Modelling environmental degradation in adhesively bonded joints

by

Yongxia Hua

Materials, Surfaces and Structural Systems
School of Engineering
University of Surrey
Guildford
Surrey
United Kingdom

Submitted for a degree of Doctor of Philosophy in April 2006
To P., M. and F.
The aim of this research was to develop predictive numerical modelling for environmental degradation of adhesively bonded joints. It is generally believed that the residual strength of an aged joint can be reduced by continuum degradation of the adhesive or interfacial degradation between adhesives and adherends. Both kinds of degradation modelling, using the finite element method, were involved in this project. However, the emphasis has been on modelling the continuum degradation in adhesive joints.

A previously developed cohesive zone model (CZM) has been modified and extended to incorporate plasticity of the substrates in predicting the interfacial degradation of AV119 bonded joints. A traction-separation law has been used to control interfacial rupture element in this CZM instead of a strain-separation law. The moisture dependent material parameters governing the traction-separation law were determined using a mixed mode flexure (MMF) test and then used to predict the other joint configurations with no further modification.

Experimental work used to inform and validate the FE modelling in this research was discussed. Bulk adhesives, FM73, EA9321 and E32, and their bonded joints have been considered. Long-term experimental testing on a series of FM73 bonded aluminium - composite double lap joints was undertaken by the author. A simple method to quantitatively define the failure surfaces was developed. The experimental data have been useful in validating the predictive modelling undertaken by a co-worker in the research group.

Further validation modelling work of FM73 and EA9321 bonded joints was carried out focusing on stress analysis in the bonding adhesives under undegraded and degraded conditions. Single lap joint (SLJ) and 3-point bending T joint
configurations were considered. Both two-dimensional and three-dimensional modelling was undertaken and the results were compared. The FEA solutions were compared with the experimental data obtained by BAe systems using an in-situ microscope and useful conclusions were drawn.

A strain-based progressive cohesive failure model has been proposed to predict the continuum degradation of adhesively bonded joints using a moisture-dependent critical equivalent plastic strain for the adhesive. A single lap joint and a 3-point bending T joint bonded with a ductile adhesive (EA9321) have been studied. The critical strain parameter was calibrated using an aged MMF test. Both 2D and 3D predictions were undertaken and the results compared. The predicted joint residual strengths agreed well with the corresponding experimental data, and the damage propagation pattern in the adhesives was also predicted correctly. The mesh dependence of the strain parameter was also investigated. Swelling effects of the adhesive and the composite substrates in the joints were studied.

A mesh-independent continuum damage model was then proposed to predict continuum environmental degradation in adhesive bonded joints by introducing a displacement-based damage parameter into the constitutive equation of damaged materials. Joints bonded with EA9321 and E32 were studied for different experimental degradation conditions. Both von Mises and linear Drucker-Prager yield models were considered. 2D and 3D modelling was compared and mesh independence of the model was demonstrated. Not only the residual strength of the adhesive joints but also the damage initiation and propagation details in the adhesive layers were predicted successfully. The continuum damage model has demonstrated to be a highly efficient and reliable method to model environmental degradation in ductile adhesively bonded joints.
ACKNOWLEDGEMENT

I am deeply indebted to my supervisors Prof A. D. Crocombe and Dr M. A. Wahab at University of Surrey. Their patient guidance, stimulating suggestions and enthusiastic encouragement helped me through the whole process of this research till the writing of this thesis.

Thanks are also due to Dr. I. A. Ashcroft at Loughborough University. It was him who first recommended me to enrol in this research project, and his open-minded suggestions and encouragement had also inspired me a lot in doing my research work.

A sincere gratitude should go to all other research partners involved in this research: Mr. C. D. M. Liljedahl (University of Surrey), Mr. F. Jumbo (Loughborough University), Mr. S. Millington (QinetiQ), Dr. S. J. Shaw (DSTL), Mr. J. F. Sargent (BAE) and Mr. T. Ackerman (MBDA).

I would also like to thank Dr. B. Le-Page and Mr. P. Haynes at University of Surrey for advice on experimental methods and equipment usage.

Thank papa, mama, and my sister in China. Their spiritual support is also important in help me work through these three years.
DECLARATION

The author declares the work presented in this thesis original and appropriate to the subject area.
PUBLICATIONS


# TABLE OF CONTENT

<table>
<thead>
<tr>
<th>Section</th>
<th>Page</th>
</tr>
</thead>
<tbody>
<tr>
<td>ABSTRACT</td>
<td>i</td>
</tr>
<tr>
<td>ACKNOWLEDGEMENT</td>
<td>iii</td>
</tr>
<tr>
<td>DECLARATION</td>
<td>iv</td>
</tr>
<tr>
<td>PUBLICATIONS</td>
<td>v</td>
</tr>
<tr>
<td>TABLE OF CONTENT</td>
<td>vi</td>
</tr>
<tr>
<td>LIST OF FIGURES</td>
<td>xi</td>
</tr>
<tr>
<td>LIST OF TABLES</td>
<td>xx</td>
</tr>
<tr>
<td>1 INTRODUCTION</td>
<td>1</td>
</tr>
<tr>
<td>1.1 Research framework and objectives</td>
<td>2</td>
</tr>
<tr>
<td>1.2 Research methodology and thesis structure</td>
<td>3</td>
</tr>
<tr>
<td>2 LITERATURE REVIEW</td>
<td>8</td>
</tr>
<tr>
<td>2.1 Durability testing of adhesively bonded joints</td>
<td>8</td>
</tr>
<tr>
<td>2.1.1 Mechanical testing methods used for adhesively boned joints</td>
<td>9</td>
</tr>
<tr>
<td>2.1.2 Mechanisms of environmental degradation in adhesively boned joints</td>
<td>12</td>
</tr>
<tr>
<td>2.1.3 Moisture degradation experiments of adhesively boned joints</td>
<td>14</td>
</tr>
<tr>
<td>2.1.3.1 Ageing environment</td>
<td>14</td>
</tr>
<tr>
<td>2.1.3.2 Surface treatment</td>
<td>17</td>
</tr>
<tr>
<td>2.2 Modelling environmental degradation in adhesively bonded joints</td>
<td>19</td>
</tr>
<tr>
<td>2.2.1 Strength of materials method</td>
<td>20</td>
</tr>
<tr>
<td>2.2.2 Fracture mechanics method</td>
<td>23</td>
</tr>
<tr>
<td>2.2.3 Continuum damage modelling method</td>
<td>27</td>
</tr>
<tr>
<td>2.2.3.1 Virtual internal bond (VIB) model</td>
<td>27</td>
</tr>
<tr>
<td>2.2.3.2 Gurson-based model</td>
<td>29</td>
</tr>
<tr>
<td>2.2.3.3 Continuum damage mechanics (CDM) model</td>
<td>32</td>
</tr>
<tr>
<td>2.2.3.4 Other continuum modelling methods</td>
<td>34</td>
</tr>
<tr>
<td>2.2.4 Modelling environmental degradation of adhesively bonded joints</td>
<td>35</td>
</tr>
<tr>
<td>2.2.4.1 Moisture diffusion characterisation and FE modelling</td>
<td>35</td>
</tr>
<tr>
<td>2.2.4.2 Moisture dependent mechanical properties</td>
<td>41</td>
</tr>
<tr>
<td>2.2.4.3 Hygroscopic swelling of adhesively boned joints</td>
<td>47</td>
</tr>
</tbody>
</table>
Table of Content

2.2.4.4 Environmental degradation modelling of adhesively boned joints .................. 49
2.3 Summary and conclusion .................................................................................. 51

3 PREDICTIVE MODELLING OF AV119 SINGLE LAP JOINTS USING
COHESIVE ZONE MODEL ........................................................................... 53
  3.1 Material property characterisation ................................................................ 54
  3.2 Rupture element calibration ........................................................................ 56
    3.2.1 Interfacial failure and the cohesive zone model ...................................... 56
    3.2.2 Rupture element calibration of AV119 using MMF .............................. 59
  3.3 Single lap joint (SLJ) modelling ................................................................ 63
  3.4 Summary and conclusion ............................................................................ 69

4 EXPERIMENTAL TESTING .......................................................................... 71
  4.1 Material properties and moisture uptake measurement ............................. 71
    4.1.1 Adhesive FM73 ................................................................................... 72
    4.1.2 Adhesive EA9321 ............................................................................ 73
    4.1.3 Adhesive E32 .................................................................................... 75
    4.1.4 Substrate materials .......................................................................... 78
  4.2 Aluminium-FM73-composite Double Lap Joint ........................................ 79
    4.2.1 Substrate pre-treatment and geometry of the specimens ....................... 80
    4.2.2 Ageing environments and testing plan ............................................... 81
    4.2.3 Loading procedure and testing results ............................................... 84
    4.2.4 Failure surface analysis .................................................................... 87
    4.2.5 Summary ......................................................................................... 89
  4.3 Mixed Mode Flexure test of EA9321 .......................................................... 89
  4.4 Thick Adherend Shear Test of EA9321 ...................................................... 91
  4.5 Single Lap Joint (EA9321/aluminium, EA9321/composite) ......................... 92
  4.6 3-point bending test of the EA9321/composite T joint ............................... 96
  4.7 E32 bonded butt joint ............................................................................. 100
  4.8 Summary and conclusion ......................................................................... 103

5 VALIDATION MODELLING OF FM73 AND EA9321 BONDED SINGLE
LAP JOINTS ............................................................................................... 105
  5.1 Introduction ......................................................................................... 106
  5.2 Finite element model and material property ............................................ 106
5.2.1 FE models.................................................................................... 106
5.2.2 Material models........................................................................... 109
5.3 Stress analysis............................................................................. 113
5.3.1 FM73/aluminium single lap joint, dry................................................... 113
5.3.2 FM73/composite single lap joint, dry.................................................. 118
5.3.3 EA9321/composite single lap joint, dry................................................. 122
5.3.4 FM73/aluminium single lap joint with fillet, 50°C/ water......................... 123
5.3.5 FM73/composite single lap joint with fillet, 50°C, 95%RH....................... 124
5.3.6 EA9321/composite single lap joint without fillet, 50°C, 95%RH................. 125
5.4 Summary and conclusion............................................................. 126

6 PREDICTIVE MODELLING OF EA9321 SINGLE LAP JOINTS USING A
STRAIN-BASED FAILURE MODEL..................................................... 127

6.1 Strain-based failure model calibration.............................................. 128
6.1.1 Strain-based failure model and critical strain......................................... 128
6.1.2 MMF calibration of critical strain for EA9321........................................... 129
6.1.2.1 2D MMF calibration............................................................................... 129
6.1.2.2 3D MMF calibration............................................................................ 133
6.2 Predictive modelling using the strain-based failure model....................... 134
6.2.1 2D predictive modelling of EA9321/aluminium single lap joint................. 134
6.2.2 2D predictive modelling of EA9321/composite single lap joint............... 139
6.2.3 3D predictive modelling of EA9321/aluminium single lap joint............... 143
6.2.4 3D predictive modelling of EA9321/composite single lap joint................. 146
6.3 Summary and conclusion............................................................. 148

7 PREDICTIVE MODELLING OF EA9321 SINGLE LAP JOINTS USING A
CONTINUUM DAMAGE MODEL...................................................... 150

7.1 Mesh-independent continuum damage failure model........................... 151
7.2 The continuum damage model calibration using MMF......................... 153
7.2.1 Von Mises model calibration (2D and 3D).............................................. 154
7.2.2 Linear Drucker-Prager model calibration (2D and 3D)............................. 158
7.3 Predictive modelling using the continuum damage model............. . .................. 159
7.3.1 2D continuum damage model for EA9321/aluminium joints in conjunction with von Mises yield model........................................ 159
<table>
<thead>
<tr>
<th>Section</th>
<th>Title</th>
<th>Page</th>
</tr>
</thead>
<tbody>
<tr>
<td>7.3.2</td>
<td>2D continuum damage model for EA9321/aluminium joints in conjunction with linear Drucker-Prager model</td>
<td>162</td>
</tr>
<tr>
<td>7.3.3</td>
<td>2D continuum damage models for EA9321/composite joints (von Mises and linear Drucker-Prager model)</td>
<td>163</td>
</tr>
<tr>
<td>7.3.4</td>
<td>3D continuum damage model for EA9321/aluminium joints in conjunction with von Mises yield model</td>
<td>166</td>
</tr>
<tr>
<td>7.3.5</td>
<td>3D continuum damage model for EA9321/composite joints in conjunction with von Mises yield model</td>
<td>170</td>
</tr>
<tr>
<td>7.4</td>
<td>Comparison of the continuum damage model and the strain-based failure model</td>
<td>173</td>
</tr>
<tr>
<td>7.5</td>
<td>Summary and conclusion</td>
<td>174</td>
</tr>
<tr>
<td>8.1</td>
<td>Experimental testing summary</td>
<td>177</td>
</tr>
<tr>
<td>8.2</td>
<td>FE model and stress analysis</td>
<td>180</td>
</tr>
<tr>
<td>8.2.1</td>
<td>Shell elements and FE models</td>
<td>180</td>
</tr>
<tr>
<td>8.2.2</td>
<td>Validation modelling</td>
<td>187</td>
</tr>
<tr>
<td>8.2.2.1</td>
<td>Validation modelling for the dry specimen</td>
<td>187</td>
</tr>
<tr>
<td>8.2.2.2</td>
<td>Validation modelling for the conditioned specimen</td>
<td>189</td>
</tr>
<tr>
<td>8.3</td>
<td>Summary and conclusion</td>
<td>192</td>
</tr>
<tr>
<td>9.1</td>
<td>Predictive modelling of the full width EA9321/composite T joints using a strain-based failure model</td>
<td>195</td>
</tr>
<tr>
<td>9.1.1</td>
<td>FE model and stress analysis</td>
<td>195</td>
</tr>
<tr>
<td>9.1.2</td>
<td>Strain-based failure model calibration</td>
<td>197</td>
</tr>
<tr>
<td>9.1.3</td>
<td>Strain-based failure model prediction</td>
<td>199</td>
</tr>
<tr>
<td>9.2</td>
<td>Predictive modelling of the full width EA9321/composite T joints using continuum damage model</td>
<td>201</td>
</tr>
<tr>
<td>9.2.1</td>
<td>Reduced width FE model</td>
<td>201</td>
</tr>
<tr>
<td>9.2.2</td>
<td>Continuum damage model prediction</td>
<td>202</td>
</tr>
<tr>
<td>9.3</td>
<td>Summary and conclusion</td>
<td>205</td>
</tr>
</tbody>
</table>
Table of Content

10 PREDICTIVE MODELLING OF E32 BONDED BUTT JOINTS USING A
CONTINUUM DAMAGE MODEL................................................................. 207
  10.1 FE model and moisture diffusion................................................... 208
  10.2 Predictive modelling using continuum damage model.................... 210
    10.2.1 Von Mises model................................................................. 210
    10.2.2 Drucker-Prager model.......................................................... 213
    10.2.3 Prediction of damage initiation and propagation in the E32 bonded butt
        joints....................................................................................... 216
    10.2.4 A verification of the damage initiation in the E32 bonded butt joints... 218
  10.3 Summary and conclusion............................................................ 220

11 SWELLING IN PREDICTIVE MODELLING OF MOISTURE
DEGRADED EA9321 BONDED JOINTS.................................................... 222
  11.1 Swelling in predictive modelling of the saturated MMF ................ 222
  11.2 Swelling in predictive modelling of the aluminium single lap joint exposed
      for 26 weeks.............................................................................. 224
  11.3 Swelling in predictive modelling of the composite single lap joint exposed
      for 26 weeks.............................................................................. 226
  11.4 Summary and conclusion............................................................ 228

12 CONCLUSIONS AND FUTURE WORK................................................. 229
  12.1 Conclusions.............................................................................. 229
  12.2 Future work.............................................................................. 231

REFERENCES..................................................................................... 233

APPENDIXES..................................................................................... 241
  Appendix 4.1 Aluminium-FM73-composite DLJs: test samples.............. 241
  Appendix 4.2 Aluminium-FM73-composite DLJs: test programme............ 242
LIST OF FIGURES

Figure 1.1  Durability Framework [1] ................................................................. 3
Figure 2.1  Double cantilevered beam (DCB) test (left) and end notch flexure test (right) [5] .... 10
Figure 2.2  Single lap joint (SLJ) specimen geometry [6] ........................................... 10
Figure 2.3  Thick adherend shear test (TAST) specimen [9] ...................................... 11
Figure 2.4  Schematic of double lap specimen [10] .................................................. 11
Figure 2.5  Schematic of T-peel joint test ............................................................... 12
Figure 2.6  Schematic of butt joint specimen ......................................................... 12
Figure 2.7  Interfacial failure and cohesive failure of a butt joint exposed to moisture for degradation [24] .......................................................... 14
Figure 2.8  Dominant paths for water ingress in: a) closed adhesive joint; b) open-faced specimen [32] .............................................................................. 15
Figure 2.9  A moisture degradation effect of the reduced shear strength after exposure to 5%RH and 100%RH [33] .......................................................... 16
Figure 2.10  The effect of temperature and humidity on the failure strength obtained using butt joint after exposure to 5%RH and 100%RH [35] ............................................... 16
Figure 2.11  The joint strength increased to a maximum value before reducing during the exposure to 100% RH at 52°C [33] .......................................................... 17
Figure 2.12  Model used to investigate singularity strength in Weibull method [53] ............ 21
Figure 2.13  Mesh details of square adhesive free ends of an adhesively-bonded single-lap joint using DZM [60] ............................................................... 23
Figure 2.14  Geometry of the system and traction-separation law used for CZM [73] ........ 25
Figure 2.15  Comparison of (a) the cohesive surface approach and (b) the VIB approach of building a cohesive law directly into the constitutive model [82] .................. 28
Figure 2.16  Sketch of the RVE for porous material in the Gurson model [89] .................. 30
Figure 2.17  Elementary volume element. (Definition of the net resisting area and the effective section for a damaged material.) [106] ........................................... 32
Figure 2.18  Hourglass specimen and dimensions for damage measurement [105] .............. 34
Figure 2.19  Dual stage Fickian diffusion model is the combination of two single Fickian models for AV119 [123] ............................................................. 38
Figure 2.20  Mechanical properties of Araldite 2014 during ageing test measured in tension [131] .................................................................................. 43
Figure 2.21  Gc versus exposure time for: (a) Cybond 1126 degraded at 65°C with 100% RH(+), 60% RH(x) and 30%RH( ); (b) Cybond 4523GB degraded at 65°C/100%RH, tested wet at 48°(o) and 60°( ), and dry at 48°(o) and 60°( ) [31] .................. 44
Figure 2.22  Geometry and loading configuration of the NCA specimen [123] .................. 46
<table>
<thead>
<tr>
<th>Figure</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>2.23</td>
<td>Fracture energy as a function of interfacial moisture concentration of AV119 characterised using the NCA tests [123]</td>
</tr>
<tr>
<td>2.24</td>
<td>Fracture energy as a function of interfacial moisture concentration of AV119 characterised using the MMF tests [123]</td>
</tr>
<tr>
<td>3.1</td>
<td>Moisture dependent constitutive properties of AV119 (mwt(_\text{w})=7.6%) [123]</td>
</tr>
<tr>
<td>3.2</td>
<td>Experimental data fitted with single Fickian diffusion model for 2.0mm thick film</td>
</tr>
<tr>
<td>3.3</td>
<td>Schematic illustration of the damage formed ahead of the crack tip along the interface</td>
</tr>
<tr>
<td>3.4</td>
<td>The separation law for the interfacial rupture element</td>
</tr>
<tr>
<td>3.5</td>
<td>Arrangement of multiple rupture elements along the interface in a finite element model</td>
</tr>
<tr>
<td>3.6</td>
<td>Geometry and loading configuration of the MMF specimen [123]</td>
</tr>
<tr>
<td>3.7</td>
<td>Experimental failure load of MMF at different crack lengths (markers) and predicted failure load of MMF specimens using the calibrated fracture parameters (lines)</td>
</tr>
<tr>
<td>3.8</td>
<td>Initial peak loads (20mm crack length) as a function of interfacial moisture concentration and the resulting moisture dependent interfacial fracture energy of steel/AV119 interface</td>
</tr>
<tr>
<td>3.9</td>
<td>MMF finite element model with the rupture elements along the interface of the upper substrate and the adhesive [123]</td>
</tr>
<tr>
<td>3.10</td>
<td>Tripping traction calibration for MMF specimen at different moisture concentration levels</td>
</tr>
<tr>
<td>3.11</td>
<td>Schematic configuration of the single lap joint used by [6] (not to scale)</td>
</tr>
<tr>
<td>3.12</td>
<td>(a) Finite element mesh of single lap joint; (b) Mesh refinement in the adhesive layer; (c) Deformation of the joint with crack extension</td>
</tr>
<tr>
<td>3.13</td>
<td>Elastic-plastic behaviour of the steel from [6] used for the substrates in the SLJ modelling</td>
</tr>
<tr>
<td>3.14</td>
<td>Moisture distribution profile of the adhesive layer exposed for 21 days</td>
</tr>
<tr>
<td>3.15</td>
<td>Predicted ultimate failure load of the SLJ using the different schemes listed in Table 3.2 and the experimental results from [6]</td>
</tr>
<tr>
<td>3.16</td>
<td>Typical contour plots of equivalent plastic strain at integration points (PEEQ) for TST1 and TST3 diffusion scheme at the unloading increment (21 days)</td>
</tr>
<tr>
<td>3.17</td>
<td>Predicted loading history and crack propagation of SLJ specimens after exposure to a moist environment for 0 and 21 days (TST1&amp;TST3)</td>
</tr>
<tr>
<td>4.1</td>
<td>Moisture dependent tensile properties of bulk FM73 (experimental data obtained from QinetiQ)</td>
</tr>
<tr>
<td>4.2</td>
<td>Experimental data (obtained from QinetiQ) fitted with single Fickian diffusion model for 0.48mm thick EA9321</td>
</tr>
<tr>
<td>4.3</td>
<td>Moisture dependent tensile properties of bulk EA9321(experimental data obtained from QinetiQ)</td>
</tr>
<tr>
<td>4.4</td>
<td>CHE (%(^3)) of EA9321 exposed to 95.8%RH/50°C(experimental data obtained from QinetiQ)</td>
</tr>
<tr>
<td>4.5</td>
<td>Experimental uptake data for E32 and uptake data modelled using a two-stage model with a boundary effect(experimental data from [122])</td>
</tr>
</tbody>
</table>
List of Figures

Figure 4.6  Moisture dependent tensile properties of E32 (from [122]) used for modelling......... 78
Figure 4.7  Tensile properties of aluminum substrates ($E = 72\text{GPa}$, $v = 0.3$) [148].......... 78
Figure 4.8  CHE (%') of the composite exposed to 95.8%RH/50°C [149] ............................ 79
Figure 4.9  A typical double lap joint to be tested............................................................. 80
Figure 4.10 Shadow graph (Mitutoyo Profile Projector PJ 300) used to measure the actual overlap
lengths of the experiment DLJ joints.......................................................... 81
Figure 4.11 Degradation environments and controlment: (a) an ageing chamber with a digital
hygrometer; (b) each of the ageing chambers was placed in the air ventilated oven at
50°C or 70°C as listed in Table 4.1.................................................................. 82
Figure 4.12 Loading setup for the DLJ testing...................................................................... 84
Figure 4.13 Calibration of contacting extensometer using a bench micrometer..................... 85
Figure 4.14 Experimental failure loads of the aluminum-FM73-composite DLJs for different
ageing environments and times................................................................................ 85
Figure 4.15 Study of bondline thickness effect on failure loads of different joints under the same
degradation conditions: (a) 80.1%RH /70°C; (b) 95.8%RH /50°C.............................. 86
Figure 4.16 Typical failure surfaces: (a) Cohesive failure primarily of the unconditioned dry joint;
(b) Interfacial failure overwhelmingly of a joint conditioned in the environment of
95.8%RH /50°C for 52 weeks........................................................................ 87
Figure 4.17 Post-processing procedure of a photoed surface of the joints.............................. 88
Figure 4.18 Interfacial failure surface ratios from the micro-scanned photographs of the tested
joints.................................................................................................................. 88
Figure 4.19 Geometry and loading configuration of the MMF specimen (width= 12.7mm)[149].. 90
Figure 4.20 Experimental failure load of MMF joints at different crack lengths[149]................. 90
Figure 4.21 Failure surfaces for the EA9321 MMF specimens: (a) Dry; (b) 79.5%RH; (c)
95.8%RH[149].................................................................................. 91
Figure 4.22 Configuration of the EA9321/steel TAST specimen (not to scale)....................... 91
Figure 4.23 Experimental shear stress-strain curve of EA9321 obtained from the TAST [150]...... 92
Figure 4.24 (a)EA9321/7075-T6 Single lap joint geometry (not to scale); (b)EA9321/IM7-8552
Single lap joint geometry (not to scale)...................................................................... 93
Figure 4.25 Fillet configuration of the EA9321 bonded SLJs (not to scale)......................... 93
Figure 4.26 Experimental failure load of the EA9321 bonded SLJs for the different ageing times
[151]........................................................................................................ 94
Figure 4.27 Typical aluminium single lap joint failure surface [151]........................................ 95
Figure 4.28 Composite single lap joint failure surfaces [151]: (a) Dry; (b) Wet......................... 96
Figure 4.29 3-point bend test rig set up for the EA9321 /composite T joints [151].................... 97
Figure 4.30 Composite T section of full width (25mm) joints with lay-up [151] shown in Table
4.10........................................................................................................ 97
Figure 4.31 Failure loads obtained from the experiment of the EA9321/composite T joint for a
range of exposure times [151]............................................................................. 99
Figure 4.32 Typical 3-point bend test failure surface of a T joint degraded for 2 weeks at 50°C
/95%RH [151].............................................................................................. 99
Figure 4.33  Showing direction of crack propagation during failure of the 3-point bend EA9321/composite T joint [151].................................100

Figure 4.34  Geometry and loading configuration of the E32 bonded butt joints: (a) steel, bondline thickness = 0.48mm, width = 2.9mm; (b) aluminium, bondline thickness = 0.48mm, width = 2.5mm.................100

Figure 4.35  Experimental failure loads of E32 bonded steel and aluminium butt joints (water at room temperature) [122]........................................102

Figure 4.36  Macro-photograph of a dry E32/steel butt joint showing cohesive failure [122]........102

Figure 4.37  Macro-photograph of a wet (75days) E32/steel butt joint showing cohesive failure [122]........................................................................102

Figure 5.1  Typical 2D model and mesh refinement at the overlap........................................108

Figure 5.2  Typical 3D model and mesh refinement..........................................................108

Figure 5.3  Moisture dependent tensile properties used for FM73 in the modelling.................109

Figure 5.4  Linear Drucker-Prager model: (a) yield surface and flow direction in the p-t plane [146]; (b) fitted curve with experimental data of FM73 (from QinetiQ) with K =0.8, $\beta =23.0^\circ$...................................................................................110

Figure 5.5  FE model of the EA9321/steel TAST (smallest mesh size: 0.225mmx0.2mm)..............111

Figure 5.6  Typical calibration results of the parameters for the linear Drucker-Prager model of EA9321 using the TAST: (a) K=1; (b) $\beta =20^\circ$..................................................112

Figure 5.7  Linear Drucker-Prager model calibration of EA9321 using the TAST (*selected)........................................................................112

Figure 5.8  Interface displacement difference along the overlap as a function of load for FM73/Aluminum (Dry, without fillet, von Mises)..................................................113

Figure 5.9  Interface displacement difference along the overlap as a function of load for FM73/Aluminum (Dry, with fillet, von Mises)................................................114

Figure 5.10  Interface displacement difference along the overlap as a function of load for FM73/Aluminum (Dry, 3D, with fillet)........................................................115

Figure 5.11  Interface displacement difference along the overlap as a function of load for FM73/Aluminum (Dry, 3D, without fillet).................................................116

Figure 5.12  Experimental strain field maps of fillet of the dry FM73/Aluminum single lap joint as a function of load: (a) $\varepsilon_{xy}$; (b) $\varepsilon_{x}$; (c) $\varepsilon_{y}$..........................................................117

Figure 5.13  FE strain distribution in the fillet of the dry FM73/Aluminum single lap joint at the load of 132N/mm (Von Mises adhesive): (a) $\varepsilon_{xy}$; (b) $\varepsilon_{x}$; (c) $\varepsilon_{y}$..........................................................117

Figure 5.14  Interface displacement difference along the overlap as a function of load for FM73/Composite (Dry, with fillet)..................................................118

Figure 5.15  Interface displacement difference along the overlap as a function of load for FM73/Composite (Dry, without fillet)..................................................120

