>7.5%</td>
</tr>
<tr>
<td>6</td>
<td>2458με</td>
<td>2.7%</td>
</tr>
<tr>
<td>$\infty$ (Craighead)</td>
<td>2498με</td>
<td>-</td>
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Table 5-3 Predicted delamination initiation strains for Model #1 (Defect at 0°/90° ply interface)

<table>
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<tr>
<th>Ply Interface &gt;&gt;</th>
<th>Data Set &gt;&gt;</th>
<th>0°/0°</th>
<th>0°/90°</th>
<th></th>
<th>Average</th>
<th>Spread</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>dry a=40mm</td>
<td>dry a=60mm</td>
<td>wet a=40mm</td>
<td>dry a=avg</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Criterion</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Linear</td>
<td>2960με</td>
<td>3110με</td>
<td>2990με</td>
<td>3030με</td>
<td>3023με</td>
<td>5.0%</td>
</tr>
<tr>
<td>Power</td>
<td>3050με</td>
<td>3180με</td>
<td>3020με</td>
<td>-</td>
<td>3083με</td>
<td>5.2%</td>
</tr>
<tr>
<td>Yan</td>
<td>2990με</td>
<td>3160με</td>
<td>3040με</td>
<td>3050με</td>
<td>3060με</td>
<td>5.6%</td>
</tr>
<tr>
<td>K</td>
<td>2980με</td>
<td>3120με</td>
<td>3010με</td>
<td>3100με</td>
<td>3053με</td>
<td>4.6%</td>
</tr>
<tr>
<td>Exponential K</td>
<td>3030με</td>
<td>3180με</td>
<td>3040με</td>
<td>2720με</td>
<td>2993με</td>
<td>15.4%</td>
</tr>
<tr>
<td>General Interaction</td>
<td>4530με†</td>
<td>3360με</td>
<td>3180με</td>
<td>2870με</td>
<td>3137με</td>
<td>15.6%</td>
</tr>
<tr>
<td>Ramkumar</td>
<td>2910με</td>
<td>3030με</td>
<td>2940με</td>
<td>2970με</td>
<td>2963με</td>
<td>4.1%</td>
</tr>
<tr>
<td>Hackle</td>
<td>3010με</td>
<td>3170με</td>
<td>3040με</td>
<td>3310με</td>
<td>3133με</td>
<td>9.6%</td>
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<tr>
<td>Exponential Hackle</td>
<td>3000με</td>
<td>3170με</td>
<td>3030με</td>
<td>2760με</td>
<td>2990με</td>
<td>13.7%</td>
</tr>
<tr>
<td>Benzeggagh</td>
<td>3030με</td>
<td>3180με</td>
<td>3050με</td>
<td>2790με</td>
<td>3013με</td>
<td>12.9%</td>
</tr>
<tr>
<td>Kinloch</td>
<td>3070με</td>
<td>3190με</td>
<td>3030με</td>
<td>2940με</td>
<td>3058με</td>
<td>8.2%</td>
</tr>
<tr>
<td><strong>Average</strong></td>
<td><strong>3003με</strong></td>
<td><strong>3168με</strong></td>
<td><strong>3034με</strong></td>
<td><strong>2954με</strong></td>
<td><strong>3058με</strong></td>
<td><strong>12.9%</strong></td>
</tr>
<tr>
<td><strong>Spread</strong></td>
<td><strong>5.3%</strong></td>
<td><strong>10.4%</strong></td>
<td><strong>7.9%</strong></td>
<td><strong>20.0%</strong></td>
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<td></td>
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† Result not included
Table 5-4  Predicted delamination initiation strains for Model #2 (Defect at +45°/-45° ply interface)

<table>
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<tr>
<th>Ply Interface &gt;&gt;</th>
<th>0°/0°</th>
<th>0°/90°</th>
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<tbody>
<tr>
<td></td>
<td>dry a=40mm</td>
<td>dry a=60mm</td>
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<tr>
<td>Data Set &gt;&gt;</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Criterion</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Linear</td>
<td>4010µε</td>
<td>4170µε</td>
</tr>
<tr>
<td>Power</td>
<td>-</td>
<td>4360µε</td>
</tr>
<tr>
<td>Yan</td>
<td>4050µε</td>
<td>4250µε</td>
</tr>
<tr>
<td>K</td>
<td>4030µε</td>
<td>4160µε</td>
</tr>
<tr>
<td>Exponential K</td>
<td>4110µε</td>
<td>4270µε</td>
</tr>
<tr>
<td>General Interaction</td>
<td>4550µε</td>
<td>4400µε</td>
</tr>
<tr>
<td>Ramkumar</td>
<td>3930µε</td>
<td>4070µε</td>
</tr>
<tr>
<td>Hackle</td>
<td>4060µε</td>
<td>4250µε</td>
</tr>
<tr>
<td>Exponential Hackle</td>
<td>4090µε</td>
<td>4250µε</td>
</tr>
<tr>
<td>Benzeggagh</td>
<td>4110µε</td>
<td>4270µε</td>
</tr>
<tr>
<td>Kinloch</td>
<td>4290µε</td>
<td>4350µε</td>
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<tr>
<td>Average</td>
<td>4123µε</td>
<td>4255µε</td>
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<tr>
<td>Spread</td>
<td>15.0%</td>
<td>7.8%</td>
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Table 5-5  Comparison between predicted and experimental initiation strains

<table>
<thead>
<tr>
<th>Model</th>
<th>Size</th>
<th>Interface</th>
<th>Prediction</th>
<th>Experiment</th>
<th>Prediction</th>
<th>Δ</th>
</tr>
</thead>
<tbody>
<tr>
<td>#1</td>
<td>50mm ø</td>
<td>0°/90°</td>
<td>840µε</td>
<td>2175µε</td>
<td>2954µε</td>
<td>+36%</td>
</tr>
<tr>
<td>#2</td>
<td>50mm ø</td>
<td>+45°/-45°</td>
<td>3190µε</td>
<td>3150µε</td>
<td>3883µε</td>
<td>+23%</td>
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</table>