Figure 5.16  Experimental strain field maps of fillet of the dry FM73/Composite single lap joint as a function of load: (a) $\varepsilon_{xy}$; (b) $\varepsilon_{x}$; (c) $\varepsilon_{y}$..........................................................120

Figure 5.17  Engineering shear strain distribution in the fillet of the dry FM73/Composite single lap joint at the different load levels (Von Mises adhesive).................121
Figure 5.18 Engineering shear strain distribution in the fillet of the dry FM73/Composite single lap joint at the different load levels (Drucker-Prager adhesive) ............................................. 121
Figure 5.19 Interface displacement difference along the overlap as a function of load for EA9321/Composite (Dry, without fillet) ................................................................. 122
Figure 5.20 Interface displacement difference along the overlap as a function of load for FM73/Aluminum (50°C/water, with fillet)................................................................. 123
Figure 5.21 Interface displacement difference along the overlap as a function of load for FM73/Composite (50°C, 95%RH, with fillet) .................................................. 124
Figure 5.22 Interface displacement difference along the overlap as a function of load for EA9321/composite (50°C, 95%RH, without fillet) .............................................. 125
Figure 6.1 Strain-based progressive damage model limited by critical strain $\varepsilon_{pl,cr}$ ................................................................. 129
Figure 6.2 FE model of the MMF with mesh refinement along the adhesive overlap .............. 130
Figure 6.3 Critical strain calibration using MMF specimens at different moisture concentration levels (Smallest mesh size 0.05mmx0.05mm, m$_{w}$ = 3.85%) .............. 131
Figure 6.4 Experimental failure load of MMF joints at different crack lengths (markers) and predicted failure load of MMF specimens using the calibrated failure strain (lines) .... 131
Figure 6.5 Critical strain calibration for MMF specimens at different moisture concentration levels for different mesh schemes (m$_{w}$ = 3.85%): (a) undegraded; (b) degraded .... 132
Figure 6.6 3D model of the EA9321/aluminum MMF test and local mesh refinement (smallest mesh size: 0.1mmx0.1mmx0.5mm) ................................................................. 133
Figure 6.7 MMF predictions from the 2D and 3D modelling using the same set of critical strain (smallest mesh size: 0.1mmx0.1mmx0.5mm) ..................................................... 134
Figure 6.8 FE model of the EA9321/aluminum SLJ and local mesh refinement (smallest mesh size: 0.05mmx0.05mm) ................................................................. 135
Figure 6.9 Contour of the moisture distribution in the adhesive layer of the EA9321/aluminium joint after exposure to moisture for 2, 8, 26 weeks ........................................ 136
Figure 6.10 Predicted ultimate failure load of the EA9321/aluminium SLJ using the strain-based failure model ................................................................. 136
Figure 6.11 Damage initiation and propagation in the EA9321/aluminum SLJ model (26weeks degraded, Mises stress contour, smallest mesh size 0.05mmx0.05mm)........... 137
Figure 6.12 Damage in the EA9321/aluminum SLJ model with different mesh schemes (26weeks degraded, Mises stress contour) ................................................................. 138
Figure 6.13 Predicted loading history and damage propagation in an undegraded joint and a joint aged for 26 weeks (EA9321/aluminium, smallest mesh size 0.05mmx0.05mm) ..... 138
Figure 6.14 FE model of the EA9321/composite SLJ and local mesh refinement (smallest mesh size: 0.05mmx0.05mm) ................................................................. 139
Figure 6.15 Moisture distribution profile in the SLJ adhesive layer from the diffusion analysis m1 and m2 (with and without the composite moisture diffusion) .............. 140
Figure 6.16 Predicted ultimate failure load of the EA9321/composite SLJ for the different composite diffusion schemes (smallest mesh size 0.05mmx0.05mm) .............. 141
Figure 6.17 Predicted loading history and damage propagation in an undegraded joint and a joint aged for 26 weeks (EA9321/composite SLJ model, smallest mesh size 0.05mmx0.05mm) .... 142
Figure 6.18 Damage in the EA9321/composite SLJ model for the dry joint and the joint aged for 26 weeks (smallest mesh size 0.05mmx0.05mm, Mises stress contour) ......................................................142

Figure 6.19 3D quarter model of the EA9321/Aluminium SLJ and local mesh refinement (smallest mesh size: 0.1mmx0.1mmx0.5mm) .......................................................... 143

Figure 6.20 Moisture diffusion profile of 3D quarter model of the EA9321/aluminium SLJ specimen (26 weeks degraded) ............................................................................144

Figure 6.21 Predicted ultimate failure load of the EA9321/aluminium SLJ from the 2D and the 3D models (smallest mesh size 0.1mmx0.1mmx0.5mm) .........................144

Figure 6.22 Predicted loading history and damage propagation in a 3D and 2D EA9321/aluminium SLJ model (26 weeks degraded, smallest mesh size 0.1mmx0.1mmx0.5mm) ...145

Figure 6.23 3D damage propagation in the EA9321/aluminium SLJ model (26 weeks degraded, Mises stress contour, smallest mesh size 0.1mmx0.1mmx0.5mm) .............146

Figure 6.24 Predicted ultimate failure load of the EA9321/composite SLJ from the 2D and the 3D models (smallest mesh size 0.1mmx0.1mmx0.5mm) .........................147

Figure 6.25 Predicted loading history and damage propagation in 3D and 2D EA9321/composite SLJ models (26 weeks degraded, smallest mesh size 0.1mmx0.1mmx0.5mm) ........147

Figure 7.1 A damaged material response according to the equivalent plastic displacement over the characteristic length using a continuum damage failure model ......................151

Figure 7.2 Damage curve calibrations of dry EA9321 using MMF (mesh size 0.1mmx0.1mm, von Mises): (a) calibration curves (of unit-size elements); (b) predicted loading history ..154

Figure 7.3 Mesh independence of the continuum damage failure model: (a) actual tensile stress-strain responses in elements with different mesh sizes; (b) predicted loading history ............................................................................................156

Figure 7.4 Moisture dependent damage curve calibration results of EA9321 using the continuum damage model of MMF (von Mises) ........................................156

Figure 7.5 Predicted failure loads of the MMF using the continuum damage model with the different mesh sizes (von Mises) .................................................................157

Figure 7.6 3D model of the MMF test and local mesh refinement used for the continuum damage model (smallest mesh size: 0.25mmx0.25mmx0.25mm) .................158

Figure 7.7 Damage curve calibration results of EA9321 using the continuum damage failure model of MMF (Drucker-Prager, K=0.9, β =25°) .................................158

Figure 7.8 Predicted ultimate failure load of the EA9321/aluminium SLJ using the continuum damage model with the different mesh schemes (von Mises) .................................160

Figure 7.9 Damage propagation in the EA9321/aluminium SLJ model (26 weeks degraded, smallest mesh size 0.05mmx0.05mm, von Mises) .............................................161

Figure 7.10 Predicted loading history and damage propagation in an undegraded joint and a joint aged for 26 weeks (EA9321/aluminium SLJ model, mesh size 0.05mmx0.05mm, von Mises) ..........................................................161

Figure 7.11 Predicted ultimate failure load of the EA9321/aluminium SLJ using the continuum damage model with the different mesh schemes (Drucker-Prager) .........................163

Figure 7.12 Predicted ultimate failure load of the EA9321/composite SLJ using the continuum damage model (smallest mesh size 0.05mmx0.05mm) .............................164

Figure 7.13 Predicted loading history and damage propagation in an undegraded joint and a joint aged for 26 weeks (EA9321/composite SLJ model, mesh size 0.05mmx0.05mm, von Mises) ..........................................................165
<table>
<thead>
<tr>
<th>Figure</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>7.14</td>
<td>3D quarter constrained model of the EA9321/aluminium SLJ and local mesh refinement (smallest mesh size: 0.1mmx0.1mmx0.1mm)</td>
</tr>
<tr>
<td>7.15</td>
<td>Comparison of the peel stress and shear stress along the middle of the bondline of the EA9321/aluminum SLJ from the integrated model (shown in Figure 6.19) and the constrained model (shown in Figure 7.14) (von Mises)</td>
</tr>
<tr>
<td>7.16</td>
<td>Predicted ultimate failure load of the EA9321/aluminum SLJ using the continuum damage 2D and 3D model (von Mises)</td>
</tr>
<tr>
<td>7.17</td>
<td>Predicted loading history and damage propagation in a 3D and 2D EA9321/aluminum SLJ model after exposure for 26 weeks (von Mises)</td>
</tr>
<tr>
<td>7.18</td>
<td>3D damage propagation in the EA9321/aluminum SLJ model using the continuum damage model (26 weeks degraded, von Mises)</td>
</tr>
<tr>
<td>7.19</td>
<td>Predicted ultimate failure load of the EA9321/composite SLJ using the continuum damage 2D and 3D model (von Mises)</td>
</tr>
<tr>
<td>7.20</td>
<td>Predicted loading history and damage propagation in a 3D and 2D EA9321/composite SLJ model after exposure for 26 weeks (von Mises)</td>
</tr>
<tr>
<td>7.21</td>
<td>3D damage propagation in the EA9321/composite SLJ model using the continuum damage model (26 weeks degraded, von Mises)</td>
</tr>
<tr>
<td>8.1</td>
<td>3-point bending T joint test piece and loading configuration (unit: mm)</td>
</tr>
<tr>
<td>8.2</td>
<td>Fillet of the noodle part of the composite T joint (BAe)</td>
</tr>
<tr>
<td>8.3</td>
<td>3-point bending rig on straining stage of optical microscope (BAe)</td>
</tr>
<tr>
<td>8.4</td>
<td>End of adherend overlap for an unconditioned T joint: a) at 6N/mm; b) at 108N/mm; c) after failure</td>
</tr>
<tr>
<td>8.5</td>
<td>Conventional versus continuum shell element (ABAQUS)</td>
</tr>
<tr>
<td>8.6</td>
<td>FE models of the reduced width EA9321/composite T joint: (a) continuum shell elements; (b) conventional shell elements</td>
</tr>
<tr>
<td>8.7</td>
<td>Modelling lay-up property orientations in Layer B of the EA9321/composite T joint: (a) continuum shell elements; (b) conventional shell elements</td>
</tr>
<tr>
<td>8.8</td>
<td>A verification example of the FE modelling laminated composite layers using LAP: (a) FE modelled strain distributions; (b) LAP solution</td>
</tr>
<tr>
<td>8.9</td>
<td>Comparison of stress distribution using a closed form sandwich analysis: (a) the end-loaded sandwich; (b) modelled stress distribution along the adhesive layer</td>
</tr>
<tr>
<td>8.10</td>
<td>Deflection of the FE modelled T joint at the load 108N/mm (unit: mm): (a) continuum shell element; (b) conventional shell element</td>
</tr>
<tr>
<td>8.11</td>
<td>Interface displacement difference along the overlap as a function of load for reduced width EA9321/composite T joint (Dry)</td>
</tr>
<tr>
<td>8.12</td>
<td>Equivalent plastic strain in the adhesive of the reduced width EA9321/composite T joint model at the load of 108N/mm (Dry): (a) von Mises; (b) linear Drucker-Prager</td>
</tr>
<tr>
<td>8.13</td>
<td>Comparison of the strain field map in the noodle of the joint loaded at 97N/mm (Dry, von Mises): (a) Experiment; (b) FE modelling (LE12 is engineering shear strain)</td>
</tr>
<tr>
<td>8.14</td>
<td>Interface displacement difference along the overlap as a function of load for the conditioned EA9321/composite T joint (solid line-von Mises, dashed line-Drucker-Prager)</td>
</tr>
</tbody>
</table>
| 8.15   | Equivalent plastic strain in the adhesive of the reduced width EA9321/composite T }
List of Figures

Figure 8.16 Equivalent plastic strain in the adhesive of the reduced width EA9321/composite T joint model at the load of 139N/mm (conditioned): (a) von Mises; (b) linear Drucker-Prager ..........................190

Figure 8.17 Comparison of the strain field map in the noodle of the joint loaded at 114N/mm (Conditioned, von Mises): (a) Experiment; (b) FE modelling (L12 is engineering shear strain) ..................191

Figure 9.1 FE models of the full width EA9321/composite T joint: (a) continuum shell element; (b) conventional shell element (used for the multi-directional T-section) ..................192

Figure 9.2 Illustration of hourglassing effect using reduced integration elements (shown as Mises stress distribution along the adhesive overlap) ...........................................................................193

Figure 9.3 FE model of the EA9321/aluminum MMF test and local mesh refinement (smallest mesh size: 0.25mmx0.125mmx1.0mm) ...........................................................................194

Figure 9.4 Predicted loading history and damage propagation in the model of EA9321/composite model (dry) .........................................................................................................................195

Figure 9.5 Damage propagation in the adhesive layer of the EA9321/composite T joint model (shown as Mises stress distribution in the adhesive layer, conventional shell element, dry): (a) damage initiation; (b) damage propagation; (c) after failure ..........................196

Figure 9.6 FE model of the EA9321/composite T joint with reduced width of 0.13mm: (a) continuum shell element; (b) conventional shell element ..............197

Figure 9.7 Predicted loading history and damage propagation in the EA9321/composite T joint model using continuum damage model (dry, conventional shell element, damage counted from D>0) .........................................................................................198

Figure 9.8 Damage propagation in the adhesive layer of the EA9321/composite T joint model using continuum damage model (dry, von Mises) ..........................199

Figure 9.9 Comparison of the predictions obtained from the strain-based failure model and the continuum damage model of the EA9321/composite T joint (dry, von Mises, conventional shell element, damage counted till D=1) ...........................................................................200

Figure 10.1 Local FE model and mesh refinement of the steel butt joint ..........................201

Figure 10.2 FE bulk model of E32 used to validate the two-stage Fickian diffusion model ..........................202

Figure 10.3 Validated diffusion profile using the two-stage Fickian diffusion model ..........................203

Figure 10.4 Moisture concentration contour (for the total uptake) of the 3D model for the E32/steel joint degraded for 18.7 days (water at room temperature) ...........................................................................204

Figure 10.5 Calibration for E32 (von Mises, dry) using the continuum damage model: (a) calibrated material curves; (b) predicted loading histories ..........................205

Figure 10.6 Moisture dependent calibration damage curves of E32 using the continuum damage model (von Mises) ...........................................................................................................206

Figure 10.7 Predicted failure loads for the steel butt joints using the continuum damage model (von Mises) ...........................................................................................................207

Figure 10.8 Moisture dependent calibration damage curves of E32 using the continuum damage model (linear Drucker-Prager, K=1.0, ß=26.56°) ...........................................................................208

Figure 10.9 Predicted failure loads for the steel butt joints using the continuum damage model (linear Drucker-Prager) ...........................................................................................................209
List of Figures

Figure 10.10 Predicted failure loads for the aluminum butt joints using the continuum damage model (linear Drucker-Prager) ............................................................... 215

Figure 10.11 Loading history of the steel butt joint models using the continuum damage model (linear Drucker-Prager) ................................................................. 216

Figure 10.12 Modelling yield initiation in the adhesive layer of the steel butt joints (linear Drucker-Prager): (a) dry; (b) 18.7days ......................................................... 217

Figure 10.13 Damage initiation and propagation in the adhesive layer of the steel butt joints (linear Drucker-Prager): (a) dry; (b) 18.7days (SDEG = stiffness degradation, the damage parameter D in ABAQUS) ...................................................... 217

Figure 10.14 Plots of the damage initiation locus for a wet (exposed for 13.8days) steel butt joint model: (a) actual equivalent plastic strain distribution at the damage initiation point; (b) critical damage initiation plastic strain allocation in the adhesive layer; (c) damage initiation occurs at the locus where (a)>(b) ................................................ 218

Figure 10.15 Loading history of the aluminium butt joint models using the continuum damage model (linear Drucker-Prager model) ........................................ 220

Figure 11.1 Comparison of the predicted results of the EA9321/aluminium MMF with and without swelling (saturated, smallest mesh size 0.1mmx0.1mmx0.5mm) ......................................................... 223

Figure 11.2 Equivalent plastic strain caused by the swelling in the saturated EA9321/aluminium MMF ................................................................. 224

Figure 11.3 Comparison of the predicted results of the EA9321/aluminium SLJ with and without swelling (26 weeks, smallest mesh size 0.1mmx0.1mmx0.5mm) ......................................................... 225

Figure 11.4 Equivalent plastic strain caused by the swelling in the EA9321/aluminium SLJ exposed for 26 weeks ................................................................. 225

Figure 11.5 Comparison of the predicted results of the EA9321/composite SLJ with and without swelling (26 weeks, smallest mesh size 0.1mmx0.1mmx0.5mm) ......................................................... 227

Figure 11.6 Equivalent plastic strain caused by the swelling in the EA9321/composite SLJ exposed for 26 weeks: (a) only adhesive swelling; (b) fully swelling (including composite) .227
LIST OF TABLES

Table 2.1  Moisture diffusion parameters obtained from a range of adhesives and exposure environments.................................................................39
Table 2.2  Swelling coefficients for epoxy adhesives obtained from literature review........49
Table 3.1  Standard Fickian diffusion coefficients for 2.0mm thick AV119 at 50°C........55
Table 3.2  Diffusion schemes used to model the moisture uptake of the SLJ joint........66
Table 4.1  Fickian diffusion data for 0.48mm thick EA9321(experimental data obtained from QinetiQ).................................................................73
Table 4.2  Parameters of E32 for a two-stage Fickian diffusion model with boundary effect [122].................................................................77
Table 4.3  Mechanical properties of IM7-8552 [147]........................................78
Table 4.4  Fickian diffusion data for IM7-8552 unidirectional CFRP [149]........79
Table 4.5  Selected degradation environments controlled by the salt solutions and the corresponding temperatures..........................82
Table 4.6  The DLJ testing programme..........................................................83
Table 4.7  Failure load of the tested DLJ specimens........................................85
Table 4.8  Experimental failure loads of the EA9321/aluminium single lap joints [151]..94
Table 4.9  Experimental failure loads of the EA9321/composite single lap joints [151]..94
Table 4.10 Lay-up schemes in T-section of the joint [151]..................................98
Table 4.11 Immersion times (days) for the butt joints [122].................................101
Table 5.1  Single lap joints configuration and model details [152]........................107
Table 5.2  Comparison of the maximum strains of the adhesive at the load of 132N/mm (average over a 0.05mmx0.1mm area around the corner)....118
Table 5.3  Comparison of the maximum shear strains in the adhesive for FM73/Composite single lap joint at the loads of 105N/mm and 202N/mm......121
Table 7.1  Example of ABAQUS input file for a continuum damage model........155
Table 7.2  Strain-based failure model summary..............................................173
Table 7.3  Continuum damage model summary..............................................174
Table 9.1  Critical strain calibration results of EA9321 using the MMF test model with different mesh schemes (Dry).................................198
The advantages of adhesive bonding over traditional joining techniques have been long established. Compared with other joining techniques, such as the use of bolts and rivets, adhesive bonding can distribute load over a much wider area, reduce stress concentrations, increase fatigue and corrosion resistance of the bonded joints, as well as provide weight savings for the whole structure and the ability to join different materials. For these reasons, they have been widely used in automotive, aerospace and electronic packaging industries.

However, adhesive bonding would be used even more widely if the long-term service-life of bonded joints and components could be reliably predicted under the combined effects of an aggressive environment and mechanical loading. A commonly encountered hostile environment is exposure to moisture, often at elevated temperatures. In fact, the durability of adhesive joints to hostile environments has become the main challenge for researchers in this area.

Recent research on adhesive joints has been undertaken focusing on environmental degradation and the long-term adhesive joint performance prediction. Such work can be broadly divided into two categories: durability experiments and lifetime predictive methodologies. Many kinds of experimental techniques have been undertaken to study the durability of adhesively bonded joints. Two main types of failure, interfacial and cohesive, are commonly found for adhesive joints: failure sites are at
the adhesive/substrate interface or cohesive within the adhesive layer, respectively. Due to the difficulties in carrying out long-term experiments or accurately ranking short-term testing results, a dedicated predictive modelling methodology can be used to obtain improved life expectancy predictions of adhesively bonded structures.

Computer simulation techniques such as finite element analysis (FEA) have been employed to develop durability prediction models. So far, a number of modelling methods have been developed to predict the degraded strength and life expectancy of adhesively bonded joints exposed to hostile environments. More success has been achieved in modelling progressive interfacial failure than modelling progressive cohesive failure. However, none have proved entirely effective. The work of this project is mainly focused on such modelling with FE analysis and aims to improve the prediction of the durability of adhesive joints under environmental degradation. Both kinds of failure (interfacial and cohesive) were studied in this project but the emphasis was on modelling the cohesive failure.

A limited amount of testing has been carried out as part of this project since the modelling work needs the characteristics of the constituent materials and subsequent joint performance for validation.

1.1 Research framework and objectives

A generalised durability framework to model environmental degradation of adhesively bonded joints has been outlined by Crocombe [1], Figure 1.1. It assumed moisture as the main driving factor for strength degradation of adhesive bonded joints. The environmental performance of the adhesive joints was mainly controlled by the relative kinetics of moisture diffusion and degradation of the interface and the bulk adhesive. Whether transported through the bulk or along the interface, the moisture degraded both the adhesive and interface of the joints and thus affected the failure criteria. Modelling the long term durability of adhesively bonded joints is complex as the material properties and governing processes are inter-dependent.
A coupled diffusion-mechanical analysis has thus been developed to obtain moisture concentration for the degradation modelling of adhesive joints. Bulk diffusion of moisture through the materials was simulated by a transient heat transfer analogue analysis. The mechanical properties of adhesives and the degradation of bonded joints were all dependent on the modelled moisture concentration. A failure criterion had to be developed to determine failure of the bonded joints and this was also moisture dependent. The residual strength and damage propagation of the joints was then predicted in a subsequent stress/displacement FE analysis.

The main objective of the research reported in this thesis was to develop a progressive damage modelling approach for bulk degradation in adhesive bonded joints as well as refine the current interfacial degradation modelling approach to work in conjunction with a more realistic material model.

1.2 Research methodology and thesis structure
The methodology of the research has been designed to achieve the objectives and it will be introduced with the structure. It is worth noting that computer simulation, using finite element (FE) analysis, was applied in this research and the commercial FE package ABAQUS has been adopted in all the modelling work.

A survey of the current literature is presented in Chapter 2 following this introduction. This was carried out in order to become acquainted with the background of this research and to determine the relevance of the methods already developed by others in modelling adhesive bonded joints.

A cohesive zone model (CZM) with interfacial rupture elements has been proposed and used to model AV119 bonded lap joints. Linear elastic fracture mechanics (LEFM), together with a mixed model flexure (MMF) test had already been used to calibrate the rupture element parameters for the predictive modelling. However, this model was only used with linear substrate continuum responses. It is essential to be able to incorporate more realistic non-linear material properties and it is also necessary to assess the effect of the rupture element parameters on the non-linear analyses. Thus a modified cohesive zone model was developed and an enhanced prediction was obtained by incorporating the plasticity of the substrates. This is presented in Chapter 3.

The experimental work used to inform and validate the FE modelling in this research is covered in Chapter 4. This included characterisation of bulk specimens and mechanical testing of bonded joints at selected ageing environments. Three adhesives, FM73, EA9321 and E32, were involved in this research. The diffusion of the selected adhesives in a range of ageing environments was characterised using a gravimetric approach. Moisture dependent bulk adhesive properties were measured using standard tensile tests. Testing of residual strength on a series of environmentally degraded EA9321 bonded joints was carried out and the configurations included the mixed mode flexure (MMF) test, the thick adhered shear test (TAST), the single lap joint (SLJ) test and the composite T joint 3-point bending test. A series of E32 bonded butt joints were also tested for a range of exposure moisture. All these tests were undertaken by co-workers and the results were
used for the subsequent predictive modelling. A series of FM73 bonded aluminium - composite double lap joints, aged in two controlled environments, were prepared and tested by the author. The failure surfaces were inspected to justify the failure types. The experimental data were then used to validate the predictive modelling carried out by other researchers in the group.

Chapter 5 outlines validation FE modelling work which was carried out for the FM73 and EA9321 bonded single lap joints. The purpose of this study was to establish confidence in the moisture dependent non-linear adhesive models to be used in subsequent predictive modelling. A series of unconditioned and conditioned joints with different geometries and different substrates were studied. Two elastic-plastic materials models, von Mises and linear Drucker-Prager, were considered. Both 2D and 3D models were undertaken and compared. Predicted results were compared with experimental data obtained by a colleague at BAe systems. Useful conclusions and modelling experience were obtained.

A strain-based progressive cohesive failure model is proposed and incorporated to predict bulk degradation in EA9321 bonded single lap joints in Chapter 6. Joints bonded with the ductile adhesive EA9321 often fail in the adhesive layer rather than along the bondline interface. A range of environmental degradation in the joints was studied. A single moisture-dependent failure parameter, the critical strain, was calibrated using the MMF test and then applied to the lap joints without further modification. The mesh dependence of this parameter was also investigated. Only the von Mises plasticity model was available for this failure model. Both 2D and 3D models were considered and compared. The good correlation with experimental data has demonstrated the potential of this method in predicting both the residual strength of environmentally degraded adhesive joints and the damage initiation and propagation in adhesive layers.

Another bulk degradation modelling method, continuum damage model, is proposed and discussed in Chapter 7. It has been used to predict the response of the same EA9321 bonded joints studied in Chapter 6. A damage parameter was introduced to account for the material degradation, in terms of element displacement rather than
strain. This made the model mesh-independent. The same calibration process was carried out for this model using the MMF test. Both von Mises and linear Drucker-Prager models were considered and both 2D and 3D models were used and compared. The good predictions and the mesh-independence have demonstrated the high potential of this modelling method.

A validation analysis for reduced width moisture conditioned EA9321 bonded CFRP T joints, loaded in a 3-point bending, is presented in Chapter 8. This CFRP T joint was chosen to be representative of joint design in larger, more complex, bonded structures. Two shell elements, continuum and conventional, were used to model the laminated composite T-section of the joint and the behaviour of these elements was compared. The predicted results were compared with the experimental data and some useful conclusions were drawn.

In Chapter 9, a full width EA9321 bonded CFRP T joints, loaded in a 3-point bending, was studied using the two cohesive failure models. The mesh dependent failure parameter for the strain-based failure model was calibrated using a re-meshed MMF model whilst the mesh independent failure parameter calibrated in Chapter 7 was used without any modification for the continuum damage modelling. A reduced width FE model was created for the continuum damage modelling due to the requirement of mesh independence. Only the un-aged joint was modelled. The predicted strengths correlated well with the experimental data and the two cohesive failure models were further demonstrated and compared.

Butt joints bonded with another ductile adhesive, E32, were modelled and are discussed in Chapter 10. The continuum damage model was used. In this case, von Mises material model was found to be unsuitable and a linear Drucker-Prager model had to be used. The good correlation with the experimental data has further demonstrated the high potential of this continuum degradation modelling method.

The swelling effect of the adhesive and the composite is investigated in Chapter 11 for the EA9321 bonded joints using the strain-based failure model. The critical strains, calibrated from the MMF specimens without swelling, were used to predict
the same MMF specimen and the single lap joints with swelling. Only the longest exposure time and 3D models with the von Mises plasticity were considered. The predicted failure loads were reduced consistently with the incorporation of the swelling and this led to a reasonable expectation that these predictions with swelling can be good if the critical strains are calibrated from the MMF with swelling. The swelling of the composite substrates in the SLJ model had little effect on the predicted residual strength.

Chapter 12 is the concluding part of this thesis. Key achievements of the research and suggestions for future work are presented in this chapter. The publications that have resulted from this research are listed before the Table of Content of this thesis.
The advantages of adhesive bonding over traditional joining techniques have been well recognized. Adhesively bonded joints have been applied successfully in many technological applications, especially in the aerospace and automotive sectors. Recent research of adhesive joints has been extended to the combined effects of complex modes of loading, environmental degradation and long-term joint performance predictive modelling. It is generally agreed that environmental effects such as moisture and temperature are major contributors to adhesive joint degradation, both within the adhesive and at the interfacial regions. Modelling environmental degradation in adhesively bonded joints involves a large number of different aspects of materials, mechanics and mathematics. A literature review aiming at establishing a comprehensive background of these areas is presented in this chapter.

2.1 Durability testing of adhesively bonded joints

Durability testing of adhesively bonded joints has been carried out on a number of adhesive/adherend systems over recent decades. The adhesives studied have included acrylcs, epoxies, phenolics, polyurethanes, silicones and polyimides. The substrates
have included steels, copper-based alloys, aluminium alloys, metal matrix composites, GRP (glass reinforced plastic) composites, FRP (fibre reinforced polymer) composites and various other composite formulations. Typical testing methodologies can be categorised into three groups: comparative methods for adhesive/adherend system and process selection, quantitative methods for generating engineering property data for design purposes, and qualitative methods for assessing long-term performance of bonded systems under combined mechanical loading and hostile environments. It has been found that the durability of the bonded joints depends not only on the type of substrate and adhesive, joint configuration and loading conditions that are covered in national and international standards [2-4], but also on the surface pre-treatment, adhesive storage, specimen and machine alignment, joint assembly and the ageing environment (moisture and temperature), which have not been included in written standards.

In this section, commonly used testing methods for adhesive bonding, mechanisms of moisture degradation, and durability testing of adhesively bonded joints exposed to moisture are reviewed.

2.1.1 Mechanical testing methods used for adhesively bonded joints

Quantitative testing methods for evaluating the strength of the joints and for generating engineering property data are mainly discussed in this subsection. Such data can be interpreted with subsequent finite element (FE) modelling.

Fracture mechanics tests provide information on the growth of a crack within a material and have been applied to adhesive joints. These tests require an initial notch or pre-crack. The precise geometry of this notch will influence the results and is a source of uncertainty or variability in the tests. Notch geometry will have more effect on the initiation of crack propagation. Results from the initial part of the test are normally excluded from analyses, with critical parameters determined from the regions of steady state crack growth. Common fracture mechanics tests include the double-cantilevered beam (mode I) and end notch flexure (mode II) tests, as illustrated in Figure 2.1 [5].
Lap tests are performed when overlapped adherends are pulled in tension to generate shear and peel stresses within the adhesive layer. Single lap joint (SLJ) tests with thin adherends are the most familiar types in this class. The geometry of the lap shear joint used by Broughton et al. [6] and also in this thesis is shown in Figure 2.2. Such tests are relatively straightforward from the specimen preparation and testing perspective. Failure tends to initiate at the end of the overlap, close to one of the adherends. There are some closed-form analytical solutions for stress at the overlap ends (Loh et al. [7]). However, these tests have recognised limitations for the determination of joint design parameters (Ziane et al. [8]).

The thick adherend shear test (TAST) is another lap test and is preferred for determining design parameters as the thick, rigid adherends reduce (but not eliminate) the peel stresses. Typical joint geometry is shown in Figure 2.3 (Vaughn et al. [9]). The state of stress is predominantly shear along the overlap but there are small peel
stresses at the end.

![Figure 2.3- Thick adherend shear test (TAST) specimen](image)

The double-lap joint, as illustrated in Figure 2.4 [10], has been developed as a symmetric variant of the single-lap shear test in an attempt to eliminate the eccentric loading, responsible for bending of the adherends and rotation of the bonded region that is found large in the single-lap test. Although bending is reduced peel stresses are unavoidable, since the load is applied to the outer adherends via the adhesive, away from the neutral axis. This test removes some of the disadvantages of the single lap test but is more expensive to prepare.

![Figure 2.4- Schematic of double lap specimen](image)

The T-peel test, as illustrated in Figure 2.5, is commonly used to assess the resistance of adhesive systems to normal force peel loading [11]. The T-peel joint performance is dependant on joint materials and geometry. Most of the deformation in the test occurs in the adherends. Therefore, the thickness, stiffness and plastic yield strength of the adherend material have major influences on the test results. The adherends bend during the test, changing the stress distribution.
A butt joint test, such as the one shown in Figure 2.6, is a severe test of an adhesive as the adhesive experiences high levels of tensile and hydrostatic stress (Alwar et al. [12]). The butt-joint specimen is prepared by bonding two rods or bars of equal cross-section together end-on. The joint is pulled in tension to obtain the butt tension strength. Although appearing straightforward to perform, obtaining reliable and accurate data from this test can be challenging.

These test configurations are often used to assess the durability of adhesively bonded joints and some of them have been used in this research.

2.1.2 Mechanisms of environmental degradation in adhesively bonded joints
A hostile environment can reduce the residual strength of adhesive bonded joints in various ways. Water has been regarded by many researchers [13-17] to be a critical agent in the degradation of adhesive bonds. Moisture effects nearly all adhesive applications because water is always present in the atmosphere, readily absorbed and aggressive toward displacement of physical bonding. Thus, the aspect of the durability of adhesive joints to moisture environment has become the main challenge for researchers in this area.

Adhesives are susceptible to moisture attack because they are polymers and have hydrophilic properties that attract water molecules (Kinloch [13]). This may plasticise and induce relaxation in the adhesive as well as lead to the formation of cracks in the joints (McBrierty et al. [18]). The degradation of an adhesive system can take place with or without changing the molecular structure (Rodriguez [19]). Irreversible degradation involves permanent molecular damage such as hydrolysis (chain scission) and dissolution of the adhesive at higher moisture concentrations. However plasticisation is reversible, the effects are removed when the water molecules are eliminated [14]. There is some evidence of a critical humidity marking the onset of adhesion loss [20]. This moisture concentration may separate the reversible and irreversible degradation and it has been detected by Lefebvre et al. [21].

Due to the preference of a metal-water interface over a metal-polymer interface, the presence of water at the adhesive/substrate interface is thermodynamically feasible and this promotes clustering of water molecules at the interface that displace the adhesive from the substrate (Koehler [22]). The absorption and clustering of water causes hydrolysis of the adhesive, corrosion (oxidation) and cathodic delamination at the interface that can weaken the joint and reduce its durability (Watts [23]). Therefore, there are two main adhesive joint failure sites: at the adhesive/substrate interface and in the cohesive region within the adhesive, as illustrated in Figure 2.7 by Gledhill et al. [24], where a butt joint was loaded to failure after exposure to moisture. It has been shown that cohesive failure occurred at the centre of the bond area and the rest was interfacial failure. Workers in the adhesives field, such as Sharpe [25] and Bikierman [26], have argued that the statistical improbability of a
fracture propagating solely along a molecularly rough surface means that true interfacial failure never occurs. However, other authors [27-28] have argued that failure at the interface can be thermodynamically favoured and that the surface energies can be correlated with bond strength with polar components of the surface energies playing a critical role.

High-performance surface treatments that provide a high density of stable physical bonds (and thus are resistant to hydration) require the presence of moisture a long time before hydration of the adherend surface causes joint failure [24]. As the primary cause of environmentally induced degradation of an adhesive joint is moisture intrusion into the bondline, the detection of moisture has produced a way to detect the degradation or potential degradation of adhesive bonding before a bonded joint fails [16]. Thus an electrochemical sensor or other moisture-detecting device has been used to provide warning before irreversible structure damage occurs to the joint [28].

2.1.3 Moisture degradation experiments of adhesively bonded joints

This subsection is coarsely divided into two parts: the ageing environment and the surface treatment.

2.1.3.1 Ageing environment
In industrial laboratories, accelerated ageing tests [29] have been carried out by immersing adhesive joints in water before testing. The times to failure are measured in order to generate basic design data. A newly developed technique to solve the problem of the time required for ageing the joints uses the adhesive as a coating on one substrate rather than sandwiched between two adherends [30-31]. Such "open-faced" specimens were prepared by applying adhesive to the substrate plates and, after curing, exposing them to a range of temperatures and humidities. Then, at various times, the adhesive layer (in the "wet" state) was bonded to a second adherend to form a sandwich specimen as shown in Figure 2.8 [32]. This accelerated the moisture uptake by several orders of magnitude and the acceleration was accomplished geometrically rather than thermally.

Minford [33] obtained a basic residual shear strength of different adhesives after exposing adhesively bonded lap joints to a wet environment for extended times. This showed that the joint strength was not entirely lost as the exposure time increased. Similarly, Orman and Kerr [34] found the adhesively bonded lap joint strength reached a limiting ultimate value after a steady reduction with the increasing time of exposure to a wet environment (5%RH and 100%RH) as shown in Figure 2.9 [33]. Further experiments have found that much bond strength was recovered after drying the exposed joint in a vacuum at 90°C for 24 hours (also shown in Figure 2.9). The recovered component probably originated from the reversible effect of plasticisation whereas the non-recoverable strength is attributed to the permanent degradation of water attack such as hydrolysis of the materials. A similar phenomenon was observed.
by Gledhill et al. [35] in studying moisture degradation in butt joints exposed to severely increasing moisture environments at various temperatures as shown in Figure 2.10 [35]. The experiment also demonstrated that an increase of temperature accelerated the rate of the degradation process.

![Figure 2.9](image_url)

Figure 2.9- A moisture degradation effect of the reduced shear strength after exposure to 5%RH and 100%RH [33]

![Figure 2.10](image_url)

Figure 2.10- The effect of temperature and humidity on the failure strength obtained using butt joint after exposure to 5%RH and 100%RH [35]

In contrast to the bonding strength reduction shown from the above moisture degradation experiments, a kind of insensitivity of the joints to moisture degradation...
has also been observed in other work. Minford [33] found that the joint (etched aluminium bonded with a two part epoxy) strength passed through a maximum value then fell off as illustrated in Figure 2.11 [33]. The increase of strength was attributed to the effect of stress relief, plasticisation and swelling that countered the adverse effects of water. The strength finally reduced when the degradation was far more pronounced than the plasticisation and swelling. A similar behaviour has been observed by Cotter [36] where various patterns of degradation were found with different adhesives using double overlap joints. Gledhill et al. [35] concluded that there was certain critical water content for some adhesives, below which the bonded joint can still retain its original strength.

![Figure 2.11](image)

Figure 2.11- The joint strength increased to a maximum value before reducing during the exposure to 100% RH at 52°C [33]

2.1.3.2 Surface treatment

Moisture uptake is governed by the adhesive (and composite if present) and there is little dependence on surface treatment. However, the response of the bond to absorbed moisture is critically dependent on surface preparation.

In general, the surface treatment removes the layer of impurities on the bonding
surface, increases surface free energy to maximise intimate adhesive/substrate interaction and generates specific surface topography for intrinsic adhesion. There are many types of surface treatment available depending on the type of substrate and durability performance required [37].

Brockman [38] studied the effect of different surface treatments on steel substrates using single lap joints with two different adhesives (Tegofilm (phenolic adhesive) and FM123/5 (epoxy adhesive)) after exposure to the natural climate in North Germany and to an artificial ageing climate (30°C, 95%RH) for one year. It was found that the phenolic bonded joints showed no interfacial failure for shotblasted steel substrates but clear interfacial failure, with corrosion at edges, was noted for substrates that were degreased only. The epoxy bonded joints showed small areas of interfacial failure for shotblasted steel but pure interfacial failure for the degreased substrate.

Different types of grit blasting material and grit size were used to treat the mild steel and aluminium alloy substrates. These affected the durability of lap shear joints and tensile butt joints in hostile environment. Harris and Beevers [39] found that the initial dry strength was relatively independent of grit size in lap shear joints and showed 100% interfacial failure for all cases. However, the butt joint showed increasing interface failure from 30% to 70% by area after immersion in de-ionised water at 60°C for 12 weeks.

Knox and Cowling [40] investigated AV119 bonded thick adherend lap shear joints and strap joints aged in 100%RH at 30°C to distinguish the durability performance of various surface pre-treatments. The surface pre-treatments considered were the silanes (A187 and SiP) and the corrosion inhibitors (Albritect and Accomet-C). The results showed that the silane primers increased the durability performance of the joint more than the corrosion inhibitors. This is because the use of primer on well prepared surfaces increases the stability of the adhesive and adherend interface against the attack of water.

Brewis et al. [41] have showed the effect of substrate surface treatment on the epoxy
adhesive bonded aluminium joints exposed to water. The results showed that the strength reduced progressively with increasing water uptake when the aluminium was etched and degreased. This was attributed to the hydrolytic instability of the weak oxide layer formed on the aluminium. However, the strength of the joint increased with increasing exposure time when the aluminium substrate was anodised. The increase in strength was attributed to the plasticisation of the adhesive together with the simultaneous process of relaxation of residual stress.

The ageing temperature is also important in the degradation of the adhesively bonded joints with various surface treatments. An epoxy-bonded aluminium alloy was pre-treated in various ways and tested by Bowditch [42] after exposure to water immersion. It was found that 50°C/water environment could not discriminate the effect of surface treatment. However, when repeated at 40°C, complete discrimination emerged with the phosphoric acid anodising (PAA) process showing superior durability. At the lower temperature, the failure was shown to be near interface, whilst at the higher temperature, the failures were exclusively cohesive due to the degradation in the adhesive.

As summarised in the work by Beevers [43], surface treatment plays an important role in obtaining good durability of adhesively bonded joints against water attack. Environmental moisture, temperature and exposure time controls the degradation rate in both the cohesive and the interface regions. Different types of adhesives show different levels of hydrophilicity, which also affect the rate of degradation. Plasticisation of adhesive and relaxation of internal stress may increase the joint strength provided that the critical value of water content in the adhesive is not reached. Otherwise, further strength reduction is expected. In certain circumstances, the joint strength is unaffected by degradation if both plasticisation and interfacial weakening are balanced below a certain value of water content.

2.2 Modelling environmental degradation in adhesively bonded joints
In order to achieve an efficient design of an adhesive joint, the stress, deformation mechanisms and failure modes of the joints need to be well known. The failure mode of an adhesively bonded joint can be categorised mainly into two types: adherend-adhesive interface failure and cohesive failure (entirely within the adhesive). The prediction of failure in an adhesively bonded joint is of great importance in the use of bonding for structural applications. Both interfacial failure and cohesive failure predictive modelling were studied in this research whilst simulating and predicting cohesive failure in ductile adhesives was focused. Several kinds of approaches in this area have been developed to model joint failure and have achieved different degrees of success. These approaches can be classified as the strength of materials method, the fracture mechanics method and the continuum damage modelling method.

2.2.1 Strength of materials method

This method is based on the strength of the materials, in which, the stress or strain distribution in an adhesive joint is examined, and the joint failure is assumed to occur when the predicted stress or strain field exceeds a critical value. Adams and his co-workers [44-46] are renowned for their work in this classic approach. Generally, they use a plane strain, geometric and material nonlinear finite element (FE) analysis with either maximum principal stress, maximum principal strain or plastic energy density as the failure criterion at a point in the model to predict joint failure. This method is also used by a number of other workers with a variety of failure criteria to predict cohesive failure in the ductile adhesive [47-49] due to its ease of use.

However, this simple method may be insufficient to predict the entire failure process of an adhesive joint, and the failure criterion is often applied at an element integration point adjacent to a singular point. The predicted strains near the end of the joint will therefore depend heavily on the refinement of the FE mesh. To overcome the FE singularity problem, some workers have proposed stress singularity parameters for the end of the joint to predict cohesive failure [50-52]. In reality true singular points probably do not exist in practice due to the fact that the corners at the ends of the joint will not be perfectly square. In addition, the stresses at the ends of
Chapter 2 Literature review

the joint will be relieved due to zones of local damage which can take the form of voids, local crazing, local cracking, etc.

Towse et al. [53] have attempted to deal with the problem using two kinds of techniques. The first uses stresses at "characteristic distances" from the singularity, and the difficulty then becomes the determination of such characteristic distances. Another relevant issue is the "in situ" nature of the adhesive strength. In lap joints only a very small volume of material is highly strained. Scale effects have been reported in an early review by Harter [54], and therefore the relevance of strength values measured using thicker bulk specimens can be questioned. The second technique used by Towse et al. [53] consisted in assuming rounded adherend corners, and it has been shown that the prediction depended on the exact corner geometry assumed. A failure criterion incorporating simultaneously scale effects via Weibull statistics was therefore proposed, in which localised rounding was adopted to remove singularities at the interface as shown in Figure 2.12 [53]. Three- or two-parameter Weibull probability density functions can be chosen to fit the experimental data and then implemented into the failure criterion. The analysis of a simplified lap joint model has demonstrated the Weibull method is less sensitive to the exact local geometry than the common methods of either stress at a distance criterion or other strength of materials-based models.

Figure 2.12 - Model used to investigate singularity strength in Weibull method [53]
As an alternative, Crocombe [1] proposed a global yielding failure criterion of the adhesive layer, based on the assumption that failure would occur when the adhesive layer had completely yielded. When the path of the adhesive through the overlap region reaches a state in which the adhesive layer can no longer sustain further significant increase in applied load, the joint fails. This criterion avoids many of the inherent problems with joint strength predictions. However, this approach is not conservative for most joints of cleavage-type, in which local failure could occur before global yielding.

Another kind of strength of materials-based method is the damaged zone model (DZM) [55]. It has been known that adhesively bonded joints generally do not contain sharp macroscopic cracks [56]. Failure of these joints will initiate from a zone of damaged material. Therefore, these structures are primarily crack initiation controlled and the prediction of the load at initiation is important. Damaged zone models based on a critical damaged zone and strain-based failure criterion have been proposed to predict the initiation of failure and ultimate failure loads of adhesively bonded joints [57-59]. In recent studies by Apalak et al. [60], a modified von Mises criterion for the adherends and a modified failure criterion including the effects of the hydrostatic stress for the ductile adhesive were used to determine the damaged adhesive and adherend zones where the strain exceeded the specified critical strains. The stiffness of all finite elements corresponding to these zones was reduced to a negligible value so that they could not contribute to the overall stiffness of the adhesive joint.

The procedure to predict the critical failure load using a damage zone model can be outlined as the following (Sheppard et al. [57]):

a) Test one or more adhesively bonded joint(s) and record the load at which a crack initiates at the end of the joint, and the failure mode.

b) Analyse the joint(s) at the experimental crack initiation load using an appropriate analysis tool.

c) Using an appropriate failure criterion and the relevant material allowable(s) calculate the critical damage zone size in the region in which failure was observed in the experiment.
b) Use the critical damage zone size calculated in the previous step to predict the critical load of bonded joints with similar adherends, adhesives and load paths.

To characterise the damage zones for different adhesively bonded joints, a number of relevant tests are required. A very fine mesh needs to be used to study the crack propagation in FE models as shown in Figure 2.13 [60]. This model is applicable to detect crack initiation while traditional fracture mechanics method (LEFM) should be used for crack growth.

![Figure 2.13- Mesh details of square adhesive free ends of an adhesively-bonded single-lap joint using DZM [60]](image)

### 2.2.2 Fracture mechanics method

To overcome the problems of the strength of materials-based methods, fracture energy-based methods have been developed to predict the failure of the joints. In this method, the joint failure is assumed to occur when the predicted crack growth driving force exceeds the measured fracture resistance. The fracture mechanics method often assumes a pre-existent crack and determines if the condition in the structure are suitable for crack growth and failure generally. This is achieved through the use of the strain energy release rate (SERR) measured through the propagation of a crack through a test specimen. Many models are two-dimensional and thus only consider modes I and II.
Considerable successes have been reported in some applications of the fracture mechanics-based criteria to adhesively bonded joints, even for failure prediction of initially uncracked joints when the failure process involved the formation of a crack in the adhesive layer, followed by some propagation under increasing loads [61-62]. Kinloch [14] has made an important contribution regarding the application of this approach to adhesively bonded joint problems. But this model is only suitable for joints where the adherends remain elastic deformation during loading and requires a detailed representation of the crack-tip region.

To determine strain energy release rates, Rybicki and Kanninen [63] used a method based on Irwin [64]'s virtual crack closure to present a simple formula for 2D cracked, isotropic domain problems. The formula was to obtain the tripping force easily from a single finite element analysis and then was extended to formulate the SERR from non-singular and singular elements of any order.

A modified crack closure integral method with square-root stress singularity elements is used for the calculation of strain energy release rates for an in-plane extension of a crack in a bi-material problem. The same approach was used by Sun and Jih [65] to calculate mode I and mode II strain energy release rates for a crack lying along the interface of two dissimilar elastic media. The analytical solutions indicate that $G_I$ and $G_{II}$ do not converge in the form of crack closure integrals although the sum (the total strain energy release rate) is well defined.

The formulation of Rybicki and Kanninen was also used by Jurf et al. [66] for the calculation of the SERR of a TAST joint as a function of temperature. The joint was modelled under plane strain conditions with four node quadrilateral isoparametric elements. The results agreed with the experimental observation, giving catastrophic failure. Wahab [67] presented a design tool for a single lap joint using a failure criterion for the SERR based on virtual crack closure. An important relationship between the lap length and the adherend thickness was derived to design a reliable joint. Other analytical and finite-element models have been developed to describe the variation of the SERR with crack length as a function of the applied fatigue loads for
the single-lap joint and the "top-hat" box-beam joint as reviewed by Curley et al. [68].

A cohesive zone model (CZM) was first proposed by Barenblatt [69] in 1960s. The early studies of ductile failure mechanisms by McClintock [70] first recognised the key role of void formation in cohesive failure processes. A continuum damage model, based on the CZM, for void nucleation by inclusion debonding has been developed by Needleman [71]. This sought to describe the evolution from initial debonding through complete separation as well as subsequent void growth. The relation between the crack growth and the joint resistance in elastic-plastic materials for fracture process was modelled with a traction-separation law proposed by Tvergaard and Hutchinson [72-73]. The microscopic fracture processes of the adhesive and the macroscopic non-linear deformations of the adherends were analysed independently and then linked together through a traction-separation law for the local decohesion processes to express the overall behaviour, as shown in Figure 2.14 [73].

The cohesive zone model (CZM) was also called an embedded-process zone (EPZ) model by Kafkalidis et al. [74-75] and Yang et al. [76]. It was used to investigate the mode I cohesive parameters for plastically deforming adhesive bonded joints [77]. It was shown that these systems provided examples where the cohesive tractions exerted by an adhesive layer can be calculated simply from considerations of the
constrained deformation of the adhesive. It was then used to study the elastic-plastic mode II fracture [78] and mixed mode fracture [79] of adhesive joints. The fracture parameters for the traction-separation law were determined by comparing the numerical and experimental results for one particular geometry of adhesively bonded specimens (ENF), and then used without further modification for joints with different geometries.

The effects of geometry and material properties on the fracture of adhesive joints have been studied. A review of the mechanics of single lap joints was followed by a detailed analysis using a cohesive zone modelling approach in the work of Kafkalidis et al. [74]. The cohesive zone model allowed not only the influence of geometry to be considered, but also the cohesive properties of the interface and plastic deformation of the adherends to be included. It was demonstrated by Kafkalidis et al [75] that the energy absorbed by the adhesive layer was essentially independent of the geometry, owing to a compensating effect in which the critical displacement for failure varies with the constraint.

An interfacial rupture element based on a CZM has been proposed to represent the process of failure initiation and propagation within both elastic and plastic deformation in materials by Yang et al. [76]. The strain tripped rupture element with time based unloading gave good predictions when using a single set of failure parameters for a cleavage test under various mixed mode loadings. The two fracture parameters: fracture energy and tripping strain, were characterised and calibrated from mixed mode flexure (MMF) testing and finite element analysis. Another fracture parameter, tripping traction, was studied and characterised further by Yang et al. [77], together with fracture energy based on extensive plastic deformation testing. A traction-separation relation was used to simulate failure of the interface for both crack initiation and steady-state crack growth conditions.

In cohesive zone models, however, some parameters for the traction-separation law can only be determined through phenomenological calibration. This is due to the current limits of micromechanics in describing the local fracture processes. Moreover, most of the successful applications of this method so far are undertaken for
interfacial failure of adhesive joints such as reviewed by Cavalli et al. [78]. The assumed interfacial crack path may be not true.

It is worth noting that Du et al. [79] have recently proposed an approach to predict the strength of joints bonded by structural adhesives using a combination of a strength of materials method and a cohesive zone model method. The failure of the joints was assumed to be governed by a maximum failure strain or stress criterion. The nominal strains in the adhesive bond were evaluated by monitoring the displacements of the nodes pairs situated along the two interfaces between the adhesive and the adherends, and then compared with the measured strain at break for the adhesive to determine the failure. This approach minimised the effect of the singularity found in the strength of materials method as well as predicting the cohesive failure in the single lap joint either in the adhesive or the adherends.

2.2.3 Continuum damage modelling method

To investigate ductile failure in metals, three kinds of approaches based on ductile material constitutive equations have been developed: the virtual internal bond (VIB) model, the porosity-based Gurson model and the continuum damage mechanics (CDM) model. These three approaches have recently attracted the attention of a number of researchers, mainly because of the range of practical applications where traditional fracture mechanics concepts have reached their limitations.

2.2.3.1 Virtual internal bond (VIB) model

In contrast to the cohesive zone model described above, in which the surfaces lying between element boundaries must be defined a priori and separate cohesive elements must be introduced on the interface, the Virtual Internal Bond (VIB) model has recently been proposed by Gao and his co-workers [80-83], wherein the constitutive model directly incorporates a cohesive type law without a presumed separate fracture criterion as illustrated in Figure 2.15 [82].
In this model, the continuum nature of the materials is treated as a random network of material points which are interconnected to each other by a number of bonds controlled by a cohesive law. The bonds are physically described by a bond energy related to the bond length, and its derivative with respect to the bond length is the cohesive bonding force. Since the work of fracture has already been included in the constitutive model via a statistical average of the internal cohesive bonds, a presumed fracture criterion is no longer needed. A hyperelastic framework to describe this bond energy in finite deformation has been introduced by Gao and Klein [82], so that the appropriate stress and strain measures, such as the Green-Lagrange strain tensor and the 2nd Piola-Kirchoff stress tensor, can be derived. The macroscopic description of the continuum damage process is determined by equating the macroscopic strain energy function at the continuum level to the potential energy stored in the cohesive bonds at the micro-scale.

Klein and Gao [83] have described the application of the VIB model to fracture initiation and propagation of an adhesively bonded cantilever joint via a statistically
averaged Cauchy-Born rule of crystal elasticity. It has been pointed out that this method differs from an atomistic model in that a phenomenological "cohesive force law" is assumed to act between "material particles" which are not necessarily atoms. However, the most accurate solution obtainable using a VIB material model occurs when the element sizes, and even the element shapes, correspond directly with the underlying microstructure of the actual material. Zhang et al. [84] presented a numerical algorithm using the implicit integration scheme for the VIB model under static loading cases, and implemented the material model in ABAQUS (a commercial FE program package from HKS Inc.) through the UMAT subroutine. Thiagarajan et al. [85] have found that explicit integration schemes avoid difficulties arising from the loss of ellipticity of the governing equations due to the stabilising effects of the mass matrix in the finite element (FE) implementation of the VIB model.

The applications of the VIB model so far have been limited in the domain of elastic deformation in the materials. Most recently, an approach to incorporate plastic deformation into the VIB model has been presented and discussed by Thiagarajan et al. [86]. The incorporation of plasticity/viscoplasticity at the continuum level is done within the framework of the multiplicative decomposition of the deformation gradient $F = F^e F^p$ proposed by Lee [87]. The fracture simulation of a ductile material is studied by treating it as an elasto-viscoplastic solid. However, one main difficulty of the VIB model is still that the model appears less suitable for the simulation of fracture in large scale structures than in small scale ones due to the presence of strains above levels typically found in macroscopically sized structures. This means the contribution to the work to fracture in these structures becomes size and geometry dependent. Another difficulty is that ill-posedness in the elliptic region implies very severe instability of the discrete system and numerical methods can fail due to such instability.

2.2.3.2 Gurson-based model

The Gurson model [88] is one of the micro-void damage accumulation models [89-92] used to study cohesive failure of ductile adhesive joints in a porosity-based
way. It was proposed to model ductile fracture by considering the growth of a single void in an ideal elastoplastic matrix, as shown in Figure 2.16 [89]. An essential feature of this material model is that a failure criterion is directly built into the constitutive equations, and thus, when the void volume fraction $f$ approaches a critical value $f_c$, the material locally loses its stress carrying capacity. In order to account for the effects of void nucleation and coalescence observed in the experiment, the original Gurson model was modified and extended into a semi-phenomenological form by Needleman and his co-workers [93-96]. This modified approach has been further extended and used to model ductile failure in various situations in recent years [97-98], including isotropic and kinematic hardening, large inelastic deformation, as well as dynamic loading conditions and histories.

![Figure 2.16 - Sketch of the RVE for porous material in the Gurson model](image)

Most recently, the local nature of this material model was noticed and studied [99-101]. As has been shown by Tvergaard et al. [99], the use of energy minimisation together with energy relaxation techniques in the local model can result in mesh-independence of the macroscopic simulation results, but not in shear-band simulations. A number of non-local extensions of Gurson-based local models have been proposed to minimise the localisation of the damage process at a material point,
Chapter 2 Literature review

as well as the finite element mesh effects caused by such local models. Most such models have been either of the so-called integral- or gradient-type modified Gurson models, in which the evolution of the void volume fraction $f$ at a material point was altered to include its inhomogeneity in a finite neighbourhood of this point. Reusch et al. [101] proposed another kind of approach to extend the local Gurson model to the non-local formulation, by introducing the additional premise that void coalescence is influenced by the interaction between neighboring Gurson representative volume elements. This is intrinsically non-local, while retaining the local model function as well as the direct interpretation of the void volume fraction $f$.