Table 6-1  Cot²β against percentage mode I, where β is the cusp angle

<table>
<thead>
<tr>
<th>% Mode I</th>
<th>T800/5245</th>
<th>T800/924</th>
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</thead>
<tbody>
<tr>
<td></td>
<td>Dry (0°/0°)</td>
<td>Wet (0°/0°)</td>
</tr>
<tr>
<td>100% Mode I</td>
<td>-</td>
<td>-</td>
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<tr>
<td>87.5% Mode I</td>
<td>4.38</td>
<td>2.13</td>
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<tr>
<td>75% Mode I</td>
<td>4.44</td>
<td>1.51</td>
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<tr>
<td>62.5% Mode I</td>
<td>2.64</td>
<td>1.96</td>
</tr>
<tr>
<td>50% Mode I</td>
<td>1.80</td>
<td>0.97</td>
</tr>
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<td>37.5% Mode I</td>
<td>1.45</td>
<td>0.90</td>
</tr>
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<td>25% Mode I</td>
<td>1.06</td>
<td>0.61</td>
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<tr>
<td>12.5% Mode I</td>
<td>0.58</td>
<td>0.11</td>
</tr>
<tr>
<td>0% Mode I</td>
<td>0.10</td>
<td>0.47</td>
</tr>
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</table>
Figures

12. Figures

LOW VELOCITY IMPACT

INCLUSIONS

NOTCHES/HOLES

STRUCTURAL FEATURES

Figure 1-1  Typical sources of delamination in composite structures
Figure 1-2 The mechanism for delamination growth
Figure 1-3  Diagram of modes I, II and III (peel, shear and tearing)
Figures

Figure 1-4  The through-width delamination (TWD)

Figure 1-5  The embedded delamination
Figure 1-6  A CFRP skin-stiffened panel

Figure 1-7  The double cantilever beam test (DCB)
Figure 1-8  The end loaded split test (ELS)

Figure 1-9  The end notched flexure test (ENF)
Figure 1-10 The mixed-mode flexure test (MMF)

Figure 1-11 The variable mixed-mode test (VMM)
Figure 1-12  The mixed-mode bending test (MMB)

Figure 1-13  The symmetrically cracked laminate test (SCL)
Figures

Figure 1-14 The cracked lap shear test (CLS)

Figure 1-15 The edge delamination test (EDT)
Figure 1-16  The Arcan test (ARCAN)

Figure 1-17  The off-axis tension test (OAT)
Figures

Stiffeners consist of four laminates, each of lay-up:

\[-45^\circ/+45^\circ/0^\circ\]_{2s}

Skin Lay-up: \([+45^\circ/-45^\circ/0^\circ/90^\circ]\)_{4s}

Figure 2.1 Dimensions of the skin-stiffened panel
Figures

Figure 2-2  Impact locations on the skin-stiffened panel

Impact A  15J on the centre of the bay
Impact B  15J on the bay, adjacent to a stringer
Impact C  15J on the stringer foot
Impact D  15J on the stringer centreline
Impact E  30J on the stringer centreline
Figure 2-3  Ultrasonic scans of damage caused by 15J impact on a plain panel
Figure 2-4 Ultrasonic scan of the entire skin-stiffened panel after impacting

- Impact A 15J on the centre of the bay
- Impact B 15J on the bay, adjacent to a stringer
- Impact C 15J on the stringer foot
- Impact D 15J on the stringer centreline
- Impact E 30J on the stringer centreline

Figures
Impact A
Impact C
Impact B
Impact D

Figure 2-5  Ultrasonic scans of damage at impact sites A to D

Figure 2-6  Variation in damage area versus distance from the stiffener centreline
Figures

Figure 2-7  Variation in peak impact force versus damage area

Figure 2-8  Variation in peak impact displacement versus damage area
Figure 2-9  Through-thickness damage distribution for an impact in a plain panel and the bay (site A)
Figure 2-10 Through-thickness damage distribution for impacts close to the foot (site B), over the foot (site C) and over the stringer centreline (site D)
Figure 2-11  A pair of matching fracture surfaces at impact site A (15J), in the centre of a bay
Figure 3-1  Diagram of the Arcan loading rig

Figure 3-2  Fracture surfaces of a +45°/-45° ply interface specimen failed under pure mode I loading (x3)
Figure 3-3  Fracture surfaces of a 0°/90° ply interface specimen failed under 35% mode I loading (x3)

Figure 3-4  Dimensions of the insert used for manufacture of the preliminary modified MMB specimens
Figures

(a) $0^\circ/0^\circ$ Conventional Specimen

(b) $\phi_1/\phi_2$ Modified Specimen

(c) $0^\circ/90^\circ$ Specimen

Figure 3-5 Dimensions of the (a) $0^\circ/0^\circ$, (b) $\phi_1/\phi_2$ and (c) $0^\circ/90^\circ$ ply interface MMB specimens
Figure 3-6  Diagram and photograph of the MMB loading rig
Figure 3-7  Fracture toughness ($G_T$) versus crack length for the preliminary 100% mode I tests (DCB) for different ply interfaces
Figure 3-8  Matching fracture surfaces of a MMB 0°/90° ply interface specimen tested at 100% mode I (x3)

Figure 3-9  Matching fracture surfaces of a preliminary MMB +45°/-45° ply interface specimen tested at 100% mode I (x3)
Figure 3-10 Fracture toughness ($G_T$) versus crack length for the preliminary 68% mode I tests (MMB) for different ply interfaces.
Figure 3-11  Micrograph of the fracture surface close to the insert of a preliminary MMB 0°/90° ply interface specimen tested at 68% mode I (x19, 55° tilt)

Figure 3-12  Micrograph of islands of resin failure on the fracture surface of a preliminary MMB 0°/+45° ply interface specimen tested at 0% mode I (x200, 30° tilt)
Figure 3-13  The stress field at the delamination tip.