The Gurson model in its general form requires a large number of parameters to be determined. Some of them can be estimated on the basis of metallurgical observations as for the case of the volume fraction of voids associated with the nucleated particles [95]. The remaining porosity parameters have to be determined with the help of numerical and experimental procedures. A common technique is using a smooth tensile cylindrical bar to identify the choice of values that best fits the load-displacement curve in the region of the sudden load drop at which rupture occurs with finite element simulations, as shown by Bonora [102]. An alternative to the tensile specimen geometry, compact tension fracture resistance data have been used to calibrate the Gurson parameters, but the identification using these geometries requires a model capable of quantifying the effects of triaxiality on porosity evolution.

However, even the minimum number of independent experimental tests necessary to identify the parameters for Gurson models becomes very large for a modified constitutive model. It is known [102] that the experimental observations give only a range of possible values for some of the parameters but do not clearly indicate a reliable procedure for determination. Moreover, the parameters calibrated are not all directly related to physical quantities even though they could describe the effects related to the evolution of the microcavities. Gao et al. [103] have pointed out the difficulties in extending a Gurson model parameter set, and the impossibility of providing a general procedural identification scheme from one set of experiments to other geometries. Finally, it is important to mention that the Gurson models
illustrated in this context experience mesh and scale effects. Some non-local Gurson methods have been proposed to solve this problem but have made the calibration procedures even more complicated.

2.2.3.3 Continuum damage mechanics (CDM) model

As an alternative to the above porosity-based approach, Lemaitre [104] and Lemaitre and Chaboche [105] proposed a constitutive framework for ductile failure processes known as continuum damage mechanics (CDM). The irreversible phenomena that take place in the material under plastic deformation are described through thermodynamics variables. This model differs from the porosity-based Gurson model because in the CDM model, damage is one of the state variables, and this variable takes into account the microstructural changes, such as void formation and growth, on the macroscopic material properties. The evolution of this damage parameter is given by a state equation of associated variables [106]. In fact, in this framework, the way in which a single void evolves or how many voids coalesce while others nucleate, are not important. The damaging process that occurs in a reference volume element (RVE) (shown in Figure 2.17 [106]) while plastic deformation takes place are described by the global effects that all damage phenomena have on the RVE constitutive response. Compared with the Gurson model, which is based on the micro-mechanisms, the CDM model is phenomenological.

![Figure 2.17- Elementary volume element. (Definition of the net resisting area and the effective section for a damaged material.) [106]](image-url)
A physical definition of the damage variable can be given by considering that the presence of a damage state in the RVE reduces the effective nominal area as the following scalar expression, assuming isotropic damage indicates:

\[ D = 1 - \frac{A_{\text{eff}}}{A_0} \]  \hspace{1cm} (2.1)

where \( A_0 \) is the nominal section area of an RVE and \( A_{\text{eff}} \) as the effective resisting section area, reduced by damage. Damage phenomena are localised on the material microscale and their effects remain confined until the complete failure of several RVEs occurs, producing a macroscopic crack. The same set of constitutive equations that is used to describe the virgin material behaviour on the macroscale is used for the damaged material; the stress is replaced by the effective stress and a state equation for the damage variable has to be given. Damage affects only stresses; the total strains are the same on both the macroscale and the microscale [107].

Similar to the Gurson models, the damage parameters of the CDM model have to be calibrated using a combination of experiments before implementation into the FE simulation. However, the number of the parameters is generally much smaller than those required by a Gurson model. It is also worth noting that, in the CDM model, all parameters have a physical meaning and can be experimentally determined without the need for iterative calculations. In addition, material damage parameters can be measured using a simple uniaxial test. A commonly used technique is an hourglass-shaped tensile testing specimen (as shown in Figure 2.18 [105]) which allows the user to know in advance where failure will occur and the localisation in the material microstructure take place. However, the CDM model is a local model and it has also a disadvantage that it does not account for the size and geometry effect. This limits its use as a general continuum damage modelling method.
2.2.3.4 Other continuum modelling methods

Some other methods such as micro-mechanics have been proposed to model the continuum damage of structures but have not been as widely used for adhesively bonded joints as the previous methods. Several micro-crack based damage models have been presented to describe the elastic damage process. Compared with the continuum damage modelling methods such as the Gurson model, in which its effective constitutive relation at macro-level differs from the constitutive relation at micro-level, the basis of contemporary micro-mechanics aims to discover unknown but important constitutive information by homogenising simple but massive micro-mechanics objects [108]. Ju and his co-workers [109-110] have applied a micro-mechanics technique to model effective elastoplastic behaviour of a composite with distributed inhomogeneities.

Most recently, a micro-mechanics damage model has been proposed by Li and Wang [111], based on homogenisation of penny-shaped cohesive micro-cracks in a three dimensional reference volume element (RVE). One of the distinguished features of the new damage model is that it includes the reversible part of the effective constitutive relation, whereas the irreversible part of effective constitutive relation is a form of pressure sensitive plasticity, both of which are significantly different from material's
behaviour at micro-level before homogenisation. In this model, when the ratio of macro-hydrostatic stress and the true yield stress reaches a critical value, the cohesive damage model will predict a complete failure of material even if the amount of damage is infinitesimal. Moreover, the effective yield surfaces as well as damage evolution equations depend on the Poisson ratio of the material and the rate of damage accumulation may depend on the rate of elastic deformation. In the Gurson model, no such dependence can be predicted due to the assumption of an incompressible RVE.

2.2.4 Modelling environmental degradation of adhesively bonded joints

To predict long-term performance of adhesively bonded joints, one approach is to develop accelerated testing techniques which use some relatively short-term experiment to characterise the long-term response. Another approach is to use predictive modelling methods that can represent the long-term performance of the joints in terms of known characteristics of different components of the joints. To accurately model environmental degradation of adhesively bonded joints, quantitative calibration and modelling of moisture diffusion in adhesives and adhesive joints, and experimental characterisation of the environmentally degraded materials are necessary. These can be used directly in the corresponding finite element modelling.

2.2.4.1 Moisture diffusion characterisation and FE modelling

To quantify moisture diffusion in adhesives, the most common technique used is gravimetric measurement. This method requires manual intervention and removal from the environment for weighing at predefined time intervals to give the total change in weight of the sample due to moisture diffusion. Shen and Springer [112] have demonstrated that this technique is a very useful experiment procedure. The bulk diffusion is assumed as the primary transport of moisture into the joint.

The analytical solutions of moisture diffusion have been developed based on Fick's law [113] and are widely used to describe moisture absorption in the adhesive. However, it is now well known that Fick's Law is sometimes inadequate for
describing penetrant diffusion in polymers or polymer composites. Non-Fickian or anomalous diffusion can occur when the rates of diffusion and viscoelastic relaxation in a polymer are comparable, and the ambient temperature is below the glass transition temperature (Tg) of the polymer. As a result, it is necessary to take into account the combined effects of temperature, stress (or strain), and damage in the construction of such a model. However, the most important factors that influence the diffusion characterisation are moisture content, temperature and type of adhesive.

**Moisture diffusion characterisation**

Brewis and his co-workers have done much work in this area [114-117]. They characterised the moisture absorption of DGEBA-DAB epoxy adhesive exposed to a range of relative humidities at 50°C [114] and another six different epoxides made mainly of diglycidylether of bisphenol A (DGEBA) exposed to water at 25°C, 45°C and 70°C [115]. It was found that all moisture uptakes for all the adhesives were Fickian, and the diffusion coefficients and saturation levels increased with relative humidity. The equilibrium uptake of each individual adhesive was similar for all temperatures considered. Following the above study, another work [116] showed that diffusion coefficient was independent of moisture concentration of exposure but the equilibrium uptake increased with moisture concentration of the exposed environment.

The water uptake of two nitrile-phenolic (NP1 and NP2) and vinyl-phenolic (VP) film adhesives were also studied [117]. Fickian diffusion was observed when NP2 and VP were exposed to water at 50°C. It was shown that the NP1 adhesive absorbed water to attain a maximum weight and then slowly decreased in weight until a steady value was reached for the same ageing environments. When the same specimen was dried and the uptake was repeated, it became Fickian.

Wright [118] studied two different epoxy resins MY750 and 5208 and found the equilibrium moisture uptake increased with relative humidity but the rate of increase was different. The diffusion coefficient also increased with temperature, especially
above 50°C and 60°C. Zhou et al. [119] investigated the effect of moisture absorption on a range of adhesives (TGDDM-DDS, DGEBA-MPDA, Fiberite 934) at temperatures of 45°C, 60°C, 75°C and 90°C. All showed Fickian diffusion. The saturation levels of these adhesives were not affected by the temperature.

Not all moisture diffusions in adhesives can be fitted with a single constant diffusion coefficient Fickian model. Roy et al. [120] exposed both epoxy and urethane adhesives to salt solution and brake oil respectively over a range of temperatures, and found the experimental data was best fitted with a non-Fickian model with a time varying diffusion coefficient. De Neve and Shanahan [121] observed a dual stage uptake when exposing the DGEBA / Permabond ESP470 epoxy adhesive to water and 100% RH at 70°C. The first stage uptake showed a linear Fickian response. However, the second uptake stage was due to hydrothermal ageing of the adhesive, resulting in changes to its mechanical properties through plasticisation.

Hambly [122] investigated the uptake characteristic of thin (0.4mm) film adhesives of Permabond E32 and Ciba AV119 submerged in water at 22°C and 55°C. A single Fickian model failed to reproduce the experimental data. However, a dual stage Fickian diffusion model, which consisted of two separate single Fickian model with an evaporative boundary effect [113], was shown to model the anomalous uptake well for both adhesives. Loh [123] measured the moisture uptake of Ciba AV119 of different thicknesses (0.4mm, 0.8mm, 2mm) in three ageing environments: 81.2%RH, 95.8%RH and water. The results showed that the saturation level increased with increasing relative humidity. A dual stage Fickian model, as a combination of two simple single Fickian models as shown in Figure 2.19 [123], was proposed and has given an excellent fit to the experimental results for all ageing environments and thicknesses (using different diffusion parameters). It was also found that the uptake of the thin specimen behaves more like the dual stage model whereas the thick bulk specimen tends to show a single stage response.
A different moisture uptake behaviour was observed in adhesive joints that in the bulk adhesives. Dawson [124] studied the diffusion into AV119/steel joints in water at 60°C using gravimetric measurement and dielectric measurement techniques. It was found that diffusion coefficient in the joint $6.7 \times 10^{-12} \text{ m}^2/\text{s}$ was significantly higher compared to bulk diffusion coefficient $6.4 \times 10^{-13} \text{ m}^2/\text{s}$. This has been attributed to the capillary diffusion along the interface of the adhesive and the substrate. Similar conclusions were made by Zanni-Deffarges et al. [125]. The bulk diffusion coefficient for a modified epoxy resin based on DGEBA and TGMDA was $1.4 \times 10^{-12} \text{ m}^2/\text{s}$ whilst the bonded joint diffusion gave a value of $5.3 \times 10^{-12} \text{ m}^2/\text{s}$ for the environment of 100%RH at 70°C.

A list of diffusion parameters for a range of adhesives obtained from the reviewed literatures is summarised in Table 2.1.
Table 2.1 Moisture diffusion parameters obtained from a range of adhesives and exposure environments

<table>
<thead>
<tr>
<th>Ref.</th>
<th>Adhesive</th>
<th>%RH</th>
<th>°C</th>
<th>Saturation</th>
<th>$D \times 10^{-13} \text{ m}^2/\text{s}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>[114]</td>
<td>DGEBA-DAB</td>
<td>23/100</td>
<td>50</td>
<td>0.54/2.1</td>
<td>7.2/14</td>
</tr>
<tr>
<td>[115]</td>
<td>DGEBA-DAB</td>
<td>Water</td>
<td>25/45/70</td>
<td>2.3/3.2/1.9</td>
<td>1.9/13/49</td>
</tr>
<tr>
<td></td>
<td>DGEBA-DDM</td>
<td>Water</td>
<td>25/45/70</td>
<td>4.1/1.4/4.04</td>
<td>0.1/2.1/20</td>
</tr>
<tr>
<td></td>
<td>DGEBA-DMP</td>
<td>Water</td>
<td>25/45/70</td>
<td>4.4/4.0/3.89</td>
<td>2.0/21/380</td>
</tr>
<tr>
<td></td>
<td>DGEBA-DAPEE</td>
<td>Water</td>
<td>25/45/70</td>
<td>5.0/4.2/4.9</td>
<td>1.3/36/500</td>
</tr>
<tr>
<td></td>
<td>DGEBA-TETA</td>
<td>Water</td>
<td>25/45/70</td>
<td>3.8/3.2/3.89</td>
<td>1.6/4.5/170</td>
</tr>
<tr>
<td>[117]</td>
<td>NP1</td>
<td>Water</td>
<td>25/50</td>
<td>1.5/4.5</td>
<td>33/47</td>
</tr>
<tr>
<td></td>
<td>NP2</td>
<td>Water</td>
<td>25/50</td>
<td>2.38/1.72</td>
<td>16/32</td>
</tr>
<tr>
<td></td>
<td>VP</td>
<td>Water</td>
<td>25/50</td>
<td>3.5/8.6</td>
<td>18/23</td>
</tr>
<tr>
<td>[118]</td>
<td>MY750/5208</td>
<td>100</td>
<td>0.2/90</td>
<td>-</td>
<td>0.3-63/0-20</td>
</tr>
<tr>
<td>[119]</td>
<td>TGDDM-DDS</td>
<td>Water</td>
<td>45/75/90</td>
<td>6.8</td>
<td>3.13/11.5/23.5</td>
</tr>
<tr>
<td></td>
<td>DGEBA-MPDA</td>
<td>Water</td>
<td>45/75/90</td>
<td>3.35</td>
<td>3.35/13.5/31.4</td>
</tr>
<tr>
<td></td>
<td>Fiberite 934</td>
<td>Water</td>
<td>45/75/90</td>
<td>6.95</td>
<td>2.0/8.96/13.4</td>
</tr>
<tr>
<td>[121]</td>
<td>DGEBA/ESP470</td>
<td>100</td>
<td>70</td>
<td>1.5</td>
<td>2.6</td>
</tr>
<tr>
<td>[122]</td>
<td>E32</td>
<td>Water</td>
<td>22</td>
<td>9.7</td>
<td>0.16</td>
</tr>
<tr>
<td></td>
<td>AV119</td>
<td>Water</td>
<td>22/55</td>
<td>5.1/6.6</td>
<td>0.29/1.3</td>
</tr>
<tr>
<td>[123]</td>
<td>AV119</td>
<td>81/96/water</td>
<td>50</td>
<td>3.1/5.0/7.6</td>
<td>3.85/2.83/1.9</td>
</tr>
<tr>
<td>[124]</td>
<td>AV119</td>
<td>Water</td>
<td>60</td>
<td>-</td>
<td>6.4</td>
</tr>
<tr>
<td>[125]</td>
<td>DGEBA-TGMDA</td>
<td>100</td>
<td>70</td>
<td>-</td>
<td>14</td>
</tr>
</tbody>
</table>

Moisture diffusion FE modelling

The modelling of non-linear, isothermal moisture flow in porous media without hysteresis was considered by Arfvidsson et al. [126]. Different formulations based on different potentials for the Fickian moisture flow were compared. Kirchhoff's flow potential, i.e. the integral of any state-dependent moisture flow coefficient, was introduced. Only the relation between moisture content and Kirchhoff's potential is used in the internal process and the moisture flow coefficient is identically equal to unity. The use of Kirchhoff's potential considerably simplifies the numerical calculation.

A simple yet robust methodology has been proposed by Roy et al. [127], based on irreversible thermodynamics applied within the framework of composite macro-mechanics. This allowed the characterisation of non-Fickian diffusion
coefficients from penetrant weight gain data for a polymer below its Tg. Reduced absorption plots are used to verify the thickness independence of the diffusivity data.

Based on the experimental work and characterisation discussed above, the moisture diffusion parameters of adhesives can be determined and then used in finite element modelling of adhesive bonded joints. This allows coupled diffusion-mechanical analyses to be undertaken. Analytical solutions of moisture distribution as a function of time in homogenous materials exposed to environmental degradation are also available [14].

The FE diffusion analysis results and a one-dimensional analytical solution in a single lap joint have been compared by Crocombe [1]. There was good agreement between the two solutions. This agreement was also shown by Broughton and Hinopoulos [128] using a similar FE moisture diffusion model compared with 1D linear Fickian analytical solutions. The moisture distribution in a T-peel specimen was also modelled by Hinopoulos and Broughton [11]. A higher value of diffusion coefficient was used instead of the value from bulk diffusion samples. Similarly, Loh [123] modelled the moisture diffusion of AV119 bonded single lap joints (SLJ) using single Fickian model with higher diffusion rates \((1.9 - 9.5 \times 10^{-12}\, \text{m}^2/\text{s})\) assumed at the interface. The experimental characterised diffusion coefficient from the bulk adhesive (2.0mm) was \(1.9 \times 10^{-13}\, \text{m}^2/\text{s}\).

Dual stage Fickian diffusion parameters were determined and used to model the moisture uptake of E32 bonded butt joints by Hambly [122]. Both the diffusion and evaporative process in moisture uptake were modelled using the heat conduction and convection process in heat transfer analysis respectively. An eighth of the adhesive layer was modelled in three dimensions and the moisture diffused through the exposed edges. A fine mesh was generated at regions of high moisture concentration profiles. It was found that dual stage uptake experimental observations cannot be modelled using simple Fickian diffusion model. A modified uptake model with variable diffusion coefficient or gradual boundary equilibrium conditions can be used to model the uptake characteristic. Hydrothermal and stress conditions may change the uptake characteristics of an adhesive.
2.2.4.2 Moisture dependent mechanical properties

Moisture dependent mechanical properties are necessary for coupled diffusion-mechanical analysis that provides the predictive models for environmental degradation of adhesive joints. The properties include shear modulus, tensile modulus, Poisson’s ratio, yield and ultimate stress of the adhesives. These are usually determined from testing of bulk adhesives as well as bonded joints, following exposure to various controlled ageing environments. Interface fracture energy that may be dependent on the materials and surface treatments is characterised by fracture mechanical testing.

Tensile tests of DGEBA/HY 959 epoxy adhesive using dogbone specimens were undertaken by Gledhill and Kinloch [24]. The specimens were tested after exposure to water at temperatures of 20°C, 40°C, 60°C and 90°C. It was found that the failure stress was not particularly affected except at 90°C where the failure stress decayed with ageing time. This supported their conclusion that less aggressive environments hardly degrade the adhesive.

Another tensile test was undertaken on tetraglycidy-4,4'-diaminodiphenylmethane-4,4'-diaminodiphenylsulphone (TGDDM-DDFS) adhesive by Morgan et al. [129] using dogbone specimens. The results of the tests showed that the elastic modulus and ultimate tensile strength all decreased with increasing moisture content at test temperatures between 23°C to 150°C. These results were attributed to the disruption of the hydrogen bond between the hydroxyl group in molecular chains by the highly polarised water molecules and hydrolysis.

Zanni-Deffarges and Shanahan [125] carried out tensile tests and torsion tests on DGEBA/TGMDA-DICY epoxy using bulk specimens and torsional joints respectively. The elastic modulus of the epoxy fell by 20% after conditioning at 70°C /100%RH for both cases, and the elastic modulus from the torsion joint decreased much more rapidly than that from the bulk. This rapid reduction of modulus was attributed to the higher rate of weakening at the interface as a result of interfacial
moisture diffusion. In similar work [121], the torsion tests were carried out on DGEBA-Permabond ESP470/ aluminium joints after exposure to the same condition as above. It was found that the adhesive shear modulus decreased with ageing time.

Broughton and Hinopoulos [128] tested bulk AV119 specimens aged in distilled water at 60°C. The results showed a 35% reduction of elastic modulus and 20% reduction for the Poisson's ratio of AV119. The strain to failure steadily increased with conditioning time and apparent plasticisation and necking was observed in specimens that were conditioned for up to 5 days. It is generally agreed that the moisture ingress in the adhesive causes plasticisation of bulk material and increases of strain to failure. As a result, the elastic modulus, and ultimate tensile stress reduce as moisture content increase.

A TAST joint test was used by Jurf et al. [66] to present a comprehensive study of the effect of moisture on the structural properties of two commercial adhesives, FM73M and FM300M. The results showed that the modulus decreased substantially at the temperature and the glass transition temperature reduced as the moisture content increased. Similarly, three different adhesive systems (AV119, F241 and AF126-2) were tested by Broughton et al. [130] using TAST specimens. The results showed that an increase of the moisture content has the same effect on the creep behaviour as a temperature increase.

Change of the adhesive properties due to incomplete curing can affect the results of the adhesion tests and mask the effect of the pre-treatment as shown in Figure 2.20 by Barraza et al. [131]. A two component room temperature curing adhesive paste (Araldite 2014) has been selected. The properties have been characterised with respect to the curing temperature and to the elapsed time between the bonding and testing. Ageing tests (40°C in water vapour) has been conducted on adhesive samples and the adhesive properties recorded over a period of 36 days. It has been shown that the absorbed water works as a plasticiser leading to a softening of the adhesive.
A stress approach has been used to obtain moisture dependent constitutive properties in the work reviewed. This approach has been found to produce reasonable predictions provided failure in the joint is essentially cohesive. However, interfacial or near interfacial failure would make this approach difficult to apply and it can result in a poor correlation between theoretical and experimental results. Another approach to characterise the mechanical properties of the adhesive is with fracture mechanics.

Jurf et al. [66] modelled a TAST joint using this approach. Linear elastic fracture mechanics (LEFM) was used to characterise the failure of these joints. It was assumed that the initial flaw was formed along the mid plane of the bondline. The fracture energy (Gc) was calculated using the virtual crack closure technique [63] at the corresponding joint failure load. The aluminium joints bonded with FM73M and FM300M were aged at 54°C/63%RH and 59°C/95%RH for 90 days and 120 days respectively. The results showed that the Gc reduced with increasing relative humidity. This indicated the weakening of joints after moisture degradation.

Wylde and Spelt [31] studied the fracture strength of two epoxy adhesives (Cybond 1126 and Cybond 4523GB) using double cantilever beams (DCB). The specimens were exposed to moisture at 100%RH, 60%RH and 30%RH. A special jig has been used by Fernlund and Spelt [132] to allow the fracture envelope Gc, as a function of
mode I and mode II ratio, to be measured through a double cantilever beam specimen. For DCB bonded with Cybond 1126, mode I fracture energy decreased with increasing humidity at 65°C, as shown in Figure 2.21(a). The reduction of fracture energy was more severe at 100%RH. DCB bonded with Cybond 4523GB was tested at two phase angles (48° and 60°). "Dried" and "wet" specimens were tested after exposure to 100% RH at three different temperature 35°C, 65°C and 85°C. For the "wet" joints, the strength first increased then decreased. It has been proposed that this increase of bond strength is due to plasticisation of the adhesive. A plot of $G_c$ versus exposure time for Cybond 4523GB degraded at 100% RH/85°C is shown in Figure 2.21(b). For longer exposure times, permanent degradation was noted and a reduction of fracture energy resulted. Greater reduction was observed at higher exposure temperatures.

![Figure 2.21 - $G_c$ versus exposure time for: (a) Cybond 1126 degraded at 65°C with 100% RH(+), 60% RH(x) and 30%RH(+); (b) Cybond 4523GB degraded at 65°C/100%RH, tested wet at 48°(○) and 60°(○), and dry at 48°(●) and 60°(●) [31]](image)

Moidu et al. [133] investigated the fracture energy of two aluminium-epoxy peel test systems: Hysol EA 9346 and Permabond E04. Both "wet" (immersed in deionised water at 67°C) and "dried" (kept under vacuum at 70°C for 3 days after removal from the water) peel specimens were tested at a rate of 5mm/min. The fracture energy corresponding to the peel force was calculated. Permabond E04/ aluminium showed markedly different peel forces between "dried" and "wet" where the "dried" peel
force was half of the "wet". This was attributed to plasticisation of the adhesive and cohesive failure when tested "wet", whereas, interfacial failure occurred when tested "dried". The fracture energy corresponding to the "dried" conditioning reduced with increasing exposure time. On the other hand, Hysol EA 9346/aluminium fracture energy in "dried" and "wet" conditions showed similar trends of reduction but the "dried" fracture energy was greater. This indicated that there was some strength recovery after drying the specimen.

Bowditch et al. [134] performed the 90° peel tests to characterise the fracture energy (Gc) over a range of relative humidities with 3M 2216 and Permabond ESP110 adhesives. The results showed that a sharp drop of fracture energy occurred at a critical water concentration and that the critical water concentration was test rate dependent. The existence of a critical water concentration was attributed to plasticisation, stress relief and hydration of salts. When the water exceeded the critical value, it was suggested that the strength reduction was due to interfacial degradation.

Two fracture tests, notched coating adhesion (NCA) and mixed mode flexure (MMF) were carried out by Loh [123] to characterise the fracture energy of AV119 as a function of the moisture concentration at the interface. The virtual crack closure and J-integral techniques were used to calculate the fracture energy and LEFM finite element modelling was used to validate the analytical solution. The schematic geometry of the NCA is depicted in Figure 2.22 [123]. A notch in the adhesive was introduced across the centre of the specimen. This notch placed significant stress on the interface and usually produced an initial sharp crack along the interface. The specimen was then loaded in tension as illustrated in Figure 2.22. The critical strain at which the crack propagated was obtained from the recorded strain data by synchronising the times. This critical strain was then used to calculate the fracture energy as shown in Figure 2.23 [123].
Figure 2.22 - Geometry and loading configuration of the NCA specimen [123]

Figure 2.23 - Fracture energy as a function of interfacial moisture concentration of AV119 characterised using the NCA tests [123]

Similar studies have been undertaken with the MMF tests as shown in Figure 2.24 [123]. It was found that fracture energy decreased monotonically as moisture content increased. The characterised strength parameters have been used to model progressive damage along the interface in some aspects of this research.
2.2.4.3 Hygroscopic swelling of adhesively bonded joints

It is known that polymers are hydrated and that the possibility of volume change in the presence of water exists. The swelling of adhesives due to moisture uptake may cause significant stresses in adhesive bonded joints and in turn has an effect on the joint strength.

Different relationships between the swelling and the moisture content have been noted. Xiao et al. [135] found that that swelling is concentration and temperature dependent. MacKague et al. [136] suggested that the relation between the length change and the moisture content can be best described by a power law. Gazit [137] studied the absorption of water and the swelling of glass-filled or reinforced epoxy resin from selected constant relative humidity atmospheres. The results suggested that swelling was only concentration dependent. The dimensional change rates were measured and formulated in an exponential expression. Various other researchers [138-141] found that, for the adhesives considered, the swelling showed a linear relationship with moisture content. The relationship between the swelling strain and the moisture content can be represented using a swelling coefficient.
The effect of hygrothermal aging on a particle-filled, epoxy-based adhesive was studied by Chiang et al. [138]. This study has explored moisture sorption characteristics and the associated behaviours of swelling of the adhesive. It was found that the hygrothermal aging temperature (50°C and 70°C) did not alter the volume increase of the adhesive with the water absorption. The average swelling coefficient obtained from this work was 0.41%\(^{-1}\).

Romanko and Knauss [139] studied the moisture expansion of FM73-M from swelling of bulk adhesive sheets exposed to water at room temperature. The linear swelling strain was calculated from measurements of the length of the samples both before immersion and after saturation had been reached. It was assumed that the swelling was isotropic, so the volumetric swelling was taken to be three times the linear swelling. This linear coefficient of swelling was found to be 0.22%\(^{-1}\) (moisture content).

Xiao and Shanahan [140] investigated the swelling of DGEBA/DDA epoxy resin during hygrothermal ageing. The specimens were aged in distilled water at different temperatures for different time intervals. Specimen volume was calculated from length, thickness, and width measurements made with micrometers, accurate to ±0.001 mm. The results showed that the rate of swelling of the polymer was less than that attributable to the mass of water absorbed initially, but that the rates equalise later. It was also found that the swelling was not fully reversible. The swelling coefficient was seen to be slightly dependent on the temperature (increased with increasing temperature). The average linear swelling coefficient was found to be 0.16%\(^{-1}\).

Cabanelas et al. [141] studied the exposure with water absorption in DGEBA cured with a synthesised aminopropyl perfunctionalised siloxane. The moisture uptake was measured gravimetrically. The fractional volume change due to water uptake was determined through near infrared spectroscopy. It was proved that a linear relationship existed between integrated near infrared reference band and the amount of water. The equilibrium moisture content was found to be around 2.5% and volume
change at saturation to be approximately 4%. This gives a coefficient of hygroscopic expansion (CHE) of about 0.53%$^{-1}$. A summary of swelling coefficients for epoxy adhesives reviewed is listed in Table 2.2.

<table>
<thead>
<tr>
<th>Ref.</th>
<th>Adhesive</th>
<th>Swelling coefficients (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>[138]</td>
<td>Epoxy</td>
<td>0.41</td>
</tr>
<tr>
<td>[139]</td>
<td>FM73-M</td>
<td>0.22</td>
</tr>
<tr>
<td>[140]</td>
<td>Epoxy</td>
<td>0.16</td>
</tr>
<tr>
<td>[141]</td>
<td>Epoxy</td>
<td>0.53</td>
</tr>
</tbody>
</table>

### 2.2.4.4 Environmental degradation modelling of adhesively bonded joints

Gledhill et al. [35] suggested that the durability can be predicted by combining water diffusion data with fracture mechanics. The diffusion in the bulk adhesive was determined using gravimetric experiments. The moisture uptake data was fitted to the Fickian diffusion relation. Applying a fracture mechanics approach, it was found that the fracture stress could be predicted if the water concentration in the adhesive was known.

Using moisture diffusion models and moisture dependent material properties, a durability model considering environmental degradation of adhesive bonded joints has been developed. Crocombe [1] presented a durability study framework (shown in Figure 1.1) and applied it to a FM1000 bonded lap joint through finite element modelling. A full non-linear coupled diffusion-mechanical analysis was undertaken to study the response of the joint after exposure to a moist environment. The moisture ingress in the joint was assumed to be Fickian. It was found that the joint failure occurred at the centre of the exposed joint, where the adhesive was dry and less ductile. The predicted residual strength was within a few percent compared with the experimental data.

Similar analyses were applied by Wahab et al. [142] to another two bonded joints, single lap joint and butt joint. The joints were immersed in water at 60°C for up to 60
weeks. Transient finite element diffusion analyses were performed in order to determine the moisture distribution in the adhesive layer at different time intervals. The results of these simulations were coupled to non-linear stress finite element analyses in which the constitutive data of the adhesive was defined as a function of moisture concentration. The swelling strains have been taken into account in the stress analysis and have been introduced to the adhesive layer according to the moisture distribution at a particular time. Further, FE diffusion simulations were carried out on various configurations of adhesive resin diffusion discs. Together with the experimental data these have been used to study the effect of the interface on the moisture degradation.

Hinopoulos and Broughton [128] found that FE modelling can be used to accurately predict the moisture concentration within the adhesive layer. This approach offers substantial time savings compared to the previously used analytical approach since the nodal concentration results can be directly transferred to the global FE model and readily linked with the moisture-dependent mechanical properties. A sequentially coupled mechanical-diffusion finite element model was then employed to perform a series of non-linear stress and deformation analyses of multiple T-peel joints exposed to moist environments [11]. The numerical predictions revealed that the distributions of stresses became more uniform along the adhesive layer when the adhesive contains increased amounts of moisture.

The interfacial rupture element model which is based on a cohesive zone model has been used to predict the durability of adhesively bonded joints exposed to controlled ageing environments by Crocombe and his co-workers [7, 143-145]. The adhesives studied were AV119 and FM73. A diffusion-stress coupled analysis was undertaken to include the effect of moisture on the failure of the joints. Moisture dependent material properties were obtained based on standard bulk tests and then incorporated into the model. The strain-separation relation which controls the behaviour of rupture elements was also modified in term of moisture content in the model. The two fracture parameters, fracture energy and tripping strain for the strain-separation relation, were characterised and calibrated from the degraded mixed mode flexure (MMF) testing and finite element analysis. In these studies only elastic performance
of the materials were modelled in conjunction with the rupture elements. Incorporating the substrate plasticity into this modelling method has been a goal in this research.

2.3 Summary and conclusion

To accurately predict the long-term performance of an adhesive joint working in a hostile environment, knowledge of the adhesive, the substrates, the joint assembly, the environment, the diffusion mechanism and the loading conditions are required. These can be difficult to acquire. Substantial durability and moisture diffusion testing methods have been developed for both bulk adhesives and bonded joints.

Absorbed moisture generally degrades adhesive joint strengths. The constitutive properties such as elastic modulus, failure stress and fracture energy tend to reduce with increasing moisture content of the adhesives. Occasionally, absorbed moisture can increase bonding strength. This is believed due to a stress relaxation in the slightly plasticised adhesive. High temperature can accelerate moisture diffusion in most adhesives.

The moisture concentration within the adhesive layer can be accurately predicted using finite element modelling. The uptake of moisture in adhesives can be characterised by gravimetric experiment. Moisture diffusion in bulk adhesives and bonded joints is different and should be handled separately in order to allow better durability prediction. Fickian and non-Fickian diffusion models are both encountered in the experiments and the latter requires particular attention in the FE modelling.

A sequentially coupled mechanical-diffusion finite element model can be used to predict the environmental degradation in adhesively bonded joints by combining the moisture-dependent mechanical properties with the nodal moisture concentration. Constitutive data of adhesives for a range of moisture concentrations can be obtained using tensile tests of bulk adhesives after exposure to controlled ageing environments.
Swelling and thermal characteristics are also needed to be incorporated in some cases to give a more realistic prediction in the durability modelling.

Both cohesive and interfacial degradation have been found in the durability testing of the joints and ought to be considered in the FE modelling as appropriate. Many predictive methods have been developed to model the failure initiation of bi-material structures. However, few of them have been able to predict environmental degradation and damage propagation in adhesively bonded joints. Some success has been achieved in modelling progressive interfacial failure, using the cohesive zone model approach, while little effort has been found in modelling progressive cohesive failure where the damage propagation path within the adhesive layer cannot be predefined. Thus, developing a progressive cohesive failure model for bulk degradation in adhesive bonded joints has been the main focus of this research. This goal has been achieved by integrating the coupled diffusion-mechanical analysis with a strain-based "strength of materials" modelling method and a continuum damage modelling method using FE package ABAQUS. The cohesive zone model approach has also been used in this research to model the progressive interfacial degradation in certain joints where the interfacial failure was found to be predominant.
As reviewed in Chapter 2, interfacial failure (debonding along the adherend-adhesive interface) and cohesive failure (failure entirely within the adhesive) are the two main types of failure commonly found in adhesively bonded joints. A progressive damage modelling method called the cohesive zone model (CZM) has been introduced to predict the failure and the crack propagation along the interface for adhesively bonded joints. An interfacial rupture element [7] has been developed to implement this method into finite element modelling and has obtained some degree of success.

In this chapter, a cohesive zone model with a modified interfacial rupture element is used to predict the residual strength of various AV119 bonded lap joints. A range of environmental degradation in these adhesive joints has been studied. A mixed mode interfacial rupture element with a traction-separation law was integrated into the finite element (FE) models. The two moisture dependent fracture parameters, fracture energy and tripping traction, were calibrated using a mixed mode flexure (MMF) test and finite element analyses. These parameters were used to model a series of single lap joints (SLJ) manufactured using the same adhesive/substrate
Chapter 3 Predictive modelling of AV119 single lap joints using cohesive zone model

materials. The FEA package ABAQUS [146] was used to implement the coupled mechanical-diffusion analyses. Plasticity of the substrates has been successfully incorporated into the modelling.

It should be noted that the experimental work represented in this chapter and the original cohesive zone model (CZM) for AV119 bonded joints has been carried out by Loh [123]. The original CZM was not appropriate for use in conjunction with elastic-plastic material models and developing this has been the main focus of the work presented in this chapter.

3.1 Material property characterisation

This section summarises all relevant experimental data, which have been determined by previous researchers [7, 123, 143].

The adhesive AV119 (Araldite 2007) is a one-component rubber toughened epoxy adhesive produced by CIBA Polymers and suitable for bonding a wide variety of materials. In this study, the AV119 was cured at 120°C for 2 hours before being exposed in three artificial ageing environments (as specified in ASTM [3] E104 or BS [4] 3718): 81.2%RH, 95.8%RH and distilled water immersion. The ageing temperature was 50°C and a digital hygrometer was used to ensure the required environments were maintained. The moisture dependent mechanical properties of environmentally aged bulk AV119 were determined using uniaxial tension tests of dogbone configuration specimens. (For test details refer to Loh et al. [123].)

Although the specimens were exposed to three different environments to obtain different moisture concentrations, the mechanical properties were found to be a function of moisture only. The elastic modulus of 0.8mm thick AV119 is shown in Figure 3.1 as a function of fractional mass moisture uptake based on a saturation value of 7.60% mwt. [123]. Poisson ratio was 0.4. These data were used for the subsequent durability modelling of the MMF and SLJ tests. The joint substrates used
Chapter 3 Predictive modelling of AV119 single lap joints using cohesive zone model

were mild steel, treated with alumina grit blasting. The elastic modulus and Poisson's ratio used for the steel substrates were 207GPa and 0.33 respectively.

The moisture uptake performance of AV119 was determined using a gravimetric approach. Specimens were periodically removed from the ageing environment, surface water was removed using analytical grade tissue paper and the specimen weighed on a Mettler M5 analytical microbalance. It was found that the response for the 2.0mm thick film matched the single Fickian model [113] given in Equation 3.1. The diffusion coefficients and the experimental equilibrium mass uptakes are listed in Table 3.1.

$$\frac{mwt_t}{mwt_\infty} = 1 - \frac{8}{\pi^2} \sum_{n=0}^{\infty} \frac{1}{(2n+1)^2} \exp \left[ -\frac{D(2n+1)^2 \pi^2 t}{4l^2} \right]$$

Equation 3.1

Table 3.1 Standard Fickian diffusion coefficients for 2.0mm thick AV119 at 50°C

<table>
<thead>
<tr>
<th>Ageing Environment</th>
<th>81.2%RH</th>
<th>95.8%RH</th>
<th>Water</th>
</tr>
</thead>
<tbody>
<tr>
<td>Diffusion coefficient (m^2/s)</td>
<td>3.85 x 10^{-13}</td>
<td>2.83 x 10^{-13}</td>
<td>1.9 x 10^{-13}</td>
</tr>
<tr>
<td>Equilibrium mass uptake (%mwt_\infty)</td>
<td>3.06</td>
<td>5.01</td>
<td>7.60</td>
</tr>
</tbody>
</table>
Chapter 3 Predictive modelling of AV119 single lap joints using cohesive zone model

It can be seen that saturation level in water is significantly higher than at 95.8%RH. Other researchers [114, 117] have noted an exponential type increase in saturation level with ageing RH. The experimental data and Fickian fits are shown in Figure 3.2. More details can be found elsewhere [123].

![Figure 3.2 - Experimental data fitted with single Fickian diffusion model for 2.0mm thick film](image)

3.2 Rupture element calibration

3.2.1 Interfacial failure and the cohesive zone model

Interfacial failure is commonly found in adhesively bonded joints and the interfacial separation process can be considered as a macro mechanism of failure of two different bonded materials. To model such interfacial failure, a separation law is required to characterise, phenomenologically, the separation or process zone that occurs ahead of the crack tip along the interface. It was assumed that the crack initiated when the stress at the crack tip reached a maximum stress and then softened as the crack opening increased as shown in Figure 3.3. The material that experienced softening was called the fracture process zone. The work done in opening the crack to form a new crack area is called fracture energy.
The separation law developed according to this assumption is shown in Figure 3.4 where the separation process is controlled by the two parameters: \( E \), the work done to create a crack extension of one element; and \( F_{\text{unloading}} \), the element force required to trip the separation process.

Although any unloading profile can be used for the separation law, it has been found that the shape is of minor importance [72]. The finite element code ABAQUS was employed to incorporate the above mixed mode separation law into the modelling. A two-noded spring element integrated in ABAQUS was used to model the behaviour of the rupture element in the two-dimensional problem. The shape of the separation
law followed by this spring element was defined through Equations 3.2 - 3.4, where \( G \) is the fracture energy of the bi-material interface, \( w \) is the width of the interface, \( \Delta a \) is the crack extension, \( T \) is the predefined tripping traction and \( u_{\text{releasing}} \) is the maximum relative displacement between the two nodes of each element at the point of release.

\[
E = G \times \Delta a \times w
\]

\[3.2\]

\[
F_{\text{unloading}} = T \times \Delta a \times w
\]

\[3.3\]

\[
u_{\text{releasing}} = \frac{2 \times E}{F_{\text{unloading}}}
\]

\[3.4\]

To model interfacial failure, multiple rupture elements were placed along the interface as shown in Figure 3.5. The two nodes of the spring elements were initially coincident. The distance between adjacent rupture elements is the crack extension \( \Delta a \). The force between these two nodes increased steeply with a high initial stiffness which was specified to ensure connectivity when the joint is loaded initially. As the spring force reached the predefined tripping force, \( F_{\text{unloading}} \), the unloading process initiated and the two nodes began to separate with a reducing force. At the point of the maximum relative displacement, \( u_{\text{releasing}} \), the two nodes separated, the spring force dropped to zero and release occurred. Thus the rupture element was effectively terminated and removed, and the crack propagated.

![Figure 3.5 - Arrangement of multiple rupture elements along the interface in a finite element model](image-url)
The tripping traction, $T$, and fracture energy, $G$, were predefined as the two critical fracture parameters which were determined from calibration using experimental data from the MMF specimens.

It can be seen from Figure 3.5 that both opening and shearing deformation (modes I and II) were accommodated. However, at this stage the fracture energy $G$ did not vary with mode of loading. Future work in this chapter will include adhesive plasticity and hence the energy dissipated by the rupture element can be considered as an intrinsic fracture energy that will not vary with mode of loading.

### 3.2.2 Rupture element calibration of AV119 using MMF

The MMF configuration used for the rupture element calibration was shown in Figure 3.6[123]. The MMF specimen consisted of grit blasted steel substrates bonded with a 0.4mm thick AV 119 adhesive layer. The adhesive was cured on the upper substrate and this was exposed for different times before a secondary bond was used to complete the specimen. The thicknesses of the MMF substrates were sufficient to prevent their yielding during testing. A 20mm pre-crack was introduced at the epoxy-steel interface using a Teflon film. The specimens were loaded in three-point bending at a displacement rate of 0.05mm/min and the crack length corresponding to the fracture load applied was measured using an in-situ video microscope (about x10 magnification).

![Figure 3.6 - Geometry and loading configuration of the MMF specimen][123]
The failure loads at different crack lengths for the MMF tests are shown in Figure 3.7. A curve of the fracture load against interfacial moisture concentration for a 20mm crack length is shown in Figure 3.8. The interfacial moisture concentration was determined using the Fickian diffusion model discussed earlier.

![Figure 3.7](image1.png)

*Figure 3.7 - Experimental failure load of MMF at different crack lengths (markers) and predicted failure load of MMF specimens using the calibrated fracture parameters (lines)*

![Figure 3.8](image2.png)

*Figure 3.8 - Initial peak loads (20mm crack length) as a function of interfacial moisture concentration and the resulting moisture dependent interfacial fracture energy of steel/AV119 interface*
It appeared that the fracture load was a unique function of interfacial moisture concentration even when the joints were exposed to different environments and for different exposure times.

To determine the fracture energy of the MMF test configuration, a two-dimensional linear elastic plane strain finite element model of the MMF specimen was generated and LEFM principles were applied by Loh [123]. The fracture energies for this test configuration at different interfacial moisture concentrations were obtained and are shown in Figure 3.8. This moisture dependent fracture energy was used by the interfacial rupture element in the durability modelling. The interfacial rupture element was then used to model the MMF test. The FE mesh is shown in Figure 3.9. The effect of tripping traction was assessed. This provided a calibrated pair of fracture energy and tripping traction as a function of interfacial moisture concentration.

![Figure 3.9 - MMF finite element model with the rupture elements along the interface of the upper substrate and the adhesive [123]](image)

A predefined crack path was generated in the FE model along the interface between the upper substrate (steel) and the adhesive (AV119) with an initial pre-crack of 20mm, as mentioned earlier. Multiple rupture elements were incorporated along the
crack path as illustrated in Figure 3.5. The mesh was generated using four-noded quadrilateral elements with refinement along the interface. The maximum and minimum element sizes were 2mm x 2mm and 0.0625mm x 0.0625mm respectively. The distance between rupture elements was kept constant at 0.0625mm. The moisture dependent elastic modulus of AV119, shown in Figure 3.1, was used for the adhesive layer. The elastic property of the steel substrates was used for the MMF calibration because no plastic deformation occurred during the experimental testing. A damping factor of $1 \times 10^{-3}$ was used in the ABAQUS stabilize function to prevent instability during the crack propagation.

The FE modelling calibration results for five selected interfacial moisture levels are shown in Figure 3.10. Each point on these curves has been found as the maximum failure load predicted from the FE modelling when using a particular combination of $T$ and $G$ in the analysis.

![Calibration curve](image)

**Figure 3.10 - Tripping traction calibration for MMF specimen at different moisture concentration levels**

It can be seen in Figure 3.10 that the predicted failure load within a certain range of tripping traction was relatively constant for each moisture (energy) level. The
process zone length (PZL) varied with tripping traction and it was found that the predicted failure load was relatively constant, and hence controlled mainly by the fracture energy, when the PZL was greater than the crack extension Δa (the distance between adjacent rupture elements). This region was named the energy dominated region.

The effect of mesh size on the energy dominated region was also studied as shown in Figure 3.10. It can be seen that the smaller mesh \( (l_u = 0.0625\text{mm} \times 0.0625\text{mm}) \) gave a greater range of the energy dominated region than the coarser one \( (l_u = 0.25\text{mm} \times 0.1\text{mm}) \). This is because a smaller PZL can be modelled with a finer mesh. However, the failure loads obtained from both mesh sizes within the respective energy dominated regions were the same. This means that the failure load was independent of mesh size, as long as rupture remained within the energy dominated region.

The calibrated tripping traction was selected as the dotted line shown in Figure 3.10. These were defined as a function of interfacial moisture concentration, and together with fracture energy, were used as the critical fracture parameters of the rupture element in the subsequent modelling of bonded joints. In this subsequent modelling, the moisture distribution varied continuously along the crack path, unlike the uniform interfacial moisture concentration obtained from the open faced exposure of the MMF specimen. The predicted failure loads of the MMF specimens having crack lengths other than the original crack length (20mm) used for calibration are shown in Figure 3.7. The solutions show good agreement over the entire range of the experimental results.

### 3.3 Single lap joint (SLJ) modelling

The single lap joint is the most common test method used to evaluate the strength of an adhesive joint following exposure to a hostile environment. Broughton et al. [6] have implemented a series of studies on steel/AV119 durability using the single lap
joint exposed to moist environments. The single lap joint configuration used is shown in Figure 3.11.

Figure 3.11 - Schematic configuration of the single lap joint used by [6] (not to scale)

In the experiment, the substrates of the joints were degreased with acetone before and after grit blasting treatment using 80/120 alumina. The bondline thickness of the adhesive was controlled using a small quantity of 250 ballontini (glass spheres, 1% by weight) mixed with the adhesive. The joints were clamped and cured at 140°C for 75 minutes. A pair of end tabs was bonded to each end of the joint to reduce the offset in the grips when loaded, and the fillet at both lap ends were moved after curing. Batches of conditioned specimens were immersed in distilled water at 50°C and were withdrawn at selected intervals over a 6 week period for testing. The tensile testing was carried out under ambient conditions at a constant displacement rate of 1mm/min.

A finite element model of the single lap joint was generated for progressive damage modelling using the CZM. A half mesh model was designed using four-noded quadrilateral elements with mesh refinement around the lap region, as shown in Figure 3.12. The maximum and minimum element sizes were 2mm and 0.0625mm, respectively. A total of 4 rows of elements were generated across the 0.25mm thick adhesive layer. The rupture elements were introduced along the interface of the adhesive and the bottom substrate. The same moisture dependent fracture parameters calibrated from the MMF specimen were assigned to the rupture elements used in the SLJ models. A rotational boundary condition was specified at the line of symmetry and displacement loading was applied at the end tabs. The elastic modulus used for the adhesive was the moisture dependent data shown in Figure 3.1. Both linear elastic and elastic-plastic behaviour were used for the mild steel substrates. The
elastic-plastic data of the steel, obtained by Broughton et al. [6] and shown in Figure 3.13, was incorporated into modelling.

Figure 3.12 -(a) Finite element mesh of single lap joint; (b) Mesh refinement in the adhesive layer; (c) Deformation of the joint with crack extension

Figure 3.13 - Elastic-plastic behaviour of the steel from [6] used for the substrates in the SLJ modelling

Again, a standard Fickian diffusion model was used to specify the moisture diffusion from one end of the lap region towards to the line of symmetry. Three diffusion schemes were used to model the moisture uptake in the single lap joints as listed in
Table 3.2. An interfacial diffusion coefficient was assigned to the row of the adhesive elements closest to the interface and the bulk diffusion coefficient was assigned to the other adhesive elements. The bulk diffusion data was based on the diffusion results obtained in the previous experiment from the 2.0mm AV119 adhesive immersed in water summarized in Table 3.2.

<table>
<thead>
<tr>
<th>Diffusion scheme</th>
<th>Bulk diffusion coefficient, $D_{bulk}$ (m$^2$/s)</th>
<th>Interfacial diffusion coefficient, $D_{interface}$ (m$^2$/s)</th>
</tr>
</thead>
<tbody>
<tr>
<td>TST1</td>
<td>$1.9 \times 10^{-13}$</td>
<td>$1.9 \times 10^{-13}$</td>
</tr>
<tr>
<td>TST2</td>
<td>$1.9 \times 10^{-13}$</td>
<td>$3.8 \times 10^{-12}$</td>
</tr>
<tr>
<td>TST3</td>
<td>$1.9 \times 10^{-13}$</td>
<td>$7.6 \times 10^{-12}$</td>
</tr>
</tbody>
</table>

In the literature [123], there is some suggestion of more rapid diffusion in the interfacial region. Thus, higher diffusion rates were defined at the interface. The mechanisms for this might be attributed to capillary diffusion and cathodic delamination. Contour plots of the moisture distribution in the adhesive layer after exposure to moisture for 21 days are shown in Figure 3.14 for the different diffusion schemes.

![Figure 3.14 - Moisture distribution profile of the adhesive layer exposed for 21 days](image)

It can be seen that a higher interfacial diffusion results in quicker penetration into the joint and a slightly curved moisture concentration front. Failure prediction with elastic substrates was carried out first, for a range of selected exposure times. The ultimate failure loads of the joint obtained from the experimental results and the
finite element model are plotted in Figure 3.15. It is seen that the predicted failure load of the dry joint did not correlate well with the experimental results. This is probably because plasticity has not been incorporated in the steel substrates. This would allow more bending of the substrates and straining of the adhesive, which would directly affect the predicted failure response. This was investigated by carrying out the analysis including plasticity in the substrates. The predicted dry joint failure load, also plotted in Figure 3.15, is seen to be much closer to the experimental data.

![Figure 3.15 - Predicted ultimate failure load of the SLJ using the different schemes listed in Table 3.2 and the experimental results from [6]](image)

The predicted results from the elastic-plastic substrate modelling for degraded specimens also showed better agreement with the experimental results for the range exposure times considered. It is also seen that the predicted failure load matched the experimental results more closely when a higher interfacial diffusion was accommodated. This is due to higher rates of degradation at the interface of the joints as illustrated in Figure 3.14, where the contours of TST2 and TST3 show relatively similar penetrations of moisture into the adhesive but much greater than TST1. As exposure times increased, the predicted failure load of the linear analysis gets closer to the results of the non-linear analysis. This trend occurred more quickly with higher rates of diffusion of the moisture. This is because the degraded joints failed at lower levels of loading before the substrates exhibited significant plasticity.
Figure 3.16 shows typical contour plots of equivalent plastic strain at integration points of the failing joints that have been exposed for 21 days. The plot of TST3 for 21 days exhibits complete elastic behaviour at the initial peak loading level while the joint for TST1 still exhibits much plastic deformation in the lower substrate at the predicted failure load. The predicted loading history and crack propagation of the undegraded joint and joints exposed for 21 days with two different diffusion schemes are shown in Figure 3.17.

Figure 3.16 - Typical contour plots of equivalent plastic strain at integration points (PEEQ) for TST1 and TST3 diffusion scheme at the unloading increment (21 days)

Figure 3.17 - Predicted loading history and crack propagation of SLJ specimens after exposure to a moist environment for 0 and 21 days (TST1 and TST3)
Except the curve with the linear elastic legend, the other results are all obtained from analyses including the elastic-plastic substrate behaviour. The loads of an undegraded and a degraded specimen for TST1 increased linearly initially and then became nonlinear with applied displacement. The turning point of these two curves, $P_0$, occurred just at the same loading level, about 4.6kN. It marks the point when the steel substrate began to yield and it is far below the predicted joint failure loads. The predicted loading history from the TST3 specimen for 21 days exposure shows quite a linear plot. The main reason for this is that the joint failed at an applied load level of about 2.8kN which is far below the initial substrate yielding load of 4.6kN. The crack propagation of the joint using TST3 also extended quite linearly and rapidly just at the displacement level corresponding to the ultimate failure load. For the undegraded specimen with plasticity, the crack initiated when the peak load was reached, then extended slowly with a gradual unloading process and finally dropped rapidly when the applied load reduced to a value of about 5.9kN. The TST1 specimen degraded for 21 days had a similar final load of about 5.9kN. This gradual reduction probably comes from the plastic deformation in the steel substrates (compared with the sharply reducing load from the linear analysis).

### 3.4 Summary and conclusion

A recently developed rupture element has been extended to predict the residual strength of degraded adhesively bonded lap joints incorporating plasticity in the joint substrates. A mixed mode interfacial rupture element with a separation law was proposed to simulate interfacial failure of the joints exposed to various ageing environments. Two moisture dependent fracture parameters (fracture energy and tripping traction) were used to control the rupture element. The MMF test was used to calibrate these fracture parameters using FE analyses by matching the numerical results with the associated experimental failure loads. The calibrated fracture parameters then were used to model the other bonded joints with no further modification. A coupled diffusion-mechanical finite element analysis was undertaken.
using the commercial FE package ABAQUS. SLJ specimens were selected to demonstrate the efficiency of the presented methodology.

The numerical predictions for a range of degraded joints agreed well with the corresponding experimental data using the cohesive zone model. The incorporation of the plasticity for the substrates has been completed successfully, providing a significant enhancement of the predicted results.

It should be stressed that this predictive modelling methodology is in a state of development and that some aspects, such as interfacial diffusion, have not been fully implemented whilst other aspects, such as the effect of prolonged exposure and the increase in equilibrium concentration in the interfacial regions, have not yet been included.

Nevertheless, the work in this chapter has demonstrated that the cohesive zone model using rupture elements has much potential for use in progressive damage modelling of interfacial failure and the prediction of the residual strength of degraded adhesively bonded joints. A method of modelling interfacial diffusion has also been introduced and it was found that this provided improved residual strength prediction.
Experimental work is necessary to produce input for and to validate durability modelling of adhesive bonding. This chapter covers a series of experiment work that included material property characterisation, moisture uptake measurement and joint degradation testing, in order to verify the models developed.

The joints modelled in the remainder of this thesis were made using three adhesives: FM73, EA9321 and E32. The required material data includes moisture dependent stress-strain curves, diffusion and swelling. ALL THE DATA FOR THESE SYSTEMS HAVE BEEN GENERATED BY OTHER RESEARCHERS. They have been included here for completeness. Additional tests of FM73 double lap joints have been undertaken by the author as part of a joint research project and these are also reported here. These joints were manufactured elsewhere.

These experimental data were then used in the modelling work of the enviromentally degraded joints in the following chapters.

4.1 Material properties and moisture uptake measurement

This section summarises the bulk adhesive experiments undertaken by the other researchers in the same group or at University of Surrey.
4.1.1 Adhesive FM73

FM73 is a rubber toughened, heat setting, film adhesive with a polyester knit carrier cloth which enables the film to be handled, cut to shape and laid up easily. To manufacture a cured adhesive film of 0.5 mm thickness, 4 layers were stacked together. The compound was placed between two release films and squeezed together with a press. The adhesive was then cured at 120°C for 60 minutes as suggested by the manufacturer. Spacers were needed to control the thickness. After curing the film was cut to a dumbbell shape using a CNC machine. The gauge length was 30mm and the width was 5mm. The dumbbell specimens were used to undertake gravimetric experiments to determine the characteristic of the moisture ingress in the adhesive. An artificial ageing environment of 95.8%RH (Related Humidity) at an ageing temperature 50°C, as stated in ASTM E104 [3] or BS 3718 [4], was used for the 0.5mm thick specimens. A digital hygrometer was used to ensure the required environment was maintained. The moisture dependent mechanical properties of FM73 were determined using uniaxial tension tests of the dry specimens and saturated at 95.8%RH /50°C, as shown in Figure 4.1. Passions ratio was 0.4. This experiment was undertaken by researchers at QinetiQ (in the same consortium).