Figure 3-14  Crack growth at $0^\circ/0^\circ$ and $0^\circ/\phi^\circ$ ply interfaces illustrating the crack plane migration mechanism
Each point on the graphs shown in Figures 3-15 and 3-16 refer to individual toughness versus crack length results, as illustrated in Figure 3-19. The values from each test were interpolated to deduce toughness values at crack lengths of 40mm and 60mm. The line shown on each graph of Figure 3-15 and 3-16 is the average of each set of points for a particular mixed-mode ratio. The scatter in the data is significant, which might limit the relevance of using the averaged values to describe the fracture locus.
Figure 3-15a $G_I$ versus $G_{II}$ for dry T800/5245 at crack lengths of 40mm and 60mm
Figure 3-15b $G_I$ versus $G_{II}$ for wet T800/5245 at crack lengths of 40mm and 60mm.
Figure 3-16a $G_I$ versus $G_{II}$ for dry T800/924 at crack lengths of 40mm and 60mm
Figure 3-16b $G_I$ versus $G_{II}$ for wet T800/924 at crack lengths of 40mm and 60mm
Figure 3-17  Effect of crack length on averaged $G_i$ versus $G_{ii}$ for unidirectional T800/5245 and T800/924
Figures

Figure 3-18
Effect of material on averaged $G_i$ versus $G_{ii}$ for dry and wet unidirectional laminates.
Figure 3-19  Examples of $G_T$ versus crack length for dry T800/5245 and T800/924 tested at 75% mode I

Figure 3-20  Percentage R-curve versus percentage mode I for T800/5245 and T800/924
Figure 3-21 Fracture toughness (G_T) versus crack length for the 87.5%, 75% and 50% mode I tests for dry T800/924 0°/90° (blue) and 0°/0° (red) ply interfaces.
Figure 3-22  $G_i$ versus $G_{II}$ for dry T800/924 (0°/0° and 0°/90° ply interfaces) averaged over all crack lengths
Figure 3-23  Low magnification micrographs of fracture surfaces generated at 100%, 75%, 50%, 25% and 0% mode I loading for dry T800/5245 (x1000)
Figure 3-24  Low magnification micrographs of fracture surfaces generated at 100%, 75%, 50%, 25% and 0% mode I loading for dry T800/924 (x1000)
Figure 3-25 High magnification micrographs of fracture surfaces generated at 75%, 50%, 25% and 0% mode I loading for dry T800/5245 (x5000)

Figure 3-26 High magnification micrographs of fracture surfaces generated at 75%, 50%, 25% and 0% mode I loading for dry T800/924 (x5000)
Figure 3-27  Low magnification micrographs of fracture surfaces generated at 87.5%, 75%, 50%, 25% and 0% mode I loading for dry 0°/90° T800/924 (x1000)
Figure 3-28 High magnification micrographs of fracture surfaces generated at 75%, 50%, 25% and 0% mode I loading for dry 0°/90° T800/924 (x5000)

Figure 3-29 Micrograph of a typical cusp illustrating variety of tilt angles which could be chosen (x4100, 35° tilt)
Figure 3-30 Failure criteria versus summed rank in terms of $\chi^2$ (red) and $G_{IC}$ (blue) for 0°/0° ply interfaces in T800/5245, T800/924 and both materials overall.
Figure 4-1  Design and strain gauge positions on the Nonnex honeycomb sandwich panel

Figures
Figure 4-2  Design and strain gauge positions on the aluminium honeycomb sandwich panel
Figure 4-3  Front skin of the aluminium honeycomb sandwich panel after failure

Figure 4-4  The Moiré grating fixture
Figure 4-5  Illustration of the instrumentation for monitoring the Moiré fringes

Figure 4-6  Photograph of a honeycomb sandwich panel in the test machine (panel I)
Figure 4-7  Development of the damage growth during testing of panel B (50mm diameter circular insert at a 0°/90° [3/4] ply interface) Loading direction is parallel to the 0° ply
Figure 4-8  Development of the damage growth during testing of panel D (50mm x 71mm elliptical insert at a 0°/90° [3/4] ply interface) Loading direction is parallel to the 0° ply
Figure 4-9  Variation in damage height with applied strain for panels A, B, C, D and I (inserts at a $0^\circ/90^\circ$ [3/4] ply interface)

Figure 4-10  Increase in lateral damage extent (width) with applied strain for panels A, B, C, D and I (inserts at a $0^\circ/90^\circ$ [3/4] ply interface)
Figure 4-11  Development of the damage growth during testing of panel F (50mm diameter circular insert at a +45°/-45° [5/6] ply interface) Loading direction is parallel to the 0° ply
Figure 4.12 Development of the damage growth during testing of panel H (50mm x 71mm elliptical insert at +45°/-45° ply interface) Loading direction is parallel to the 0° ply.

No Load

1000με

5000με

3000με

Failure (6000με)
Figure 4-13 Variation in damage height with applied strain for panels E, F, G and H (inserts at a +45°/-45° [5/6] ply interface)

Figure 4-14 Increase in transverse damage extent (width) with applied strain for panels E, F, G and H (inserts at a +45°/-45° [5/6] ply interface)
Figure 4-15  Damage width versus applied strain for 50mm circular defects at the $0^\circ/90^\circ$ [3/4] and $+45^\circ/-45^\circ$ [5/6] ply interfaces

Figure 4-16  Damage width versus applied strain for 50x71mm elliptical defects at the $0^\circ/90^\circ$ [3/4] and $+45^\circ/-45^\circ$ [5/6] ply interfaces
Figure 4-17  Damage height versus applied strain for 50mm circular defects at the $0^\circ/90^\circ [3/4]$ and $+45^\circ/-45^\circ [5/6]$ ply interfaces

Figure 4-18  Damage height versus applied strain for 50x71mm elliptical defects at the $0^\circ/90^\circ [3/4]$ and $+45^\circ/-45^\circ [5/6]$ ply interfaces
Figure 4-19  Lower fracture surface of the damage growth in panel I (50mm diameter circular insert at the 0°/90° [3/4] ply interface)  
Loading direction is parallel to the 0° ply

Figure 4-20  Lower fracture surface of the damage growth in panel C  
(35x50mm elliptical insert at the 0°/90° [3/4] ply interface)  
Loading direction is parallel to the 0° ply
Figure 4-21  Simplified diagram of the lower fracture surface for the damage growth in panel I (50mm diameter circular insert at the 0°/90° [3/4] ply interface) Loading direction is parallel to the 0° ply
Figure 4-22  Micrograph locations for the upper fracture surface (left side) of the damage growth in panel I (50mm diameter circular insert at the 0°/90° [3/4] ply interface)

Loading direction is parallel to the 0° ply
Figure 4-23  Micrograph of the type (i) fracture (0°/90° [3/4] ply interface) at site (1) in Figure 4-22, at the longitudinal boundary of the insert (x400, 7° tilt)