![Figure 4.1 - Moisture dependent tensile properties of bulk FM73 (experimental data obtained from QinetiQ)](image-url)
Chapter 4 Experimental testing

The material behaviour of FM73 at intermediate moisture levels was determined by linear interpolation between results from the dry and the saturated conditions. These data were used for the validation modelling of the FM73 bonded single lap joints (SLJ).

4.1.2 Adhesive EA9321

Hysol EA9321 (Henkel Aerospace, Bay Point, CA, USA) is a two-component thixotropic paste adhesive that exhibits toughness, retains strength at elevated temperatures and yields durable bonds over a wide temperature range. Bulk film of thickness 0.48mm samples were manufactured and cured at room temperature for 7 days before being exposed in an artificial ageing environment of 95.8%RH at an ageing temperature 50°C. To manufacture void-free films, the adhesive had to be mixed under vacuum, breaking the bubbles to release the trapped air. After the air had been released the adhesive was placed between release films and compressed between thick glass plates. The final thickness was controlled by spacers.

The moisture uptake performance of EA9321 was determined using a gravimetric approach. Bulk film specimens were periodically removed from the ageing environment, surface water was removed using analytical grade tissue paper and the specimen weighed using a Mettler M5 analytical microbalance. It was found that the response for the film was fitted well by the Fickian model [113] given in Equation 3.1. The diffusion coefficient ($D$) and the equilibrium mass uptake ($m_\infty$) are listed in Table 4.1. These tests were undertaken at QinetiQ. The fitted Fickian parameter was obtained by the author.

<table>
<thead>
<tr>
<th>Ageing Environment</th>
<th>95.8%RH, 50°C</th>
</tr>
</thead>
<tbody>
<tr>
<td>Diffusion coefficient ($m^2/s$)</td>
<td>$3.0 \times 10^{-13}$</td>
</tr>
<tr>
<td>Equilibrium mass uptake ($m_\infty$)</td>
<td>3.85%</td>
</tr>
</tbody>
</table>
The predicted results are shown in Figure 4.2 together with the experimental data. These parameters were used in the coupled diffusion-mechanical models of the EA9321 bonded joints.

![Figure 4.2 - Experimental data (obtained from QinetiQ) fitted with single Fickian diffusion model for 0.48mm thick EA9321](image)

The moisture dependent mechanical properties of environmentally aged bulk EA9321 were determined using uniaxial tension tests with adhesive film dogbone specimens. Experimental stress-strain curves, obtained from the dry specimens and specimens saturated at 95.8%RH /50°C are shown in Figure 4.3. Passion ratio was 0.36. These data were used for the subsequent modelling of the EA9321 bonded joints. The stress-strain behaviour of EA9321 at intermediate moisture levels was determined by linear interpolation between results from the dry and the saturated conditions.

It has been shown in Chapter 2 that the swelling of a polymer due to moisture uptake may cause significant stresses in adhesively bonded joints and, in turn, affect the joint strength. A linear coefficient of hygroscopic expansion (CHE), obtained using bulk film specimens of EA9321 exposed to an environment at 95.8%RH /50°C, is shown in Figure 4.4. The data were determined by measuring the increase of a given gauge length of the bulk adhesive film for various absorbed levels of moisture. This
parameter was used to incorporate the swelling effect of the adhesive into the durability modelling of the EA9321 bonded single lap joints. These tests were also carried out by QinetiQ.

![Graph showing moisture dependent tensile properties of bulk EA9321](image)

**Figure 4.3 - Moisture dependent tensile properties of bulk EA9321 (experimental data obtained from QinetiQ)**

![Graph showing CHE (Chebyshev's error) of EA9321 exposed to 95.8%RH/50°C](image)

**Figure 4.4 - CHE (% -1) of EA9321 exposed to 95.8%RH/50°C (experimental data obtained from QinetiQ)**

4.1.3 Adhesive E32
Permabond E32 is a two-part, mineral filled ductile adhesive system. The adhesive was cured at 50°C in an oven for 48 hours. Bulk adhesive dumbbell specimens were manufactured for gravimetric and tensile tests. Sheet thicknesses were chosen to be 0.4, 0.8 and 1.5mm. The moisture uptake performance of the bulk E32 was measured using a gravimetric approach with specimens immersed in distilled water at room temperature. The ultimate equilibrium moisture uptake for the bulk specimens was found to be 9.7%. These tests were undertaken by Hambly [122].

It was found that the uptake response of the bulk specimen was thickness dependent and could not be fitted using simple single stage Fickian diffusion model [113] as shown in Equation 3.1. A two-stage Fickian model with an evaporative boundary condition [113] was therefore used to describe the moisture uptake of E32 as illustrated in Equations 4.1 to 4.3. The diffusion parameters obtained from immersion in distilled water at RT for this adhesive are shown in Table 4.2. The best fit of the moisture uptake of E32 using these parameters, from both analytical solution and finite element (FE) modelling result, is shown in Figure 4.5.

The evaporative boundary condition (EBC) can be expressed as:

$$-D_{1} \frac{\partial c}{\partial x} = \alpha (c_{\infty} - c_{s})$$

4.1

Where $D$ is moisture diffusion coefficient and $\alpha$ is an analog to the surface convective heat transfer coefficient. $c_{\infty}$ and $c_{s}$ are the ambient and surface moisture concentration levels, respectively. A single stage Fickian model used in combination with the EBC gives the time dependent moisture uptake [113]:

$$\frac{m_{t}}{m_{\infty}} = 1 - \sum_{n=1}^{\infty} \frac{2L^{2}\exp(-\beta_{n}^{2}D_{1}t/L^{2})}{\beta_{n}^{2}(\beta_{n}^{2} + L^{2} + L)}$$

$$\beta\tan\beta = L, \quad L = \frac{la}{D_{1}}$$

4.2

where $l$ is the half thickness of the sheet in the moisture diffusion direction and $t$ is the immersion time. The mass uptake of a single stage Fickian model with an instantaneous boundary equilibration condition gives [113]:

76
\[
\frac{m_{2,t}}{m_{2,\infty}} = 1 - \frac{8}{\pi^2} \sum_{n=0}^{\infty} \frac{1}{(2n+1)^2} \exp \left[ -\frac{D_z (2n+1)^2 \pi^2 t}{4l^2} \right]
\]

4.3

A two-stage Fickian model with an evaporative boundary condition is simply the superposition of the above two single stage Fickian models.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>(a_i) (10^{-11} \text{ms}^{-1})</td>
<td>5.0</td>
</tr>
<tr>
<td>(D_z) (10^{-14} \text{ms}^{-1})</td>
<td>13.75</td>
</tr>
<tr>
<td>(m_{-\infty}) (%)</td>
<td>4.53</td>
</tr>
<tr>
<td>(D_{z,2}) (10^{-14} \text{ms}^{-1})</td>
<td>6.07</td>
</tr>
<tr>
<td>(m_{-a}) (%)</td>
<td>5.17</td>
</tr>
<tr>
<td>(m_{-\infty}) (%)</td>
<td>9.7</td>
</tr>
</tbody>
</table>

Table 4.2 Parameters of E32 for a two-stage Fickian diffusion model with boundary effect [122]

Figure 4.5 - Experimental uptake data for E32 and uptake data modelled using a two-stage model with a boundary effect (experimental data from [122])

The moisture dependent mechanical properties of environmentally aged bulk E32 were determined using uniaxial tension testing of dumbbell specimens of 0.4mm thickness. The data are shown in Figure 4.6 and were used for the subsequent durability modelling. Compared with the moisture dependent stress-strain curves of EA9321 (shown in Figure 4.3), it can be seen that the saturated E32 degrades more than the saturated EA9321 from their dry states. This can be explained by the higher moisture saturation of E32 (9.7\%) than of EA9321 (3.85\%).
Chapter 4 Experimental testing

4.1.4 Substrate materials

The mechanical properties of unidirectional carbon fibre epoxy composite IM7-8552 and aluminium alloy 7075-T6 used in the modelling were obtained from standard tests, [147] and [148], as shown in Table 4.3 and Figure 4.7 respectively.

Table 4.3 Mechanical properties of IM7-8552 [147]

<table>
<thead>
<tr>
<th>$E_{11}$ [GPa]</th>
<th>$E_{22}$ [GPa]</th>
<th>$E_{33}$ [GPa]</th>
<th>$G_{12}$ [GPa]</th>
<th>$G_{13}$ [GPa]</th>
<th>$G_{32}$ [GPa]</th>
<th>$\nu_{12}$</th>
<th>$\nu_{13}$</th>
<th>$\nu_{32}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>160</td>
<td>10</td>
<td>10</td>
<td>4.8</td>
<td>4.8</td>
<td>3.2</td>
<td>0.31</td>
<td>0.31</td>
<td>0.52</td>
</tr>
</tbody>
</table>

Figure 4.6 - Moisture dependent tensile properties of E32 (from [122]) used for modelling

Figure 4.7 - Tensile properties of aluminum substrates ($E = 72$GPa, $v = 0.3$) [148]
Moisture uptake measurements of the composite (IM7-8552) were carried out using gravimetric experiments. The samples were cut so that the diffusion process could be assumed quasi one-dimensional either transverse to or parallel to the fibres. The Fickian diffusion parameters and the coefficient of hygroscopic expansion (CHE) of the composite exposed to an environment of 95.8%RH / 50°C are shown in Table 4.4 and Figure 4.8. The mechanical property of the composite was found to be essentially independent of moisture in all directions. These tests were undertaken by Liljedahl [149].

<table>
<thead>
<tr>
<th>Moisture Environment</th>
<th>D-parallel to fibre axis (m²/s)</th>
<th>D-perpendicular to fibre axis (m²/s)</th>
<th>Equilibrium mass uptake (%mₑ)</th>
</tr>
</thead>
<tbody>
<tr>
<td>95.8%RH, 50°C</td>
<td>$7 \times 10^{-13}$</td>
<td>$2 \times 10^{-13}$</td>
<td>~1.0</td>
</tr>
</tbody>
</table>

![Figure 4.8 - CHE (%⁻¹) of the composite exposed to 95.8%RH/50°C [149]](image)

4.2 Aluminium-FM73-composite Double Lap Joint
Chapter 4 Experimental testing

The Double Lap Joint (DLJ) test is widely used in studying the strength and durability of adhesive joints, as discussed in Chapter 2. A series of FM73 bonded aluminium - composite double lap joints, as shown in Figure 4.9, were prepared and tested following staged withdrawals from two controlled ageing environments. This experiment was carried out by the author and the experimental results were used to validate modelling by other researchers in the group.

![Figure 4.9 - A typical double lap joint to be tested](image)

### 4.2.1 Substrate pre-treatment and geometry of the specimens

Unidirectional carbon fibre reinforced polymer (IM7-8552) of thickness 4mm formed the centre adherend and aluminium alloy (7075-T6) of thickness 2mm was used for the outer adherends. The aluminium substrates were chromic acid etched and the composite substrates were degreased with acetone, before the adhesive was applied. The resulting FM73 bondline thickness varied between 0.05mm and 0.18mm as listed in Appendix 4.1. The nominal bondline overlap length was 12.5mm while the actual lengths ranged between 12.37mm and 12.84mm as measured using a shadow graph (Mitutoyo Profile Projector PJ 300, shown in Figure 4.10). The three substrates were nominally 84mm x 25mm. The end-tabs, added onto the inner adherend to ensure symmetrical gripping and loading, were nominally 38mm x 25mm. A rigid spacer added between the outer adherends was used to provide solid clamping at the outer end of the joint, as shown in Figure 4.9.
4.2.2 Ageing environments and testing plan

The adhesive joints were degraded by placing the samples into two hot/wet environments. Artificial ageing conditions were used, instead of a natural environment, to provide a controlled degradation. The configuration of the ageing environment is an important task and guidance can be obtained from ASTM [3] E104 or BS [4] 3718. However, the important specifications are summarised in this section, and include the ageing chamber, saturated salts solution and the hygrometer.

Glass containers were used for all the experiments as shown in Figure 4.11. The container must be air tight in order to obtain stable relative humidity (RH). A simple rubber bung will give excellent sealing for the 250ml cone flask. For the rectangular glass container, it was found that a rubber sealing strip with dead weights compressing the rubber and silicon sealant against the lid provided sufficient sealing and allowed accessibility.
Figure 4.11 - Degradation environments and controlment: (a) an ageing chamber with a digital hygrometer; (b) each of the ageing chambers was placed in the air ventilated oven at 50°C or 70°C as listed in Table 4.1.

The constant relative humidity environments were generated by means of aqueous solutions using different saturated salts at different temperatures as listed in Table 4.5. One is a potassium chloride (KCL) saturated solution at the temperature 70°C which gave a constant relative humidity 80.1±0.5%. The other one is a potassium sulphate (K$_2$SO$_4$) saturated solution at the temperature 50°C which gave a constant relative humidity 95.8±0.3%. The saturated solutions were prepared by mixing sufficient analytical grade salt with distilled water to ensure there was a significant excess of solid salt no longer dissolved. The mixture took the form of a sludge. A depth of 5mm saturated solution in the container was sufficient. The environment may take several hours to reach equilibrium depending on the stability of the temperature and the size of the container. The fixed temperatures were controlled by the ovens.

<table>
<thead>
<tr>
<th>Salts</th>
<th>Temperatures</th>
<th>Relative Humidity, % RH</th>
</tr>
</thead>
<tbody>
<tr>
<td>KCl</td>
<td>70°C</td>
<td>80.1±0.5</td>
</tr>
<tr>
<td>K$_2$SO$_4$</td>
<td>50°C</td>
<td>95.8±0.3</td>
</tr>
</tbody>
</table>
The RH created was monitored by a digital hygrometer. The hygrometer used, had a range of measurement from 10%RH - 95%RH with an accuracy of 3% and an allowable operating temperature is between 0°C - 70°C. The calibration of the hygrometer was carried out first at two specific relative humidities (33% and 75% RH) using saturated MgCl and NaCl solutions respectively in a conical flask as instructed by the manufacturer. The probe was placed inside the oven, leaving the reading console outside. Each of the environment chambers were placed in the ovens and were monitored individually to ensure the required environment had been achieved as shown in Figure 4.11. Each oven was equipped with a fan ventilation system to ensure an even temperature distribution across the entire space.

The original experimental plan was to withdraw and test the joints after they had aged for 1, 2, 4, 8, 12 and 26 weeks in each environment. However, the initial trial showed little change in failure loads after 2 weeks of degradation in both environments. Thus, the ageing time was adjusted to 1, 2, 4, 12, 26 and 52 weeks as shown in Table 4.6. Three replicates were tested for each environment and degradation time. Three other specimens were dried in a desiccator for 1 week and then tested as "dry" samples. The rest of the specimens were put directly into the oven at 70°C and tested at various times as shown in Table 4.6.

<table>
<thead>
<tr>
<th>Time (week)/ Number</th>
<th>Dry, 70°C</th>
<th>80.1% RH, 70°C</th>
<th>95.8% RH, 50°C</th>
</tr>
</thead>
<tbody>
<tr>
<td>0</td>
<td>3</td>
<td>3</td>
<td>3</td>
</tr>
<tr>
<td>1</td>
<td>2</td>
<td>3</td>
<td>3</td>
</tr>
<tr>
<td>2</td>
<td>2</td>
<td>3</td>
<td>3</td>
</tr>
<tr>
<td>4</td>
<td>2</td>
<td>3</td>
<td>3</td>
</tr>
<tr>
<td>12</td>
<td>2</td>
<td>3</td>
<td>3</td>
</tr>
<tr>
<td>26</td>
<td>2</td>
<td>3</td>
<td>3</td>
</tr>
<tr>
<td>52</td>
<td>1</td>
<td>3</td>
<td>3</td>
</tr>
</tbody>
</table>

To distribute the variation of the adhesive thickness, the specimens have been carefully chosen and grouped for different environments and ageing times. A detailed testing programme is listed in Appendix 4.2.
4.2.3 Loading procedure and testing results

The specimen setup for the test is shown in Figure 4.12. A screw driven servo-electromechanical machine, Instron 5500 using the Merlin tensile testing system, was chosen to test the double-lap joints, under a quasi-static uniaxial tensile loading. A 100kN load cell was used and the tests were carried out at a constant crosshead speed of 1.0mm/min and at room temperature. The gripped length at each end was 38mm. A 10% full scale contacting extensometer was used to record the axial strain. It was attached to the specimen by means of O-rings at the standard gauge length of 25mm, as shown in Figure 4.12. The stationary arm of the extensometer was located at the top of the gauge length while the moving arm was on the bottom. The test data was automatically logged by the computer for further processing. The overlap region was also monitored during the loading process using in-situ video microscopy. The load cell and the extensometer were calibrated prior to any testing. Calibrating the load cell was done automatically when the machine started up. Calibration of the extensometer was carried out manually, with two calibration points set with the aid of a bench micrometer, as shown in Figure 4.13.

![Figure 4.12 - Loading setup for the DLJ testing](image-url)
The failure surface of the joints were inspected later to give details of the types and loci of failure. All 46 joints were photographed before and after testing. The experimental failure loads are shown in Figure 4.14 and Table 4.7.

**Table 4.7 Failure load of the tested DLJ specimens**

| Ageing Time (weeks) | Dry (70°C) | | 80.1% RH, 70 °C | | 95.8% RH, 50 °C | |
|---------------------|------------|-----------------|-----------------|-----------------|-----------------|
|                     | Average(kN) | Number of repeats | Average(kN) | Number of repeats | Average(kN) | Number of repeats |
| 0                   | 23.87       | 3               | 21.73         | 3               | 22.17         | 3               |
| 1                   | 25.27       | 2               | 22.05         | 3               | 21.94         | 3               |
| 4                   | 21.82       | 3               | 19.93         | 3               | 21.06         | 3               |
| 12                  | 26.01       | 2               | 21.62         | 3               | 20.33         | 3               |
| 26                  | 26.92       | 2               | 17.19         | 3               | 21.75         | 3               |
| 52                  | 27.14       | 1               | 17.50         | 3               | 17.50         | 3               |
Most of the experimental results showed a decrease in residual strength with increasing ageing time for the two hot/wet environments, as expected. There was no significant difference in response between the two different environments, which raises the question of how the moisture environment degraded the joints. The specimens conditioned dry in the oven at 70 °C showed a slightly increase in failure load over the same period. This is probably due to further post curing of the adhesive or relaxation of residual stresses.

The average failure load of the 26 week-degraded specimens was about 8% higher than the results at 12 weeks. This may be a result of the variation of the joint geometry and bonding quality. The scatter in the 12 week-degraded failure loads at 80.1%RH/70°C and the 52 weeks-degraded results at 95.8%RH /50°C were also high (47%-68%). Further study of individual parameters such as the adhesive thickness and overlap length were then considered. The actual overlap lengths for these joints were quite similar (as listed in Appendix 4.1). The variation of failure load with bondline thickness of each joint under the same degradation conditions is shown in Figure 4.15.

Figure 4.15 - Study of bondline thickness effect on failure loads of different joints under the same degradation conditions: (a) 80.1%RH /70°C; (b) 95.8%RH /50°C

(Aluminium - FM73 - Composite DLJ)
It can be seen in Figure 4.15 that the failure load increased with the bondline thickness in most cases, except for the 26 week-degraded joints at 80.1%RH/70°C and the 4 and 52 weeks-degraded joints. However, other more specific experiments need to be done to further study the effect of bondline thickness on this adhesively bonded double lap joint.

### 4.2.4 Failure surface analysis

A preliminary analysis of the failure surface was undertaken. It was observed that the failure of the specimens was mainly cohesive for the dry joints, including the joints conditioned dry at 70°C. For both sets of degraded joints, a progressive increase can be identified in the extent of interfacial failure with ageing time. Some composite failure was found occasionally in some joints. The photographs of a dry specimen and a 52 week-degraded one are shown in Figure 4.16 as contrasting examples.

![Figure 4.16 - Typical failure surfaces: (a) Cohesive failure primarily of the unconditioned dry joint; (b) Interfacial failure overwhelmingly of a joint conditioned in the environment of 95.8%RH /50°C for 52 weeks](image)

A thorough quantitative analysis was then undertaken using a computer-connected micro-scanner and a series of image post-processing programs on the failure surface of the tested specimens. The basic procedure was: a), take a detailed picture of a
failure surface using the micro-scanner; b), use imaging software such as Photoshop to transform the picture into a simple black and white graphic acceptable for mathematical software MATLAB; 3), use MATLAB to calculate the percentage of this binary (black=0/white=1) system. The calculated results represented the failure type of the examined surface. Such a 3-step sequence is shown in Figure 4.17.

![Micro-scanned surface](image1)
![Digital Imaging treatment](image2)
![MATLAB binary gif surface](image3)

**Figure 4.17 - Post-processing procedure of a photoed surface of the joints**

![Graph](image4)

**Figure 4.18 - Interfacial failure surface ratios from the micro-scanned photographs of the tested joints (Aluminum - Fe375 - Composite DLJ)**

Such quantitative analyses are laborious. Thus only the joints degraded at 95.8%RH/50°C were selected, together with the set of dry conditioned joints. The calculated interfacial failure surface ratios for the above two sets of tested joints are shown in Figure 4.18. The interfacial failure in the dry conditioned joints was found consistently lower than 6.6%. This ratio increased to 55.5% in the joints exposed to
95.8%RH /50°C for 52 weeks. Compared to the experimental failure loads (shown in Figure 4.14), the curve of the increasing interfacial failure for the degraded joints has acquired a similar trend.

4.2.5 Summary

A series of FM73 bonded aluminium - composite double lap joints were prepared and tested in two controlled environmental degradation conditions: namely 80.1%RH at 70°C and 95.8%RH at 50°C. A controlled dry/hot environment was also used to study the response of the joints. It was found that the failure loads of the tested specimens generally decreased with increasing ageing time for both ageing environments. The two different ageing conditions had a similar influence on the joints. Further study of the failure surfaces of the joints has shown that, with increasing ageing time, the failure of the joints became increasingly interfacial. Joints conditioned at dry/70°C showed a slight increase in bond strength over the same ageing period. The failure surface of these joints also showed a slightly increasing cohesive failure with increasing ageing time.

4.3 Mixed Mode Flexure test of EA9321

A mixed mode flexure (MMF) test has been used in Chapter 3 to calibrate the interfacial fracture property of AV119. A similar MMF test, as shown in Figure 4.19, was chosen to calibrate the cohesive failure parameters of EA9321 for the subsequent predictive modelling. It was a significantly different configuration to the SLJ specimens tested later and hence a good test of the general applicability of the cohesive failure model. This experiment was undertaken by Liljedahl [149].

The MMF specimen consisted of aluminum alloy 7075-T6 substrates bonded with a 0.5mm thick EA9321 adhesive layer. The adhesive was cured on the upper substrate and this was exposed in different environments before a secondary bond of EA9321 was used to attach the lower substrate and complete the specimen. Two moisture saturation levels were achieved for the MMF specimens: 2.1% at 70°C /79.5%RH
and 3.85% at 50°C/95.8%RH (determined from separate gravimetric tests). The thicknesses of the MMF substrates (3.16mm) were sufficient to prevent their yielding during testing. A 20mm pre-crack was introduced on the EA9321 adhesive-aluminum interface using a teflon film. The specimens were loaded in three-point bending at a displacement rate of 0.05mm/min and the crack length corresponding to the fracture load was measured using an in-situ video microscope. The fracture loads recorded for the MMF tests were used in conjunction with the finite element analysis (FEA) modelling later to determine the moisture dependent failure parameters of the adhesive. The experimental failure loads at different crack lengths for the MMF tests are shown in Figure 4.20.

![Figure 4.19 - Geometry and loading configuration of the MMF specimen (width= 12.7mm) [149]](image)

![Figure 4.20 - Experimental failure load of MMF joints at different crack lengths [149]](image)
The failure surface for the MMF specimens tested at different moisture levels is shown in Figure 4.21. It was seen that for the dry joint the failure was mainly cohesive and for the wet joints the failure was much closer to the interface.

![Figure 4.21 - Failure surfaces for the EA9321 MMF specimens: (a) Dry; (b) 79.5%RH; (c) 95.8%RH [149]](image)

### 4.5 Thick Adherend Shear Test of EA9321

A Thick Adherend Shear Test (TAST) for dry EA9321 is summarised in this section. The joint geometry is shown in Figure 4.22. These tests were carried out by Wagman at University of Surrey [150].

![Figure 4.22 - Configuration of the EA9321/steel TAST specimen (not to scale)](image)
The specimens consisted of two, thick, mild steel substrates bonded using a 0.45mm thick EA9321 layer. The substrates were grit blasted with white alumina abrasive grit and then degreased with acetone prior to bonding. The joints were cured at 25°C for 1 week and any excess adhesive at the edge of the adhesive layer was removed after curing. The tests were carried out at a constant crosshead speed of 0.1mm/min and at ambient temperature using an Instron 5500R machine fitted with a ±5kN loadcell. The extensometer and load data were transformed to provide shear strain and shear stress, respectively, using the adhesive overlap area and the adhesive layer loading area. The experimental shear stress-strain curves are shown in Figure 4.23 and were used later with the FE modelling in conjunction with the tensile adhesive data to determine parameters in the Drucker-Prager yield model of EA9321. The testing was carried out only for dry condition.

4.6 Single Lap Joint (EA9321/aluminium, EA9321/composite)

Single Lap Joints (SLJ) are used extensively to assess an adhesive system in industry. The experimental work presented in this section was undertaken by the partners at MBDA (MISSILE SYSTEMS)[151].
The two SLJ configurations studied are shown in Figure 4.24 (a) and (b). The substrates were aluminum alloy 7075-T6 (Figure 4.24(a)) and unidirectional carbon fibre epoxy composite IM7-8552 (Figure 4.24(b)). The fillet size at both lap ends was about 0.62mm/0.90mm by radius(r)/chord(c) as illustrated in Figure 4.25. The bondline thickness of the adhesive in the EA9321/aluminum joints was controlled to 0.2mm±15% and the bondline thickness of the EA9321/composite joints was 0.2mm±20%.

Figure 4.24 - (a)EA9321/7075-T6 Single lap joint geometry (not to scale); (b)EA9321/IM7-8552 Single lap joint geometry (not to scale)

Figure 4.25 - Fillet configuration of the EA9321 bonded SLJs (not to scale)

The aluminium substrates were chromic acid etched after degreased using ultrasonic agitation in acetone. After the etching the surfaces were rinsed and dried in an oven before bonding. The composite substrates were degreased with acetone before the adhesive was applied. The cured joints were then aged at 50°C, 95.8%RH for intervals of 0, 2, 4, 8, 12 (or 16) and 26 weeks before being withdrawn for testing. Four or five replicates were used for each ageing time. The tests were carried out
under ambient conditions at a constant displacement rate of 1 mm/min. The residual strengths of the joints obtained from the experiment are shown in Figure 4.26 and listed in Table 4.8 and Table 4.9, for the aluminium SLJs and the composite SLJs, respectively.

![Experimental failure load of the EA9321 bonded SLJs for the different ageing times](image)

**Figure 4.26 - Experimental failure load of the EA9321 bonded SLJs for the different ageing times [151]**

<table>
<thead>
<tr>
<th>Exposure, wk</th>
<th>Load, N</th>
</tr>
</thead>
<tbody>
<tr>
<td>0</td>
<td>10276</td>
</tr>
<tr>
<td>2</td>
<td>9586</td>
</tr>
<tr>
<td>4</td>
<td>8724</td>
</tr>
<tr>
<td>8</td>
<td>7773</td>
</tr>
<tr>
<td>12</td>
<td>7394</td>
</tr>
<tr>
<td>26</td>
<td>8195</td>
</tr>
</tbody>
</table>

Table 4.8 Experimental failure loads of the EA9321/aluminium single lap joints [151]

<table>
<thead>
<tr>
<th>Exposure, wk</th>
<th>Load, N</th>
</tr>
</thead>
<tbody>
<tr>
<td>0</td>
<td>10240</td>
</tr>
<tr>
<td>4</td>
<td>6619</td>
</tr>
<tr>
<td>8</td>
<td>5931</td>
</tr>
<tr>
<td>16</td>
<td>7228</td>
</tr>
<tr>
<td>26</td>
<td>6255</td>
</tr>
</tbody>
</table>

Table 4.9 Experimental failure loads of the EA9321/composite single lap joints [151]
Both experimental results demonstrated a reduction in bond strength over ageing
time, whilst the trend in the results for the composite SLJs was more pronounced
initially, suggesting that the bonded composite materials were more susceptible to
the environmental conditions than the aluminium samples. These data were used to
validate the predictive modelling in Chapters 6 and 7.