Figure 4-24  Micrograph of the type (i) fracture (0°/90° [3/4] ply interface) at site (2) in Figure 4-22, at the lateral boundary of the insert (x500, 30° tilt)
Figure 4-25  Micrograph of the type (i) fracture (0°/90° [3/4] ply interface) at site (3) in Figure 4-22, showing 0° splitting above the insert (x40, 30° tilt)

Figure 4-26  Micrograph of the type (ii) fracture (-45°/0° [2/3] ply interface) at site (4) in Figure 4-22, showing fracture near the insert (x700, 30° tilt)
Figure 4-27 Micrograph of the type (ii) fracture (-45°/0° [2/3] ply interface) at site (5) in Figure 4-22, showing fracture away from insert (x1000, 25° tilt)

Figure 4-28 Micrograph at site (6) in Figure 4-22, showing -45° ply splitting at the boundary between type (ii) and (iii) surfaces (x22, 30° tilt)
Figure 4-29  Micrograph of fibre shear failure of ply 2 (-45°) along line A-A adjacent to the type (iii) fracture (+45°/-45° [1/2] ply interface) at site (7) in Figure 4-22 (x24, 40° tilt)

Figure 4-30  Micrograph of bundles of sheared fibres from ply 2 (-45°) along line A-A adjacent to the type (iii) fracture (+45°/-45° [1/2] ply interface) at site (7) in Figure 4-22 (x200, 60° tilt)
Figure 4-31 Distribution of mixed-mode fracture for the damage growth in panel I (50mm diameter circular insert at the 0°/90° [3/4] ply interface) showing the percentage of mode I failure at different sites.

Loading direction is parallel to the 0° ply.
Figure 4-32: Distribution of mixed-mode fracture for the damage growth in panel C (35x50mm elliptical insert at the 0°/90° [3/4] ply interface) after the delaminated 0° ply had been removed, showing the percentage of mode I failure at different sites. Loading direction is parallel to the 0° ply.
Figure 4-33  Lower fracture surface of the damage growth in panel E (35mm diameter circular insert at the +45°/-45° [5/6] ply interface)  
Loading direction is parallel to the 0° ply

Figure 4-34  Lower fracture surface of the damage growth in panel G (35x50mm elliptical insert at the +45°/-45° [5/6] ply interface)  
Loading direction is parallel to the 0° ply
Figure 4-35 Simplified diagram of the lower fracture surface of the damage growth in panel E (35mm diameter circular insert at the $+45^\circ/-45^\circ [5/6]$ ply interface) Loading direction is parallel to the $0^\circ$ ply.

Figure 4-36 Upper fracture surface (left side) of the damage growth in panel E (35mm diameter circular insert at the $+45^\circ/-45^\circ [5/6]$ ply interface) Loading direction is parallel to the $0^\circ$ ply.
Figure 4-37  Upper fracture surface (right side) of the damage growth in panel E (35mm diameter circular insert at the +45°/-45° [5/6] ply interface) with the delaminated ply 4 (90°) removed. Loading direction is parallel to the 0° ply.

Figure 4-38  Simplified diagram of the lower fracture surface of the damage growth in panel E (35mm diameter circular insert at the +45°/-45° [5/6] ply interface) with the delaminated ply 4 (90°) removed. Loading direction is parallel to the 0° ply.
Figure 4-39 Micrograph locations for the upper fracture surface of the damage growth in panel E (35mm diameter circular insert at the +45°/-45° [5/6] ply interface). Loading direction is parallel to the 0° ply.
Figure 4-40 Micrograph of the type (iv) fracture (+45°/-45° [5/6] ply interface) at site (1) in Figure 4-39, showing +45° ply splitting and resin cleavage at the insert boundary (x67, 45° tilt)

Figure 4-41 Micrograph of the type (iv) fracture (+45°/-45° [5/6] ply interface) at site (2) in Figure 4-39, showing shallows shear cusps (x700, 55° tilt)
Figure 4-42  Micrograph of the type (v) fracture (90°/+45° [4/5] ply interface) at site (3) in Figure 4-39, showing a shear boundary near the insert (x70, 35° tilt)

Figure 4-43  Micrograph of the type (v) fracture (90°/+45° [4/5] ply interface) at site (4) in Figure 4-39, showing compressive fibre fracture of ply 5 (110x, 60° tilt)
Figure 4-44  Micrograph of the type (vi) fracture (0°/90° [3/4] ply interface) at site (5) in Figure 4-39, showing damage growth from a 90° ply split (x300, 25° tilt)

Figure 4-45  Micrograph of the type (vi) fracture (0°/90° [3/4] ply interface) at site (6) in Figure 4-39, away from the initiation site (x500, 30° tilt)
Figure 4-46  Distribution of mixed-mode fracture for the damage growth in panel E (35mm diameter circular insert at the +45°/-45° [5/6] ply interface) showing the percentage of mode I failure. Loading direction is parallel to the 0° ply.
Figure 4-47  Distribution of mixed-mode fracture for the damage growth in panel G (35x50mm elliptical insert at the +45°/-45° [5/6] ply interface) showing the percentage of mode I failure at different sites. Loading direction is parallel to the 0° ply
Figure 5.1: Mesh geometry for the delamination models
Figure 5-2  Flow chart for the Virtual Crack Closure (VCC) calculation
Figures

Figure 5-3  Schematic diagram showing the variables used in the calculation of $G_i$

Figure 5-4  Schematic diagram showing the variables used in the calculation of $G_{\parallel}$
Figure 5-5  Blister shape (out-of-plane deflection) versus applied strain prior to initiation of delamination growth in Model #1 (defect at 0°/90° ply interface) (peak deflection is shown in brackets)
Figure 5-6  Magnitude of the mode I and II components around the boundary prior to initiation of delamination growth in Model #1 (defect at 0°/90° ply interface)
Figure 5-7  Magnitude of $\varepsilon_{22}$ in the delamination prior to initiation of delamination growth in Model #1 (defect at 0°/90° ply interface) (peak $\varepsilon_{22}$ is shown in brackets)
Figure 5-8  Blister shape versus applied strain prior to initiation of delamination growth in Model #2 (defect at +45°/-45° ply interface) (peak out-of-plane deflection is shown in brackets)
Figure 5-9  Magnitude of the mode I and II components around the boundary prior to initiation of delamination growth in Model #2 (defect at +45°/-45° ply interface)
Figure 5-10  Magnitude of $\varepsilon_{22}$ in the delamination prior to initiation of delamination growth in Model #2 (defect at $+45^\circ$/-45° ply interface) (peak $\varepsilon_{22}$ is shown in brackets)
Figures 5-11  Comparison of blister shape and height between experiments and models for defects at 0°/90° and +45°/-45° ply interfaces

Figures 5-12  Comparison of matrix splitting at the delamination plane between experiments and models for defects at 0°/90° and +45°/-45° ply interfaces
Figure 6-1  Profile of the interply zone thickness in $0^\circ/0^\circ$ and $0^\circ/90^\circ$ ply interfaces; (a) and (b) respectively

Figure 6-2  $\cot^2 \beta$ versus mixed-mode ration ($G_\perp/G_\parallel$), where $\beta$ is the cusp tilt angle
**Figure 6-3**  Mechanism for rib formation at 0°/90° ply interfaces under mixed-mode loading

**Figure 6-4**  Loading conditions at the delamination boundary
(a) General loading conditions
(b) Loading conditions at the longitudinal boundary of the defect
(c) Loading conditions at the transverse boundary of the defect
Figure 6-5  A fracture surface from damage growth in a panel containing an insert at the $0^\circ/90^\circ$ [3/4] ply interface
Figures (a) to (d) are perpendicular to the load.

Figure 6-6 Sequence of failure for damage growth from the inserts at the $0^\circ/90^\circ [3/4]$ ply interfaces.
Section Parallel to the Load

Figures (e), (f) and (h) are perpendicular to the load
Figures (a) to (d) are perpendicular to the load.

Figure 6-7  Sequence of failure for damage growth from the inserts at the +45°/-45° [5/6] ply interfaces.
Figures (e) and (f) are perpendicular to the load.

Figure (g) is parallel to the load and away from the insert.
13. Appendix A  MMB Specimen Stiffness Calculation

The equations for the MMB given by Kinloch:

\[ G_1 = \frac{3 p^2 (a + \chi_i h)^2}{w^2 h^3 E_{11}} \left[ \left( 1 - \frac{c + b}{2L} \right) - \frac{c}{b} \right]^2 \] (A-1)

\[ G_\Pi = \frac{9 p^2 (a + \chi_\Pi h)^2}{4 w^2 h^3 E_{11}} \left[ \left( 1 - \frac{c + b}{2L} \right) + \frac{c}{b} \right]^2 \] (A-2)

where

\[ \chi_\Pi = 0.42 \chi_i \] (A-3)

therefore:

\[ G_1 = \frac{p^2}{2 w E_{11}} (a + \chi_i h)^2 \left[ \frac{6}{w h^3} \left( 1 - \frac{c + b}{2L} \right) - \frac{c}{b} \right]^2 \] (A-4)

\[ = \frac{p^2}{2 w E_{11}} (a + \chi_i h)^2 M \] (A-5)

\[ G_\Pi = \frac{p^2}{2 w E_{11}} (a + 0.42 \chi_i h)^2 \left[ \frac{9}{2 w h^3} \left( 1 - \frac{c + b}{2L} \right) + \frac{c}{b} \right]^2 \] (A-6)

\[ = \frac{p^2}{2 w E_{11}} (a + 0.42 \chi_i h)^2 N \] (A-7)

From basic Linear Elastic Fracture Mechanics

\[ G_1 + G_\Pi = G_I = \frac{p^2}{2 w} \frac{dC}{da} \] (A-8)

combining these expressions

\[ \frac{dC}{da} = \frac{(a + \chi_i h)^2}{E_{11}} M + \frac{(a + 0.42 \chi_i h)^2}{E_{11}} N \] (A-9)

expanding, putting \( C = \delta/P \) and integrating leads to

\[ \frac{\delta}{E_{11} P} = \frac{(M + N)}{3} a^3 + \frac{(M \chi_i h + 0.42N \chi_i h)a^2}{a} + \frac{(M + 0.42^2 N)(\chi_i h)^2 a + B}{a} \] (A-10)

\[ = K + B \] (A-11)

where \( B \) is a constant.
When \( a = a_0; \delta = \delta_0, P = P_0 \) and substituting into the above expression:

\[
E_{II} \frac{\delta_0}{P_0} = K_0 + B
\]  

(A-12)

where \( K_0 = K(a_0) \), rearranging to give:

\[
B = E_{II} \frac{\delta_0}{P_0} - K_0
\]  

(A-13)

This finally leads to

\[
E_{II} = \frac{P P_0 (K - K_0)}{(\delta P_0 - \delta_0 P)}
\]  

(A-14)

where

\[
K = \frac{(M + N)}{3} a^3 + (M \chi a h + 0.42N \chi h) a^2 + (M + 0.42^2 N)(\chi h)^2 a
\]  

(A-15)

\[
M = \frac{6}{wh^3} \left( \left(1 - \frac{c + b}{2L}\right) - \frac{c}{b} \right)^2
\]  

(A-16)

\[
N = \frac{9}{2wh^3} \left( \left(1 - \frac{c + b}{2L}\right) + \frac{c}{b} \right)^2
\]  

(A-17)
### 14. Appendix B Parameters for the Failure Criteria

Table B-1  Optimum parameter values and goodness of fit ($\chi^2$) for the Linear Criterion from the experimental mixed-mode test results

<table>
<thead>
<tr>
<th>Data Set</th>
<th>$\chi^2$</th>
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<th>$G_{IIc}$</th>
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<td>T800/5245 0°/0° Dry (a=40mm)</td>
<td>11.32</td>
<td>185.9</td>
<td>544.9</td>
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<tr>
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<td>200.9</td>
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<td>1.36</td>
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<td>437.4</td>
</tr>
<tr>
<td>T800/924 0°/0° Dry (a=all)</td>
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<td>T800/924 0°/90° Dry (a=all)</td>
<td>2.14</td>
<td>213.0</td>
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Table B-2  Optimum parameter values and goodness of fit ($\chi^2$) for the Power Law Criterion from the experimental mixed-mode test results

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<th>$n$</th>
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<tr>
<td>T800/5245 0°/0° Dry (a=40mm)</td>
<td>3.19</td>
<td>241.1</td>
<td>222.7</td>
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<td>0.004</td>
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<td>T800/5245 0°/0° Dry (a=60mm)</td>
<td>1.57</td>
<td>294.1</td>
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<td>T800/5245 0°/0° Wet (a=40mm)</td>
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<td>213.4</td>
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<td>T800/5245 0°/0° Wet (a=60mm)</td>
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Table B-3  Optimum parameter values and goodness of fit ($\chi^2$) for the Yan Criterion from the experimental mixed-mode test results