The failure mode of the aluminium joints was similar. Figure 4.27 shows that the
failure mode was a primarily cohesive failure occurring very close to the
adhesive-substrate interface. Figure 4.28 shows that, initially, the failure mode of the
EA9321/composite joints experienced some delamination of substrate at the interface
(Figure 4.28 (a)), however, after the two weeks ageing, the mode changed to be a
mixture of adhesive and delamination of substrate at interface (Figure 4.28 (b)). The
experimental observation was also used to validate the subsequent predictive
modelling using FEA.

Figure 4.27 - Typical aluminium single lap joint failure surface [151]
Chapter 4 Experimental testing

Figure 4.28 - Composite single lap joint failure surfaces [151]: (a) Dry; (b) Wet

4.7 3-point bending test of the EA9321/composite T joint

A more complex model element, an EA9321/composite T joint test piece in 3-point bend loading conditions, as shown Figures 4.29, was considered. This more closely resembled real bonding applications such as stiffeners used in the aerospace industry. The experimental work summarised in this section was undertaken by MBDA [151].

The full width T joint test piece consisted of a 4 mm thick unidirectional composite (IM7-8552) base plate, 100mm x 25 mm, and a 25 mm wide, 2 mm thick, multi-directional T-section with a base 50 mm long and a flange 25 mm high, as shown in Figure 4.30. Prior to bonding, the base plate and T-section were degreased with acetone, grit blasted with clean 80/120-alumina grit and then cleaned with acetone again. They were allowed to dry and then within a period of 4 hours, were bonded using EA9321 paste adhesive. The adhesive was spread as a thin film on the area to be bonded and the test specimens assembled onto the bonding fixture. Consolidation pressure was applied by the use of Hargreaves clamps. The adhesive was allowed to cure for 7 days at room temperature. There was a slight bow in the
T-section which made it impossible to get a constant thickness across the joint. A nominal adhesive layer thickness of 0.13mm was suggested by the joint manufacturer.

![3-point bend test rig set up for the EA9321/composite T joints](image)

**Figure 4.29 - 3-point bend test rig set up for the EA9321/composite T joints [151]**

![Composite T section of full width (25mm) joints with lay-up](image)

**Figure 4.30 - Composite T section of full width (25mm) joints with lay-up [151] shown in Table 4.10**
All bonded test pieces were placed in a desiccator for a minimum period of 1 week and then conditioned in an environment of 95%RH and 50°C for various periods of time. The withdrawal times were after 2, 4, 8, 26 and 52 weeks. After the required conditioning time, the specimens were removed from the environmental cabinet and sealed in plastic bags to cool down. Three joints were tested for each withdrawal.

The mechanical testing was performed on an Instron 4507 load frame, with a 200kN load cell. The testing was controlled using an Instron 4500 controller via a PC operating Instron Series IX data control software. The testing was performed using position control, using a test speed of 1mm/min. Rollers of 6 mm diameter were used with a centre-to-centre span of 70mm. In order to achieve a more accurate recording of the displacement, the position control was based on a Linear Variable Differential Transformer (LVDT) transducer connected between sample and the rig. This reduced the amount of slack recorded to an absolute minimum.

The test results of the EA9321 bonded composite T joints after environmental exposure durations are shown in Figure 4.31. It can be seen that initially, the bonding strength increased as exposure time increased. After 26 weeks exposure this trend began to reverse as the bonding strength was lower at 52 weeks than at 26 weeks; although it was higher than the initial values. It is possible that the moisture had a toughening affect upon the adhesives, with an increase in failure load of 24%. After 26 weeks the change in strength may be due to thermal degradation or relaxation of the creep, rather than absorbed moisture.
Chapter 4 Experimental testing

It was also observed that initially, the failure mode was cohesive between the edges of the T-section and the base plate, and then, during rapid failure, with the crack propagating towards the centre, the mode changed to a mixture of cohesive and delamination of the substrate at the interface. A typical 3-point bend test failure surface of the T joint is shown in Figure 4.32. It was found that the failure initiated at one of the two edges of the T-section /base plate interface and then ran on to the other edge as illustrated in Figure 4.33.

Figure 4.31 - Failure loads obtained from the experiment of the EA9321/composite T joint for a range of exposure times [151]

![Failure loads from the experiment of T joints](image)

Figure 4.32 - Typical 3-point bend test failure surface of a T joint degraded for 2 weeks at 50°C /95%RH [151]
Chapter 4 Experimental testing

2. Crack propagates towards other end along bond line

1. Crack initiates at one end

Bond line

Figure 4.33 - Showing direction of crack propagation during failure of the 3-point bend EA9321/composite T joint [151]

These experimental data were used to validate the predictive modelling in Chapter 9.

4.8 E32 bonded butt joint

A series of E32 bonded thin butt joints, exposed to a range of moisture, tested by Hambly [122], have also been used to validate the predictive modelling developed in this project. The substrate materials were mild steel and aluminium alloy 7075-T6. The two joint configurations are shown in Figure 4.34.

![Figure 4.34 - Geometry and loading configuration of the E32 bonded butt joints: (a) steel, bondline thickness = 0.48mm, width = 2.9mm; (b) aluminium, bondline thickness = 0.48mm, width = 2.5mm](image-url)
The adhesive layer thickness for all E32 bonded joints in this work was controlled by the use of spacers placed externally to the adhesive layer to prevent the impedance of water uptake. The slotted substrates in the butt joints provided a central bond surface with remote outer lands for placement of the spacers. A hole was also provided towards the end of the substrate and on its axial centerline for application of loading during measurement of residual strength. The steel substrates have been degreased in acetone, grit-blasted with alumina and degreased again using ultrasonic acetone bath, twice, before the bonding. The surface treatment for the aluminium substrates included Minco N24205 alkaline cleaner, DEF STAN 03-2 chromic acid etching and phosphoric acid anodising.

The joints were cured at 50°C in an oven for 48 hours before immersed in distilled water at room temperature. In addition to dry tests, it was decided to aim for two levels of saturation for each joint tested [122]. The immersion times used for the joints to achieve the required uptake levels are shown in Table 4.11. Based on 1D diffusion calculations these times would provide relative saturation levels of 0.125 and 0.25. In practice, the diffusion process was 2-dimensional and somewhat higher uptakes were achieved in these times.

<table>
<thead>
<tr>
<th>Butt Joint</th>
<th>Dry</th>
<th>Level 1</th>
<th>Level 2</th>
</tr>
</thead>
<tbody>
<tr>
<td>E32/steel</td>
<td>0</td>
<td>18.7</td>
<td>75</td>
</tr>
<tr>
<td>E32/aluminum</td>
<td>0</td>
<td>13.8</td>
<td>55</td>
</tr>
</tbody>
</table>

The loaded joints were macro-photographed in order to present locus of failure information in support of the visual estimates made for each test. The experimental failure loads are shown in Figure 4.35 and were compared to the modelling results in a later chapter. Cohesive failure in the adhesives was observed for all the tested joints as shown in Figures 4.36 and 4.37 as examples.
Figure 4.35 - Experimental failure loads of E32 bonded steel and aluminium butt joints (water at room temperature) [122]

Figure 4.36 - Macro-photograph of a dry E32/steel butt joint showing cohesive failure [122]

Figure 4.37 - Macro-photograph of a wet (75days) E32/steel butt joint showing cohesive failure [122]
4.9 Summary and conclusion

The aim of this research work was to develop models for prediction of adhesively bonded joints exposed to humid environments. Material data has been generated for input to the modelling work. A number of joint tests were undertaken in order to validate the models developed. The experimental data has been presented in this chapter and the modelling work is discussed in the following chapters.

The ingress of moisture in the adhesives and the composite was determined using bulk specimen with gravimetric experiments. It was found that the uptake response of EA9321 fitted well with a single Fickian diffusion model and the performance of E32 was best described by a two-stage Fickian model with the boundary condition. It was also found that both transverse and parallel diffusions of the composite were single Fickian. The coefficient of swelling (CHE) was obtained for EA9321 and the composite by measurement of the increase of a given gauge length of the specimen for various moisture levels. The CHEs were found to be of the same order of magnitude for both materials. The moisture dependent mechanical properties of the adhesives were characterised using tensile tests of bulk specimens exposed to different levels of moisture. Degradation was found in the saturated stress-strain curves for all three adhesives comparing with their dry states.

A series of the aluminium-FM73-composite bonded double lap joints (DLJ) were tested and accessed for a range of exposure time in moisture. Reduction in the residual strength of the joints due to the moisture absorption of 80.1%RH and 95.8%RH over 52 weeks was obtained whilst a slight increase in the bonding strength was observed for the joints controlled in a dry, hot environment. The failure surface showed a slightly increasing cohesive failure with increasing ageing time.

A mixed mode flexure (MMF) test was undertaken for EA9321 to prepare data required by the calibration modelling for the failure parameters of the adhesive. A thick adherend shear test (TAST) of EA9321 was adopted to characterise the shear stress-strain relationship of the adhesive as complement of standard tensile test. A
series of the EA9321/aluminium and EA9321/composite single lap joints (SLJ) were tested for a range of moisture exposure conditions. Primarily cohesive failure was found in most cases. The experimental data were compared with the predictive modelling results in Chapter 6 and Chapter 7, using the two different cohesive failure models.

A series of the complex EA9321/composite T joints exposed for moisture were tested under a 3-point bending condition. An increasing trend of the bonding strength over ageing time was found in the results. A mixed failure mode of cohesive and delamination of the substrate at the interface was mainly observed throughout the tested joints. A corresponding FE modelling of the joint test was carried out in Chapter 9.

An E32/steel butt joint and an E32/aluminium butt joint were tested under three levels of moisture concentration. Cohesive failure in the adhesives was observed for all the tested joints. A cohesive failure model of the joint test using FE modelling was demonstrated in Chapter 10.
CHAPTER 5

VALIDATION MODELLING OF FM73 AND EA9321 BONDED SINGLE LAP JOINTS

The purpose of these validation studies was to establish confidence in the moisture dependent non-linear adhesive models to be used in subsequent predictive modelling in this research. The experimental data was obtained by a colleague at BAe systems [152] (as part of the same research programme), using image processing of miniature specimens tested in a straining stage under a microscope. This gave bondline deformation and local strain mapping which can be compared with the finite element analysis (FEA) results to determine the most appropriate modelling strategies.

A series of stress analyses of FM73 and EA9321 bonded single lap joints were carried out using FEA with two elastic-plastic materials models (von Mises and linear Drucker-Prager) for both the dry and an aged condition. Both 2D (plane stress and plane strain) and 3D FE models were created and analysed for all the joints. The difference between the models was discussed. The results of the 3D models were mainly presented in the thesis as the most accurate simulations. The FEA solutions were compared with experimental results and conclusions drawn.
5.1 Introduction

The single lap joints manufactured with two adhesive systems (FM73 and EA9321) and two substrates (aluminium and composite) have been studied. This gave a total of four different joint configurations. These joints were tested in the un-aged condition and after exposure to controlled hot/moisture environments (50°C/distilled water for FM73 joints and 50°C/95%RH for the EA9321 joints) [152].

The results of this study are presented in section 5.3 as follows:

5.3.1 – Aluminum/FM73 – dry
5.3.2 – Composite/FM73 – dry
5.3.3 – Composite/EA9321 – dry
5.3.4 – Aluminum/EA9321 - wet
5.3.5 – Composite/EA9321 – wet
5.3.6 – Composite/EA9321 – wet

These are preceded by section 5.2, which outlines the configurations and material properties in more detail and gives information about the FE modelling including the material models used.

5.2 Finite element model and material property

The FE code ABAQUS 6.4 was used in this work. A series of models were created for the different single lap joints shown in Table 5.1.

5.2.1 FE models

A typical 2D model and mesh refinement in the overlap is shown in Figure 5.1. Due to joint symmetry, only half the specimen was modelled. Rotational symmetry
conditions were applied on the mid-plane of the joint. A typical 3D model is shown in Figure 5.2. Only a quarter of the joint was modelled due to the symmetry.

Table 5.1 Single lap joints configuration and model details [152]

<table>
<thead>
<tr>
<th>Configuration</th>
<th>Bondline thickness</th>
<th>Adherend thickness</th>
<th>Overlap</th>
<th>Specimen Width</th>
<th>Mesh refinement</th>
</tr>
</thead>
<tbody>
<tr>
<td>a) FM73/7075-T6 Aluminum, With fillet</td>
<td>23µm</td>
<td>3.125mm</td>
<td>12.5mm</td>
<td>0.42mm</td>
<td>0.025*0.025</td>
</tr>
<tr>
<td>b) FM73/7075- T6 Aluminum, Without fillet</td>
<td>23µm</td>
<td>3.125mm</td>
<td>12.5mm</td>
<td>0.5mm</td>
<td>0.025*0.025</td>
</tr>
<tr>
<td>c) FM73/IM7-8552 Composite, With fillet</td>
<td>27µm</td>
<td>2.06mm</td>
<td>12.5mm</td>
<td>0.49mm</td>
<td>0.0245*0.025</td>
</tr>
<tr>
<td>d) FM73/IM7-8552 Composite, Without fillet</td>
<td>27µm</td>
<td>2.06mm</td>
<td>12.5mm</td>
<td>0.41mm</td>
<td>0.0245*0.025</td>
</tr>
<tr>
<td>e) EA9321/IM7-8552 Composite, Without fillet</td>
<td>27µm</td>
<td>2.06mm</td>
<td>12.5mm</td>
<td>0.54mm</td>
<td>0.0245*0.025</td>
</tr>
</tbody>
</table>
8-noded and 20-noded quadratic elements with full integration points were used for the 2D and 3D validating models respectively. Both plane stress and plane strain elements were considered for the 2D models and the results were compared with the 3D models. The 3D modelling results were further compared with the experimental data. Both material nonlinearity and geometry nonlinearity were considered in the modelling and implicit analysis has been used successfully. The displacement distribution along the interface of the adhesive and the substrates from the FE models were plotted and compared with the experimental results at the same loading levels. The contours of strain distribution in the fillets were also compared with the selected experimental observations.
5.2.2 Material models

From the geometry in Table 5.1, it was seen that all specimens were thin (0.41mm–0.54mm). The adhesive in the exposed joints was fully saturated and hence no diffusion modelling was required. The stress-strain curves of the dry and saturated (95.8%RH) EA9321, as shown in Figure 4.3, can thus be used directly in the joint modelling. However, the moisture dependent mechanical properties of FM73 shown in Figures 4.1 were obtained from an ageing condition of 50 °C /95.8%RH, rather than 50 °C / water. A stress-strain curve for the FM73 immersed in water at 50 °C was then extrapolated from the other data, as shown in Figure 5.3. Poisson ratio was 0.4 and 0.36 for FM73 and EA9321 respectively.

Figure 5.3 - Moisture dependent tensile properties used for FM73 in the modelling

The substrates used to bond the joints were aluminum alloy 7075-T6 and the unidirectional carbon fibre epoxy composite (IM7-8552). The mechanical properties of the substrates obtained from the standard tests have been shown in Figure 4.7 and Table 4.3 respectively. The swelling effect of the materials was not included in the modelling.
Von Mises and linear Drucker-Prager elastic-plastic models were both used for the adhesives. The tensile test stress-strain curves were used to define the hardening behaviour. Drucker-Prager requires another two parameters, which are the ratio of the flow stress in triaxial tension to the flow stress in triaxial compression, $K$, and the material angle of friction, $\beta$, as shown in Figure 5.4 and Equations 5.1-5.3 [146]. These were deduced based on FM73 compression tests results (obtained by QinetiQ), as shown in Figure 5.4 (b) in addition to the uniaxial tensile data.

![Figure 5.4 - Linear Drucker-Prager model: (a) yield surface and flow direction in the $p$-$t$ plane [146]; (b) fitted curve with experimental data of FM73 (from QinetiQ) with $K=0.8, \beta=23.0^\circ$](image)

The linear Drucker-Prager criterion as shown in Figure 5.4 is written as:

$$F = t - p \tan \beta - d = 0$$  \hspace{1cm} (5.1)

where

$$t = \frac{1}{2} q \left[ 1 + \frac{1}{K} \left( 1 - \frac{1}{K} \left( \frac{r}{q} \right)^2 \right) \right]$$ \hspace{1cm} (5.2)

and the cohesion of the material
\[ d = \left( \frac{1}{K} + \frac{\tan \beta}{3} \right) \sigma_t = \left( 1 - \frac{\tan \beta}{3} \right) \sigma_c \] (5.3)

\( p, q \) are the equivalent pressure stress and the Mises equivalent stress, respectively.
\( \sigma_c, \sigma_t \) are the uniaxial compressive yield stress and uniaxial tensile yield stress, respectively.

For EA9321, the shear data obtained from a thick adherend shear test (TAST) (shown in Figure 4.23 [150]) was used in conjunction with the uniaxial tensile data to find the Drucker-Prager parameters. The joint geometrical configuration has been shown in Figure 4.22. A 2D FE model of the joint is shown in Figure 5.5.

![Figure 5.5 - FE model of the EA9321/steel TAST (smallest mesh size: 0.225mmx0.2mm)](image)

Due to the symmetry of the joint configuration, only half the specimen was modelled. The mesh was generated using plane strain four-noded quadrilateral elements with a minimum element size of 0.225mmx0.2mm as shown in Figure 5.5. Rotational symmetry was applied to a section through the middle of the overlap. Only undamaged material models were used in this part. Nonlinear material and geometry were both incorporated. Implicit analysis was adopted using ABAQUS Standard. Following the FM73 adhesive experimental results, the ranges of the parameters to be investigated were set to be: \( K = 0.8 \) to 1.0 and \( \beta = 10^\circ \) to \( 30^\circ \). A calibration study was undertaken and the results are shown in Figure 5.6. The elastic modulus and Poisson's ratio used for the steel substrates were 207GPa and 0.33 respectively.
It can be seen that all the modelled stress-strain curves matched the average experimental results very well before yielding. This implied that the linear part of the models were reliable. Moreover, it was found that the yield stress was increased with increases in both $\beta$ and $K$. This was expected for $\beta$, with an increasing pressure stress (i.e. more compressive) as illustrated in Equations 5.1. Also, equation 5.2 has shown such relationship between $K$ and the Mises equivalent stress $\sigma$ irrespective of the hydrostatic stress. The best fit curve lay between the parameter sets of $K=1.0/\beta=20^\circ$ and $K=0.9/\beta=20^\circ$. More combinations of these two parameters were studied using this method and the value of $K=0.9/\beta=25^\circ$ was then selected as shown in Figure 5.7.
It was assumed that the Drucker-Prager parameters for both FM73 and EA9321 were moisture independent.

5.3 Stress analysis

A comparison of 2D plane stress, 2D plane strain and 3D models for the same joint configurations have been presented in the following subsection (5.3.1). Some general conclusions have been drawn. The experimental data were mainly compared with the 3D modelling results in subsequent sections as most accurate models.

5.3.1 FM73/aluminium single lap joint, dry

Relative substrate displacements at two loading levels obtained from the 2D and 3D models of the joint without a fillet are compared in Figure 5.8. Displacement of the 3D model in this chapter was taken from the free side, corresponding to the experiment. Only the results using von Mises model were presented for the sake of comparison.

Comparing the FE predictions at the two loading levels it can be seen that at low load there was little difference between the three models. This would be expected as there
was limited deformation at this level of load. As the load increased the plane stress model produced greater displacements than the plane strain model. This was because strain was suppressed in the plane strain model. The shear displacement \((U_x)\) produced by the 3D model tended to match the plane stress model very well whilst the peel displacement \((U_y)\) matched the plane strain model much better. This did not agree with the conventional view that plane stress is better than plane strain when the modelled configuration is very thin \((0.41\text{mm} - 0.54\text{mm})\). This may be because that shear strain in the single lap joint model tended to be greater than peel strain.

A comparison of the modelling results from the three models of the joint with fillet is shown in Figure 5.9. Again, the same trends can be seen when comparing the results at the two loading levels (good correlation at low load and at higher load the plane stress model giving higher displacements). In this case, both displacements \((U_x\text{ and } U_y)\) produced by the 3D model seemed to be midway between those produced by the plane stress and plane strain models.

![Figure 5.9 - Interface displacement difference along the overlap as a function of load for FM73/Aluminum (Dry, with fillet, von Mises)](image)

The same comparison has been carried out for the other joints at all experimental recorded loading levels in this research. The 3D modelling results were found to be between the two 2D models in all the cases. The plane stress model generally gave better correlation with the 3D model in the shear displacement \((U_x)\) whilst the plane strain model seemed better for the peel displacement \((U_y)\). However, this was not
consistent for all the models. The same results were obtained from similar comparison analyses using the Drucker-Prager model. A conclusion can be drawn that neither plane strain nor plane stress model can produce an accurate result. Thus only the 3D modelling results have been presented and compared with the experimental data in the rest of this chapter.

A comparison of the relative substrate displacements between the 3D model and the experimental data for the filleted joint is shown in Figure 5.10. The two material models, von Mises and linear Drucker-Prager, were also compared in this work.

![Figure 5.10 - Interface displacement difference along the overlap as a function of load for FM73/Aluminum (Dry, 3D, with fillet)](image)

It can be seen that at low loads there was little difference between the von Mises (VM) model prediction and the Drucker-Prager (DP) model prediction. This was expected as there was limited plasticity at these levels of load. As the load increased the VM model produced a greater shear displacement (Ux) than the DP model. It is likely that the yielding was somewhat suppressed with the DP model. The DP model produced a higher peel displacement (Uy) than the VM. Both these trends (Ux and Uy) can be consistently seen in all the model results presented in this chapter. Comparing the FE and experimental data it can be seen that at low loads the match between the two was good and this established confidence in the FM73 linear material models used. At higher loads it can be seen that the DP model corresponded
more closely to the experimental data. This implied that the incorporation of hydrostatic stress in the adhesive was important for this adhesive.

Figure 5.11 shows a comparison between the experimental and predicted substrate displacement differences for the non-filleted joint. Exactly the same trends can be seen when comparing the VM and DP FE results (good correlation at low loads and at higher loads VM giving higher shear displacements (Ux) and DP giving higher peel displacements (Uy)). Comparison between experimental and FE results seemed a little worse. The experimental displacements now exceeded the predicted ones. This could be explained if the non-fillet assumption was not exactly true as an absolute clean bond was known to be practically difficult. It should be mentioned that although a von Mises elastic-plastic model has been used for aluminum it never yielded under the experimental load levels studied.

![Graph showing interface displacement difference along the overlap as a function of load for FM73/Aluminum (Dry, 3D, without fillet)](image)

When the adhesive has not damaged, the strain distribution maps from the experiments (Figure 5.12) can be compared with the strain contours from the FE models, as shown in Figure 5.13. As with the displacement measurement, the contours on the free side of the 3D model were chosen for the comparison with the experimental data.
Due to the singularity at the fillet corner and differences in resolution, it was difficult to directly compare the sets of contours from the experiment and the modelling. A simple way to overcome the singularity problem was to average the values of the strains around the corner region. An area of about 0.05mm x 0.1mm was chosen to calculate the average maximum strain around the corner as shown in Figure 5.13. ABAQUS outputs shear strain as engineering shear strain while the experiment has used a mathematical definition. This means the FE solution should be twice the experimental results (when comparing their shear strains). Thus, it was found that the average maximum strain values in the fillet obtained from the FE model matched the
experimental data quite well. To show this comparison clearly, the maximum strain values of \( \varepsilon_{xy} \), \( \varepsilon_x \) and \( \varepsilon_y \) from the experiment and the modelling are listed in Table 5.2.

### Table 5.2 - Comparison of the maximum strains of the adhesive at the load of 132N/mm (average over a 0.05mmx0.1mm area around the corner)

<table>
<thead>
<tr>
<th>Maximum Strain</th>
<th>( \varepsilon_{xy} )</th>
<th>( \varepsilon_x )</th>
<th>( \varepsilon_y )</th>
</tr>
</thead>
<tbody>
<tr>
<td>Experiment</td>
<td>0.01</td>
<td>0.01</td>
<td>0.005</td>
</tr>
<tr>
<td>FE Modelling</td>
<td>0.018</td>
<td>0.011</td>
<td>0.005</td>
</tr>
</tbody>
</table>

### 5.3.2 FM73/composite single lap joint, dry

Again, joints with and without fillets were studied experimentally and numerically (using 3D model). The results for the filleted joint are discussed first. The experimental and predicted relative substrate displacements are shown in Fig 5.14.

![Figure 5.14](image)

**Figure 5.14- Interface displacement difference along the overlap as a function of load for FM73/Composite (Dry, with fillet)**

Exactly the same trends can be seen when comparing the VM and DP FE results (good correlation at low loads and at higher loads VM giving higher shear displacements (Ux) and DP giving higher peel displacements (Uy)). Comparison between experimental and FE results show that the shear displacements (Ux) of the DP model matched the experimental data quite well at all recorded loads whilst the
experimental peel displacements (Uy) were lower than the corresponding predictions. Different predicted values would occur if the fillet size of the joint has been changed. Since no accurate measurement had been taken before testing, the modelled geometry of the joint fillet studied in this report has been assumed based on experience and the images taken from the experiment results. However it is unlikely that fillet size would account for the significant differences experienced between the experimental and predicted the peel displacements.

It should be stated that the FE results were broadly consistent with the other results cited in this chapter whilst the experimental peel displacements (Uy) shown in Figure 5.14 were considerably lower than corresponding data from other tests. Another possible reason for the experimental displacements being lower than the predicted values was that delamination may have occurred in the composite. This would leave the bondline less deformed under loading. The fact that the differences increase with increasing level of load might support this idea.

It is interesting to compare these results with the results from the joints with no fillets, as shown in Figure 5.15. Exactly the same trends can be seen when comparing the VM and DP FE results (good correlation at low loads and at higher loads VM giving higher shear displacements (Ux) and DP giving higher peel displacements (Uy)). The shear displacement (Ux) of the VM at higher load matched the experimental data better than the DP. The experimental peel displacements (Uy) were slightly higher than the two FE modelling results and, specifically, much higher than the correspondingly abnormally low vales shown in Figure 5.14. It may be inappropriate to conclude that the VM was a better model than DP as there was evidence of damage in the experimental joints at high loads and this was not included in the FE modelling. It was possible that incorrect excessive yielding in the VM model compensated for not including damage in the FE models in this case. Again this established confidence in modelling joints with FM73 adhesive.
The strain map images obtained from the experiment on the FM73/composite joints were also studied and are shown in Figure 5.16. It can be seen that the maps for $\varepsilon_x$ and $\varepsilon_y$ contained some strange artifacts which made it difficult to obtain useable values of maximum strain. Thus only the shear strain map $\varepsilon_{xy}$ was compared with the FE results. As with the FM73/aluminum joints, an averaging technique was used to overcome the singularity problem in the FE modelling.

![Figure 5.15- Interface displacement difference along the overlap as a function of load for FM73/Composite (Dry, without fillet)](image)

![Figure 5.16 - Experimental strain field maps of fillet of the dry FM73/Composite single lap joint as a function of load: (a) $\varepsilon_{xy}$ ; (b) $\varepsilon_x$ ; (c) $\varepsilon_y$](image)
The contours from the von Mises model and the Drucker-Prager model are shown in Figure 5.17 and 5.18 respectively. It was seen that the shear strain distribution obtained from the experiment and the FE model were very comparable, (considering twice the mathematic shear strain corresponds to engineering shear strain). The strain levels from the DP model were slightly lower than those from the VM model. This was consistent with the trends shown in the shear displacement ($U_x$) plots. The maximum strain values of $\varepsilon_{xy}$ from the experiment and the modelling are listed in Table 5.3 for clarity.