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<th>Data Set</th>
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<th>$G_{ic}$</th>
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<th>$\tau$</th>
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<td>T800/5245 0°/0° Dry (a=40mm)</td>
<td>10.94</td>
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<td>181.2</td>
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<td>T800/5245 0°/0° Wet (a=60mm)</td>
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<td>245.9</td>
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<td>-1.7</td>
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<td>T800/924 0°/0° Wet (a=40mm)</td>
<td>0.42</td>
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<td>T800/924 0°/0° Wet (a=60mm)</td>
<td>0.50</td>
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<td>219.8</td>
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* $G_{IIc}$

Table B-4  Optimum parameter values and goodness of fit ($\chi^2$) for the K Law Criterion from the experimental mixed-mode test results

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### Table B-5  Optimum parameter values and goodness of fit ($\chi^2$) for the Exponential K Law Criterion from the experimental mixed-mode test results

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<td>0.11</td>
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### Table B-6  Optimum parameter values and goodness of fit ($\chi^2$) for the General Interaction Criterion from the experimental mixed-mode test results

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<th>$\Phi$</th>
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<td>T800/5245 0°/0° Dry (a=60mm)</td>
<td>1.65</td>
<td>295.2</td>
<td>473.5</td>
<td>-2.3</td>
<td>0.006</td>
</tr>
<tr>
<td>T800/5245 0°/0° Wet (a=40mm)</td>
<td>1.60</td>
<td>213.3</td>
<td>8771.1</td>
<td>-87.4</td>
<td>0.3</td>
</tr>
<tr>
<td>T800/5245 0°/0° Wet (a=60mm)</td>
<td>9.04</td>
<td>293.9</td>
<td>362.2</td>
<td>-2.2</td>
<td>0.007</td>
</tr>
<tr>
<td>T800/924 0°/0° Dry (a=40mm)</td>
<td>0.48</td>
<td>287.7</td>
<td>121.3</td>
<td>4.7</td>
<td>-0.04</td>
</tr>
<tr>
<td>T800/924 0°/0° Dry (a=60mm)</td>
<td>0.61</td>
<td>276.4</td>
<td>356.4</td>
<td>0.03</td>
<td>-0.01</td>
</tr>
<tr>
<td>T800/924 0°/0° Wet (a=40mm)</td>
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<td>229.5</td>
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<td>-0.01</td>
</tr>
<tr>
<td>T800/924 0°/0° Wet (a=60mm)</td>
<td>0.41</td>
<td>230.8</td>
<td>1041.9</td>
<td>-8.0</td>
<td>0.02</td>
</tr>
<tr>
<td>T800/924 0°/0° Dry (a=all)</td>
<td>0.00</td>
<td>277.7</td>
<td>544.7</td>
<td>-0.9</td>
<td>-0.03</td>
</tr>
<tr>
<td>T800/924 0°/90° Dry (a=all)</td>
<td>0.15</td>
<td>185.0</td>
<td>537.1</td>
<td>-1.4</td>
<td>0.007</td>
</tr>
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</table>
Appendix B Parameters for the Failure Criteria

Table B-7  Optimum parameter values and goodness of fit ($\chi^2$) for the Ramkumar Criterion from the experimental mixed-mode test results

<table>
<thead>
<tr>
<th>Data Set</th>
<th>$\chi^2$</th>
<th>$G_{IC}$</th>
<th>$G_{IC}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>T800/5245 0°/0° Dry (a=40mm)</td>
<td>9.30</td>
<td>178.3</td>
<td>408.1</td>
</tr>
<tr>
<td>T800/5245 0°/0° Dry (a=60mm)</td>
<td>2.07</td>
<td>287.6</td>
<td>334.5</td>
</tr>
<tr>
<td>T800/5245 0°/0° Wet (a=40mm)</td>
<td>7.01</td>
<td>185.1</td>
<td>307.9</td>
</tr>
<tr>
<td>T800/5245 0°/0° Wet (a=60mm)</td>
<td>14.49</td>
<td>314.5</td>
<td>291.8</td>
</tr>
<tr>
<td>T800/924 0°/0° Dry (a=40mm)</td>
<td>4.12</td>
<td>188.6</td>
<td>368.9</td>
</tr>
<tr>
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<td>2.88</td>
<td>212.2</td>
<td>326.0</td>
</tr>
<tr>
<td>T800/924 0°/0° Wet (a=40mm)</td>
<td>1.47</td>
<td>195.4</td>
<td>371.4</td>
</tr>
<tr>
<td>T800/924 0°/0° Wet (a=60mm)</td>
<td>3.22</td>
<td>207.4</td>
<td>382.1</td>
</tr>
<tr>
<td>T800/924 0°/0° Dry (a=all)</td>
<td>5.00</td>
<td>179.4</td>
<td>379.4</td>
</tr>
<tr>
<td>T800/924 0°/90° Dry (a=all)</td>
<td>1.20</td>
<td>199.8</td>
<td>528.0</td>
</tr>
</tbody>
</table>

Table B-8  Optimum parameter values and goodness of fit ($\chi^2$) for the Hackle Criterion from the experimental mixed-mode test results

<table>
<thead>
<tr>
<th>Data Set</th>
<th>$\chi^2$</th>
<th>$G_{IC}$</th>
<th>$\Omega$</th>
<th>$B$</th>
</tr>
</thead>
<tbody>
<tr>
<td>T800/5245 0°/0° Dry (a=40mm)</td>
<td>13.47</td>
<td>176.1</td>
<td>43.0</td>
<td>10.2</td>
</tr>
<tr>
<td>T800/5245 0°/0° Dry (a=60mm)</td>
<td>1.74</td>
<td>300.1</td>
<td>0.04</td>
<td>911995.6</td>
</tr>
<tr>
<td>T800/5245 0°/0° Wet (a=40mm)</td>
<td>1.65</td>
<td>209.6</td>
<td>249538.6</td>
<td>0.0</td>
</tr>
<tr>
<td>T800/5245 0°/0° Wet (a=60mm)</td>
<td>16.12</td>
<td>319.7</td>
<td>0.02</td>
<td>555040.4</td>
</tr>
<tr>
<td>T800/924 0°/0° Dry (a=40mm)</td>
<td>3.30</td>
<td>215.9</td>
<td>127.1</td>
<td>0.9</td>
</tr>
<tr>
<td>T800/924 0°/0° Dry (a=60mm)</td>
<td>1.10</td>
<td>247.5</td>
<td>122.5</td>
<td>0.5</td>
</tr>
<tr>
<td>T800/924 0°/0° Wet (a=40mm)</td>
<td>0.57</td>
<td>219.1</td>
<td>65.1</td>
<td>2.0</td>
</tr>
<tr>
<td>T800/924 0°/0° Wet (a=60mm)</td>
<td>0.51</td>
<td>233.5</td>
<td>55.9</td>
<td>2.3</td>
</tr>
<tr>
<td>T800/924 0°/0° Dry (a=all)</td>
<td>5.05</td>
<td>267.7</td>
<td>0.04</td>
<td>4664.8</td>
</tr>
<tr>
<td>T800/924 0°/90° Dry (a=all)</td>
<td>14.29</td>
<td>269.2</td>
<td>0.03</td>
<td>12358.0</td>
</tr>
</tbody>
</table>
Appendix B  Parameters for the Failure Criteria

Table B-9  Optimum parameter values and goodness of fit ($\chi^2$) for the Exponential Hackle Criterion from the experimental mixed-mode test results

<table>
<thead>
<tr>
<th>Data Set</th>
<th>$\chi^2$</th>
<th>$G_{IC}$</th>
<th>$G_{IIc}$</th>
<th>$\gamma$</th>
<th>$B$</th>
</tr>
</thead>
<tbody>
<tr>
<td>T800/5245 0°/0° Dry (a=40mm)</td>
<td>8.10</td>
<td>156.3</td>
<td>348.3</td>
<td>1055.9</td>
<td>0.002</td>
</tr>
<tr>
<td>T800/5245 0°/0° Dry (a=60mm)</td>
<td>1.45</td>
<td>279.6</td>
<td>350.9</td>
<td>502.0</td>
<td>0.5</td>
</tr>
<tr>
<td>T800/5245 0°/0° Wet (a=40mm)</td>
<td>1.65</td>
<td>209.6</td>
<td>586981.5</td>
<td>1.7</td>
<td>0.0</td>
</tr>
<tr>
<td>T800/5245 0°/0° Wet (a=60mm)</td>
<td>9.15</td>
<td>244.9</td>
<td>254.6</td>
<td>4418.1</td>
<td>0.06</td>
</tr>
<tr>
<td>T800/924 0°/0° Dry (a=40mm)</td>
<td>3.09</td>
<td>211.1</td>
<td>432.0</td>
<td>2296.3</td>
<td>0.0</td>
</tr>
<tr>
<td>T800/924 0°/0° Dry (a=60mm)</td>
<td>1.07</td>
<td>246.2</td>
<td>435.6</td>
<td>224.6</td>
<td>0.001</td>
</tr>
<tr>
<td>T800/924 0°/0° Wet (a=40mm)</td>
<td>0.45</td>
<td>217.5</td>
<td>417.6</td>
<td>594.8</td>
<td>0.001</td>
</tr>
<tr>
<td>T800/924 0°/0° Wet (a=60mm)</td>
<td>0.51</td>
<td>236.0</td>
<td>469.3</td>
<td>33.2</td>
<td>0.01</td>
</tr>
<tr>
<td>T800/924 0°/90° Dry (a=all)</td>
<td>0.14</td>
<td>221.5</td>
<td>545.4</td>
<td>349.1</td>
<td>0.001</td>
</tr>
<tr>
<td>T800/924 0°/90° Dry (a=all)</td>
<td>0.28</td>
<td>83.6</td>
<td>523.0</td>
<td>0.01</td>
<td>5705.5</td>
</tr>
</tbody>
</table>

Table B-10  Optimum parameter values and goodness of fit ($\chi^2$) for the Benzeggegh Criterion from the experimental mixed-mode test results

<table>
<thead>
<tr>
<th>Data Set</th>
<th>$\chi^2$</th>
<th>$G_{IC}$</th>
<th>$G_{IIc}$</th>
<th>$m$</th>
</tr>
</thead>
<tbody>
<tr>
<td>T800/5245 0°/0° Dry (a=40mm)</td>
<td>8.77</td>
<td>173.9</td>
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</tr>
<tr>
<td>T800/5245 0°/0° Dry (a=60mm)</td>
<td>1.46</td>
<td>-107042.4</td>
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</tr>
<tr>
<td>T800/5245 0°/0° Wet (a=40mm)</td>
<td>1.42</td>
<td>220.5</td>
<td>685.1</td>
<td>5.7</td>
</tr>
<tr>
<td>T800/5245 0°/0° Wet (a=60mm)</td>
<td>11.59</td>
<td>-545050.8</td>
<td>358.9</td>
<td>0.0</td>
</tr>
<tr>
<td>T800/924 0°/0° Dry (a=40mm)</td>
<td>2.42</td>
<td>219.8</td>
<td>509.5</td>
<td>2.7</td>
</tr>
<tr>
<td>T800/924 0°/0° Dry (a=60mm)</td>
<td>0.87</td>
<td>251.7</td>
<td>437.0</td>
<td>3.3</td>
</tr>
<tr>
<td>T800/924 0°/0° Wet (a=40mm)</td>
<td>0.29</td>
<td>223.6</td>
<td>455.3</td>
<td>2.3</td>
</tr>
<tr>
<td>T800/924 0°/0° Wet (a=60mm)</td>
<td>0.67</td>
<td>242.6</td>
<td>486.1</td>
<td>3.1</td>
</tr>
<tr>
<td>T800/924 0°/0° Dry (a=all)</td>
<td>0.019</td>
<td>232.1</td>
<td>545.0</td>
<td>4.2</td>
</tr>
<tr>
<td>T800/924 0°/90° Dry (a=all)</td>
<td>0.30</td>
<td>135.4</td>
<td>505.8</td>
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</table>
Table B-11 Optimum parameter values and goodness of fit ($\chi^2$) for the Kinloch Criterion from the experimental mixed-mode test results

<table>
<thead>
<tr>
<th>Data Set</th>
<th>$\chi^2$</th>
<th>$G_{ic}$</th>
<th>$G_0$</th>
<th>$\omega$</th>
<th>$\phi_0$</th>
</tr>
</thead>
<tbody>
<tr>
<td>T800/5245 0°/0° Dry (a=40mm)</td>
<td>9.59</td>
<td>138.4</td>
<td>122.7</td>
<td>0.6</td>
<td>-0.4</td>
</tr>
<tr>
<td>T800/5245 0°/0° Dry (a=60mm)</td>
<td>1.69</td>
<td>292.1</td>
<td>284.6</td>
<td>1.1</td>
<td>-0.3</td>
</tr>
<tr>
<td>T800/5245 0°/0° Wet (a=40mm)</td>
<td>3.12</td>
<td>218.8</td>
<td>202.9</td>
<td>0.5</td>
<td>0.3</td>
</tr>
<tr>
<td>T800/5245 0°/0° Wet (a=60mm)</td>
<td>16.44</td>
<td>331.0</td>
<td>331.1</td>
<td>1.6</td>
<td>0.3</td>
</tr>
<tr>
<td>T800/924 0°/0° Dry (a=40mm)</td>
<td>1.83</td>
<td>268.5</td>
<td>223.4</td>
<td>0.0</td>
<td>0.4</td>
</tr>
<tr>
<td>T800/924 0°/0° Dry (a=60mm)</td>
<td>0.66</td>
<td>277.1</td>
<td>246.8</td>
<td>0.6</td>
<td>0.4</td>
</tr>
<tr>
<td>T800/924 0°/0° Wet (a=40mm)</td>
<td>0.32</td>
<td>229.2</td>
<td>221.3</td>
<td>0.6</td>
<td>0.2</td>
</tr>
<tr>
<td>T800/924 0°/0° Wet (a=60mm)</td>
<td>0.92</td>
<td>236.3</td>
<td>231.6</td>
<td>0.7</td>
<td>0.2</td>
</tr>
<tr>
<td>T800/924 0°/90° Dry (a=all)</td>
<td>0.16</td>
<td>554.7</td>
<td>228.8</td>
<td>0.6</td>
<td>0.3</td>
</tr>
<tr>
<td>T800/924 0°/90° Dry (a=all)</td>
<td>1.11</td>
<td>515.6</td>
<td>181.6</td>
<td>0.6</td>
<td>-0.2</td>
</tr>
</tbody>
</table>

*${G_{iiC}}$
15. Appendix C Design Criteria for Sandwich Panels

The critical mode of failure of the panel is Euler buckling which occurs at \( \varepsilon_{\text{crit}} \). From Reference 190, the buckling load of a strut, \( P_{\text{crit}} \), with fixed ends is:

\[
P_{\text{crit}} = \frac{P_E}{1 + \frac{P_E}{AG}}
\]

where \( G \) is the shear stiffness of the core and \( P_E, A \) and \( D_2 \) are given by:

\[
P_E = \frac{4\pi^2D_2}{L^2}
\]

\[
A = \frac{b(c + t)^2}{c}
\]

\[
D_2 = \frac{Et(c + t)^2b}{2(1 - v^2)}
\]

where \( b \) is the panel width, \( L \) is the panel length, \( c \) is the core thickness, \( t \) is the skin thickness, \( E \) is the axial stiffness of the skin and \( v \) is Poisson’s ratio of the skin material. Combining these expressions and substituting into,

\[
\varepsilon_{\text{crit}} = \frac{P_{\text{crit}}}{2Ebt}
\]

leads to

\[
\varepsilon_{\text{crit}} = \frac{\pi^2G(c + t)^2}{L^2G(1 - v^2) + 2\pi^2Etc}
\]

This expression is based on the assumption that the panel is ‘thin-skinned’, i.e.

\[
3\frac{(c + 2t)^2}{t^2} \geq 100
\]

A further failure mode is skin wrinkling, which is given:

\[
\varepsilon_{\text{crit}} = \beta \left[ \frac{E_c}{E} \right]^{\frac{3}{2}}
\]

where \( E_c \) is the core stiffness and

\[
\beta = 3\left[ 2(3 - v)^2(1 + v)^2 \right]^{\kappa}
\]
16. Appendix D Analysis of the Cusp Tilt Angle

Consider a resin layer between two plies of a laminate loaded under a mixed-mode loading ratio of $G_i/G_{ii}$. The far field out-of plane stress, $\sigma_\infty$ is applied at an angle $\theta$ to the fibres and can be partitioned into $\sigma_i$ and $\sigma_{ii}$, the mode I and mode II components. From this the associated stress intensity factors $K_i$ and $K_{ii}$ at the crack tip are\textsuperscript{34,133},

\begin{align}
K_i &= Y_i \sigma_\infty \sin^2 \theta \sqrt{\pi a} \\
K_{ii} &= Y_{ii} \sigma_\infty \sin \theta \cos \theta \sqrt{\pi a}
\end{align}

where $a$ is the initial crack length and $Y_i$ and $Y_{ii}$ are correction factors\textsuperscript{32}. These are related to the strain energy release rates $G_i$ and $G_{ii}$\textsuperscript{132} by;

\begin{align}
G_i &= K_i^2 \frac{1}{2E_{ii}E_{22}} \left[ \frac{E_{ii}}{E_{22}} - \nu + \frac{E_{11}}{2G_{12}} \right] \\
G_{ii} &= K_{ii}^2 \frac{1}{2E_{ii}E_{22}} \left[ \frac{E_{ii}}{E_{22}} - \nu + \frac{E_{11}}{2G_{12}} \right]
\end{align}

Substituting for $K_i$ and $K_{ii}$ and dividing these expressions to give the mixed-mode ratio $(G_i/G_{ii})$ leads to:

$$
\frac{G_i}{G_{ii}} = \frac{Y_i^2}{Y_{ii}^2} \frac{E_{ii}}{E_{22}} \tan^2 \theta
$$

The back face of the cusp is generated by crack formation perpendicular to the applied stress and therefore $\beta = 90^\circ - \theta$, leading to;

$$
\frac{G_i}{G_{ii}} = \frac{Y_i^2}{Y_{ii}^2} \frac{E_{ii}}{E_{22}} \cot^2 \beta
$$