![Figure 5.17 - Engineering shear strain distribution in the fillet of the dry FM73/Composite single lap joint at the different load levels (von Mises adhesive)](image1)

![Figure 5.18 - Engineering shear strain distribution in the fillet of the dry FM73/Composite single lap joint at the different load levels (Drucker-Prager adhesive)](image2)

<table>
<thead>
<tr>
<th>LE, LE12</th>
<th>(Ave. Crit.: 75%)</th>
<th>LE, LE12</th>
<th>(Ave. Crit.: 75%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>+3.450e-02</td>
<td>+8.526e-02</td>
<td>+3.146e-02</td>
<td>+7.744e-02</td>
</tr>
<tr>
<td>+2.841e-02</td>
<td>+6.961e-02</td>
<td>+2.577e-02</td>
<td>+6.179e-02</td>
</tr>
<tr>
<td>+2.233e-02</td>
<td>+5.396e-02</td>
<td>+1.928e-02</td>
<td>+4.614e-02</td>
</tr>
<tr>
<td>+1.624e-02</td>
<td>+3.831e-02</td>
<td>+1.320e-02</td>
<td>+2.866e-02</td>
</tr>
<tr>
<td>+1.015e-02</td>
<td>+3.049e-02</td>
<td>+7.11e-03</td>
<td>+1.484e-02</td>
</tr>
<tr>
<td>+2.068e-03</td>
<td>+7.012e-03</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

a) 105N/mm: Max Avg$^*$$=0.0209$

b) 202N/mm: Max Avg$^*$$=0.0463$

![a) 105N/mm: Max Avg$^*$$=0.0199$](image3)

![b) 202N/mm: Max Avg$^*$$=0.0434$](image4)

![Figure 5.17 - Engineering shear strain distribution in the fillet of the dry FM73/Composite single lap joint at the different load levels (von Mises adhesive)](image1)

![Figure 5.18 - Engineering shear strain distribution in the fillet of the dry FM73/Composite single lap joint at the different load levels (Drucker-Prager adhesive)](image2)

Table 5.3 - Comparison of the maximum shear strains in the adhesive for FM73/Composite single lap joint at the loads of 105N/mm and 202N/mm

<table>
<thead>
<tr>
<th>Maximum shear strain $\varepsilon_{xy}$</th>
<th>105N/mm</th>
<th>202N/mm</th>
</tr>
</thead>
<tbody>
<tr>
<td>Experiment</td>
<td>0.01</td>
<td>0.017</td>
</tr>
<tr>
<td>Von Mises</td>
<td>0.0105</td>
<td>0.0232</td>
</tr>
<tr>
<td>Drucker-Prager</td>
<td>0.0099</td>
<td>0.0217</td>
</tr>
</tbody>
</table>
5.3.3 EA9321/composite single lap joint, dry

Only the non-filleted version of this joint was tested experimentally. The adherend displacement difference obtained from the experiment and the 3D FE models are shown in Figure 5.19.

![Figure 5.19 - Interface displacement difference along the overlap as a function of load for EA9321/Composite (Dry, without fillet)](image)

Exactly the same trends can be seen when comparing the VM and DP FE results (good correlation at low loads and at higher loads VM giving higher shear displacements (Ux) and DP giving higher peel displacements (Uy)). The correlation between the experimental and predicted displacements was reasonable. The experimental shear displacement (Ux) was slightly lower than the predicted values at low loads but this difference reduced with increasing load for the DP models. The experimental peel displacements, matched the predicted values at low loads but actually increased with increasing load more quickly than the predicted values. This may be due to a loss of bonding, thus allowing greater substrate displacements. This good correlation generally led to confidence in the modelling of EA9321 joints in addition to the FM73 joints with the DP model being seen to give a better fit to the experimental data.
Having established confidence in the modelling of dry FM73 and EA9321 joints we now focus on the modelling of the aged joints.

5.3.4 FM73/aluminium single lap joint with fillet, 50°C/water

As mentioned before an elastic-plastic stress-strain data for FM73 aged in water at 50°C was extrapolated from the data for FM73 aged at 50°C/95%RH, as shown in Figure 5.3. The von Mises and the Drucker-Prager material models were both used and the predictions for the differential substrate displacement were compared with the experiment results and are shown in Figure 5.20. These can be compared with the corresponding dry plots shown in Figure 5.10, where the correlation between predicted and experimental results was good.

![Graph of interface displacement difference along the overlap as a function of load for FM73/Aluminum (50°C/water, with fillet)](image)

Figure 5.20 - Interface displacement difference along the overlap as a function of load for FM73/Aluminum (50°C/water, with fillet)

Exactly the same trends can be seen when comparing the VM and DP FE results (good correlation at low loads and at higher loads VM giving higher shear displacements (Ux) and DP giving higher peel displacements (Uy)). The correlation between the experimental and predicted displacements was not as good as in the case of the dry joints. The predicted results were slightly higher than the dry joint predictions at high levels of load. This was to be expected as the adhesive became plasticised by the moisture and hence became more flexible. On the other hand the
experimental data showed no increase when compared with the dry data and in the case of the peel strain \((U_y)\) seemed to exhibit smaller displacements. This experimental behaviour could be considered to be anomalous. It was also possible, however, that the FM73 material data was over extrapolated and that slightly smaller reductions to the dry response should be applied.

### 5.3.5 FM73/composite single lap joint with fillet, 50°C, 95%RH

Only a filleted version of this joint was tested experimentally. The differential substrate displacements for this joint are shown in Figure 5.21. They can be compared with the corresponding dry joint data, shown in Figure 5.14.

![Figure 5.21- Interface displacement difference along the overlap as a function of load for FM73/Composite (50°C, 95%RH, with fillet)](image)

It can be seen from Figure 5.17 that, once again, exactly the same trends can be seen when comparing the VM and DP FE results (good correlation at low loads and at higher loads VM giving higher shear displacements \((U_x)\) and DP giving higher peel displacements \((U_y)\)). The correlation between the experimental and predicted displacements was a little worse than the correlation found with the corresponding dry joints (Figure 5.14). As in the last section, this could be at least partially attributed to the stress-strain data for FM73 being over extrapolated. However, as with the dry joint data, the experimental peel displacements were really rather
anomalous. Indeed, the peel displacements of both the wet and dry FM73/composite single lap joints with fillets did appear to be rather inconsistent with the rest of the experimental data. However, the shear data (Ux) predictions were really quite reasonable.

5.3.6 EA9321/Composite single lap joint without fillet, 50°C, 95%RH

Only a non-filleted version of this joint was tested. The differential substrate displacements for this joint are shown in Figure 5.22. These can be compared with the corresponding dry joint data, shown in Figure 5.19.

Once again, exactly the same trends can be seen when comparing the VM and DP FE results (good correlation at low loads and at higher loads VM giving higher shear displacements (Ux) and DP giving higher peel displacements (Uy)). The correlation between the experimental and predicted displacements was good, establishing confidence in the degraded EA9321 material properties being used in the analyses.

There was some suggestion from the shear displacements (Ux) that the DP model is better than the VM model. There was also similar evidence in Figures 5.10, 5.14 and 5.19. It was possible that yielding with VM is not sufficiently suppressed. It has been
found that in some analyses it was not possible to sustain the maximum failure loads, probably due to global yielding of the joint and this effect was likely to be more pronounced with VM rather than DP. This is an aspect that has been considered further in later chapters.

5.4 Summary and conclusion

Several sets of FM73 and EA9321 bonded single lap joints have been analysed using FE modelling with both 2D (plane strain and plane stress) and 3D models. Comparison of these three models for each joint (both dry and conditioned) has found that the 3D modelling results were always between the two 2D solutions. In some cases, the plane stress model showed better correlation for the shear displacement (Ux) prediction and the plane strain model seemed better for the peel displacement (Uy). However, this cannot be taken as a rule for all the joints. Both von Mises and a linear Drucker-Prager model were studied and compared for the three models. It has been concluded that neither the plane strain nor plane stress model could produce the same accurate results comparing with the 3D model.

The solutions obtained from the 3D model were compared with the corresponding experimental results. Both dry and degraded joint conditions were considered. Two elastic-plastic materials models, von Mises and Linear Drucker-Prager, were both used and compared. The following specific conclusions can be drawn:

- Dry FM73 constitutive data seems good
- Degraded FM73 constitutive data may be over extrapolated
- Dry EA9321 data seems reasonable
- Degraded EA9321 data seems good
- Some evidence that Drucker-Prager is more appropriate than von Mises

It is noteworthy that joint damage (debonding and delamination) may well have resulted in a lack of correlation in a few cases. The FE modelling has been demonstrated as a useful method to model adhesively bonded joints.
As reviewed in Chapter 2, two main types of failure, interfacial and cohesive, are commonly found for adhesive joints: failure sites are at the adhesive/substrate interface, or cohesive within the adhesive respectively. A cohesive zone model has been successfully employed to predict the interfacial failure in AV119 bonded single lap joints in Chapter 3. It has focused on modelling material separation using a predefined crack propagation path and an interfacial rupture element. However, the method cannot be used for modelling progressive cohesive failure because the locus of damage in the adhesive cannot always be predefined. Such cohesive failure is often found in those joints bonded with a ductile adhesive such as EA9321.

In this chapter, a progressive cohesive failure model is proposed to predict the residual strength of adhesively bonded joints using FE modelling. Joints bonded with the ductile adhesive EA9321 were studied for a range of environmental degradation. A single moisture-dependent failure parameter, the critical strain, was calibrated using an aged mixed mode flexure (MMF) test. This failure parameter was then used without further modification to model failure in aluminum and composite single lap joints (SLJ) bonded with the same adhesive. The FEA package ABAQUS was used
Chapter 6 Predictive modelling of EA9321 single lap joints using a strain-based failure model

to implement the coupled mechanical-diffusion analyses required. The elastic-plastic response of the adhesive and the substrates, both obtained from the bulk tensile tests, were incorporated. Both 2D and 3D modelling was undertaken and the results compared. Residual strain was not considered. Experimental work related to this chapter has been presented in Chapter 4.

6.1 Strain-based failure model calibration

Cohesive failure is often found in well made joints bonded with ductile adhesives such as EA9321. It is often difficult to locate the damage initiation point and propagation path. A failure model coded within ABAQUS [146] can, not only predict the strength, but also the damage initiation and propagation within an adhesive joint. This strain-based cohesive failure model is the simplest method of modelling progressive continuum damage and failure in adhesively bonded joints. The only parameter required in this method is the moisture dependent critical failure strain. A mixed mode flexure (MMF) configuration was used to calibrate this parameter at various moisture levels and then this moisture-dependent calibrated failure strain was used for predictive modelling of the SLJs without any modification.

6.1.1 Strain-based failure model and critical strain

In this strain-based progressive damage failure model, the only parameter required is the moisture dependent critical failure strain, which needs to be calibrated before use in predictive modelling. The material response followed the non-linear constitutive response until the equivalent plastic strain (corresponding to Mises equivalent stress) reached a critical value at any element integration point. To accurately describe the stress and strain distribution in the adhesive, the experimental moisture-dependent elastic-plastic material properties of the adhesive were incorporated into the model. Then a critical maximum equivalent plastic strain was added, as a failure measure, to restrict the stress-carrying capability of the elements, as shown in Figure 6.1. It is worth noting that this failure model can only be used in conjunction with von Mises yielding and ABAQUS Explicit [146].
When an element integration point reached this predefined critical strain, all the stress components will be set to zero and the material point fails. An element failed when all its integration points failed and showed as a void in the mesh of the model. The stress that was carried by this element was redistributed to the adjacent elements and the damage propagated. The whole joint failed once a path of failed elements extended over the entire overlap. The residual strength is the maximum load carried by the joint. One problem with this method is mesh dependence, observed when analysing configurations with singular stress fields. This aspect is discussed later.

### 6.1.2 MMF calibration of critical strain for EA9321

As tested in Chapter 4 (section 4.3), the MMF configuration (shown in Figure 4.19) has been chosen to calibrate the moisture dependent critical strain parameter of the adhesive. It consisted of aluminium alloy substrates bonded with a 0.5mm thick EA9321. Three moisture concentration levels, dry, 2.1% and 3.85%, were considered. (The latter two corresponded to saturation in the two environments used.) The MMF was a significantly different configuration to the SLJ specimens tested (section 4.6) and hence a good test of the general applicability of the cohesive failure model. Both 2D and 3D modelling were studied.

#### 6.1.2.1 2D MMF calibration

A 2D FE model of the MMF joint with elements in the adhesive layer of 0.05mm x 0.05mm was created first and is shown in Figure 6.2. Plane strain four-noded
quadrilateral elements were used. The moisture dependent material property of the adhesive was calculated using linear interpolation between the dry and saturated data shown in Figure 4.3 (section 4.1.2). The mechanical property of the aluminium substrates was shown in Figure 4.7 (section 4.1.4). Explicit analysis has to be used when implementing this failure model in ABAQUS [146]. A mass scaling factor of $1 \times 10^5$ was used to prevent dynamic instability. This value provided a time efficient solution but did not modify the accuracy of the static analyses. Nonlinear geometric behaviour was also included in the modelling. The critical parameters were determined by comparing the predicted failure loads with the experimental data. The calibrated results for the MMF strength as a function of moisture uptake, normalised by the saturation value $m_- = 3.85\%$, is shown in Figure 6.3.

![Figure 6.2 - FE model of the MMF with mesh refinement along the adhesive overlap](image)

It can be seen in Figure 6.3 that the critical failure strain reduced with moisture content. This was not apparent in the adhesive tensile data presented in Figure 4.3. From both the MMF and the SLJs discussed later there was evidence that the failure, although still mainly cohesive, shifted towards the interface. It was thus conjectured that in a joint the wet adhesive failed at a strain below the value measured in the bulk tensile testing, shown in Figure 4.3. The reason for this is not certain but one possibility is that the adhesive at the interface was degraded preferentially; maybe the molecular structure adjacent to the interface was more susceptible to moisture.
These critical strains determined from the data at the original crack length (20mm) have been used to predict the measured fracture loads at increasing crack lengths in the MMF tests (shown in Figure 4.20). The predicted results fit the experimental data very well, as shown in Figure 6.4. This established the confidence in the predictive modelling techniques.

Figure 6.4 - Experimental failure load of MMF joints at different crack lengths (markers) and predicted failure load of MMF specimens using the calibrated failure strain (lines)
As expected, it was found that the moisture dependent critical strain was also mesh-dependent. The calibrated results for different mesh schemes for the dry condition are shown in Figure 6.5(a). This mesh-dependence clearly arose from the singularity existing around the embedded bi-material corner.

![Critical strain calibration for MMF specimens at different moisture concentration levels for different mesh schemes (m=-3.85%): (a) undegraded; (b) degraded](image)

Further study has found that there was a proportional relationship for the calibrated failure strain between the different mesh schemes at each moisture concentration level, as illustrated in Equation 6.1.

\[
\frac{\varepsilon_{c,m}^1}{\varepsilon_{c,m}^2} = \frac{\varepsilon_{c,\text{dry}}^1}{\varepsilon_{c,\text{dry}}^2}
\]  

where \( \varepsilon_{c,\text{dry}} \) represents critical strain of the dry adhesive, \( \varepsilon_{c,m} \) is critical strain of a wet adhesive and the superscript numbers 1 and 2 denote different mesh schemes. Much modelling and computational work for the cohesive failure model can be saved by using Equation 6.1.

The moisture dependent critical strain calibration results of the MMF specimens for the three different mesh schemes are shown in Figure 6.5(b). These values were used without further modification to model cohesive failure in the single lap joints bonded
with the same adhesive. The MMF calibrated strain (shown in Figure 6.5) has then been used directly to predict the failure loads of the same adhesive system but in a single lap joint configuration with different substrates.

6.1.2.2 3D MMF calibration

A 3D FE model of the MMF joint with elements in the adhesive layer of 0.1mmx0.1mmx0.5mm was then created and is shown in Figure 6.6.

![Figure 6.6 - 3D model of the EA9321/aluminum MMF test and local mesh refinement (smallest mesh size: 0.1mmx0.1mmx0.5mm)](image)

Half of the specimen was modelled due to the symmetry of the MMF configuration in the transverse direction. Eight-noded quadratic elements were used in this model. The same moisture dependent material property of the adhesive shown in Figure 4.3 and the mechanical property of the aluminium substrates shown in Figure 4.7 were used. Explicit analysis and a mass scaling factor of $1 \times 10^5$ were incorporated for the model. Nonlinear geometric behaviour was also included. The calibrated critical strains of the 2D model with 0.1mmx0.1mm mesh refinement (blue line in Figure 6.5(b)) were used first in the trial of this 3D calibration. It was found that the predicted failure loads of the MMF joint were only slightly higher (2-3%) than the experimental data and the predicted results from the 2D calibration, as shown in Figure 6.7. This showed that the residual strength of the MMF joints was mainly
determined by the planar response of the joints. Thus, the same moisture-dependent critical strains used in the 2D modelling were used in the 3D modelling of the SLJs with the same mesh scheme along the adhesive layer on the plane parallel to the loading. The failure loads of the MMF tests at different crack lengths with different moisture contents were also predicted using 3D modelling and these calibrated strains. The same good correlation between the predicted results and the experimental data was obtained. It is worth noting that residual strain was not considered in this research.

![Figure 6.7 - MMF predictions from the 2D and 3D modelling using the same set of critical strain (smallest mesh size: 0.1mmx0.1mmx0.5mm)](image)

**6.2 Predictive modelling using the strain-based failure model**

The single lap joint is the most common test method used to evaluate the strength of an adhesive joint. The experimental results used to validate the predictive modelling in this section have been presented in Chapter 4 (section 4.6).

**6.2.1 2D predictive modelling of EA9321 /aluminium single lap joint**
The EA9321/aluminum single lap joint shown in Figure 4.24 (a) was considered first. The modelling was used to predict the residual strength of the joints after exposure to moisture. The mesh was generated using plane strain four-noded quadrilateral elements with an element size of 0.05mm in the adhesive layer, as shown in Figure 6.8. Due to the symmetry of the SLJ configuration, only half the joint needed to be modelled. Rotational symmetry was applied to a section through the middle of the overlap, also shown in Figure 6.8. The same moisture dependent critical failure strains calibrated from the MMF modelling were used in the SLJ modelling. As with the MMF models, explicit analysis was applied with a mass scaling factor of $1 \times 10^5$ and geometric nonlinearity was taken into account. The other two mesh refinement schemes, 0.1mmx0.1mm and 0.025mmx0.025mm, corresponding to the MMF calibration model, were also generated for the SLJ prediction model. They were similar to the one shown in Figure 6.8.

Single Fickian diffusion was used to obtain the moisture profiles along the overlap length. The mass diffusion model coded in ABAQUS [146] was used to generate the normalised nodal moisture concentration as field output for the coupled diffusion-mechanical analysis using the same mesh scheme. The diffusion
parameters of the adhesive were determined from the experimental data, as shown in Table 4.1. The contour of the moisture distributions in the adhesive layer after exposure to moisture for 2, 8, and 26 weeks are shown in Figure 6.9.

![Figure 6.9 - Contour of the moisture distribution in the adhesive layer of the EA9321/aluminium joint after exposure to moisture for 2, 8, 26 weeks](image)

The critical strain - moisture concentration curves previously calibrated from the MMF modelling for the three mesh schemes were used as the failure parameters for the SLJ models. The variation of the residual strengths of the joint with time of exposure obtained from the experimental results and the finite element modelling is shown in Figure 6.10 and the full dataset has been presented in Table 4.8.

![Figure 6.10 - Predicted ultimate failure load of the EA9321/aluminium SLJ using the strain-based failure model](image)
It can be seen in Figure 6.10 that the FE modelling predictions for the three different mesh schemes agreed well with the experimental results. Thus, the use of mesh dependent failure parameters successfully accommodated the mesh sensitivity of the adhesive stress and strain values. The experimental data exhibited considerable scatter at the longer exposure times. There were specific outlying data at 12 weeks (4.4kN) and 26 weeks (2.3kN). As can be seen in Table 4.8 (section 4.6), there was some evidence to suggest that these lower strengths may not have been representative and this then would reduce the scatter and further enhance the correlation between the predicted and measured strengths.

Another advantage of this continuum modelling method is that the cohesive damage initiation and propagation within the adhesive layer can also be predicted. A series of contour plots selected from the 26 week degraded joint model with the mesh scheme of 0.05mmx0.05mm are shown in Figure 6.11. The failed elements have been marked in white. These show, visually, the damage propagation trail with an increase in the applied displacement. It can be seen that the damage initiated around the corner of the joint, propagated first along the centre of the adhesive for a short distance (and through the fillet) and then extended mainly along the lower interface of the adhesive layer to the middle of the joint.

Figure 6.11 - Damage initiation and propagation in the EA9321/aluminum SLJ model (26 weeks degraded, Mises stress contour, smallest mesh size 0.05mmx0.05mm)
Chapter 6 Predictive modelling of EA9321 single lap joints using a strain-based failure model

It is worth noting that this damage location appeared to be insensitive to the mesh refinement. The complete damage contour trails for a 0.1mmx0.1mm mesh model and a 0.025mmx0.025mm mesh model are shown in Figure 6.12 to illustrate this. Failure was found to occur close to the adhesive-substrate interface.

![Mesh 0.1mmx0.1mm](image1)

![Mesh 0.025mmx0.025mm](image2)

**Figure 6.12 - Damage in the EA9321/aluminum SLJ model with different mesh schemes (26 weeks degraded, Mises stress contour)**

Two graphs showing the predicted loading history and damage propagation obtained from an undegraded joint model and a 26 weeks-degraded joint model with the 0.05mmx0.05mm mesh scheme are shown in Figure 6.13.

![Graph](image3)

**Figure 6.13 - Predicted loading history and damage propagation in an undegraded joint and a joint aged for 26 weeks (EA9321/aluminium, smallest mesh size 0.05mmx0.05mm)**
The load in the undegraded specimen increased linearly with applied displacement and peaked at about 9.14kN before suddenly failing. The damage in the adhesive initiated and propagated very quickly. The predicted loading history of the 26 week degraded joint gave a similarly linear loading increase and a catastrophic drop. However there was a degree of non-linearity close to the end in both the loading history and the damage propagation process. In this degraded model, the damage initiated at an applied displacement level of 0.140mm, and extended in a continued and stable manner over 2.1mm (through the adhesive overlap) as the applied displacement increased. Failure then went though the rest of the adhesive layer rapidly as the joint reached the ultimate load.

6.2.2 2D predictive modelling of EA9321/composite single lap joint

A finite element model of the EA9321/composite single lap joint was also generated for cohesive failure analysis. Again, a half mesh model was designed using four-noded quadrilateral elements with mesh refinement around the lap region as shown in Figure 6.14. A rotational boundary condition was specified at the line of symmetry and displacement loading was applied at the end. The size of the adhesive elements in the overlap was 0.05mmx0.05mm, giving a total of 4 rows of elements across the 0.2mm thick adhesive layer.

Figure 6.14 - FE model of the EA9321/composite SLJ and local mesh refinement (smallest mesh size: 0.05mmx0.05mm)
Chapter 6 Predictive modelling of EA9321 single lap joints using a strain-based failure model

The moisture dependent mechanical property of EA9321 shown in Figure 4.3 and the orthotropic composite data shown in Table 4.3 were assigned to the adhesive and the substrates respectively. The same moisture dependent critical strain calibrated from the MMF was used for failure predictive modelling in the joint. The same single Fickian diffusion model was used to specify the moisture diffusion from one end of the lap region towards to the line of symmetry. However, moisture can also diffuse through the composite substrates. The Fickian diffusion parameters for the composite, presented in Chapter 4 (section 4.1.4, Table 4.4), have been used in the modelling.

To study the effect of moisture diffusion through the substrate on the moisture distribution in the adhesive layer, two diffusion schemes, m1 and m2 (with and without composite diffusion, respectively), were used. The resulting moisture distributions along the adhesive layer are compared in Figure 6.15.

![Figure 6.15 - Moisture distribution profile in the SLJ adhesive layer from the diffusion analysis m1 and m2 (with and without the composite moisture diffusion)](image)

It was found that the increase of moisture concentration in the adhesive with the substrates modelled as permeable was significantly accelerated at extended exposure times. The average moisture concentration in the adhesive was increased by about 20% after the joint has been exposed for 4 weeks and by about 80% following 26 weeks exposure. The predicted residual strengths of the EA9321/composite joint for
Chapter 6 Predictive modelling of EA9321 single lap joints using a strain-based failure model

the two moisture diffusion schemes are shown in Figure 6.16. The reduction of the residual strength for the joint degraded for 26 weeks was about 10%. The experimental results are also shown in Figure 6.16 and compared with the FE predictions. The full dataset has been shown in Table 4.9 (section 4.6).

![Graph](image)

Figure 6.16 - Predicted ultimate failure load of the EA9321/composite SLJ for the different composite diffusion schemes (smallest mesh size 0.05mmx0.05mm)

It is seen that the predicted residual strengths matched the experimental results quite well at short and long exposure times. The prediction of the degraded joints based on the composite diffusion scheme was closer to the experimental solution than the model excluding the substrate diffusion. However, both were still higher than the experimental results, particularly at intermediate exposure times. This may have been due to the absence of the composite failure in the modelling, while some delamination of the substrates did occur in the joints tested (section 4.6).

The cohesive damage initiation and propagation within the adhesive layer was also predicted for the EA9321/composite joints. The predicted loading history and damage propagation progress obtained from an undegraded joint model and a 26 weeks-degraded joint model including composite diffusion are shown in Figure 6.17. The mesh size of the adhesive layer in the model was 0.05mmx0.05mm. Both dry
and 26 weeks-degraded joints experienced a sudden loading drop and rapid damage propagation as shown in Figure 6.17.

Figure 6.17 - Predicted loading history and damage propagation in an undegraded joint and a joint aged for 26 weeks (EA9321/composite SLJ model, smallest mesh size 0.05mmx0.05mm)

The complete damage contour trails of the model for the dry and the 26 weeks-degraded joint are shown in Figure 6.18. Failure occurring very close to the adhesive-substrate interface was found. The mesh insensitivity of the damage location was also assessed for the EA9321/composite joint models and it was found to be very similar to the EA9321/aluminium joint.

Figure 6.18 - Damage in the EA9321/composite SLJ model for the dry joint and the joint aged for 26 weeks (smallest mesh size 0.05mmx0.05mm, Mises stress contour)
6.2.3 3D predictive modelling of EA9321/aluminium single lap joint

The predicted residual strength and damage propagation of the single lap joint has been determined as a function of the ageing time. However, the previous FE modelling was all 2D, including the moisture diffusion analysis of the SLJ specimens. This has excluded the third direction of moisture penetration and therefore has not given a complete simulation of the moisture profile. To achieve this, a 3D coupled diffusion-stress FE model was developed and is discussed in this subsection.

A 3D FE model of the EA9321/aluminium single lap joint is shown in Figure 6.19. Due to the symmetry, only a quarter of the joint was modelled. The maximum and the minimum mesh sizes were 0.75mmx0.75mmx0.5mm and 0.1mmx0.1mmx0.5mm respectively. The full diffusion path was considered, as shown in Figure 6.19. The predicted moisture distribution profile from the joint degraded for 26 weeks is shown in Figure 6.20. In the 2D model moisture only diffuses in the 1-direction, so that any section along the adhesive width had the same moisture profile. It can be seen in the 3D model that the moisture diffusion in the adhesive layer flowed from both the 1- and 3-directions. A reduction in the predicted failure load using the 3D model was expected. However, since the adhesive width was twice the adhesive length, the 1-direction diffusion was the main contributor to the moisture profile.

![Image of 3D quarter model of the EA9321/Aluminium SLJ and local mesh refinement](image)

Figure 6.19 - 3D quarter model of the EA9321/Aluminium SLJ and local mesh refinement (smallest mesh size: 0.1mmx0.1mmx0.5mm)
Chapter 6 Predictive modelling of EA9321 single lap joints using a strain-based failure model

The moisture-dependent critical strains calibrated from the 2D MMF model (demonstrated by the 3D MMF model) were used for this predictive modelling. The predicted residual strengths of the EA9321/aluminium joints from the 2D and the 3D model are compared with the experimental data in Figure 6.21. A reduction of about 8% was found in the failure load from the 2D model to the 3D model, whilst there was little difference between the 2D modelling results and the 3D modelling results for the less degraded joints, as expected.

Figure 6.20 - Moisture diffusion profile of 3D quarter model of the EA9321/aluminum SLJ specimen (26 weeks degraded)

Figure 6.21 - Predicted ultimate failure load of the EA9321/aluminium SLJ from the 2D and the 3D models (smallest mesh size 0.1mmx0.1mmx0.5mm)
The loading histories of the joint after being exposed for 26 weeks from the 2D and 3D models are compared in Figure 6.22. It was found that the predicted stiffness was reduced from the 2D to the 3D model by about 5%. This is because more of the adhesive absorbs moisture and hence more of the adhesive has a reduced modulus reducing the overall joint stiffness.

![Figure 6.22 - Predicted loading history and damage propagation in a 3D and 2D EA9321/aluminum SLJ model (26 weeks degraded, smallest mesh size 0.1mmx0.1mmx0.5mm)](image)

The damage propagation processes in the 2D and the 3D models are also shown in Figure 6.22. The damage propagation in the 3D model was much more rapid than the 2D model. This data, however, was taken from the symmetry side of the 3D model, as shown in Figure 6.19, corresponding to the 2D model for the sake of comparison. To investigate the real spatial damage propagation in the 3D model, a series of contour plots are shown in Figure 6.23. The arrows in Figure 6.23 indicate the faces exposed to the environment.

It can be observed in combination with Figure 6.20 that the damage initiated around the corner of the joint at the saturated edge (A), rather than the slightly less degraded mid-plane section (B), and then, propagated from the saturated corner to the middle
Chapter 6 Predictive modelling of EA9321 single lap joints using a strain-based failure model

(B) and the central section (C) of the adhesive layer rapidly. What is not clear from these figures is that failure also occurred in the lower layer of elements in the middle part of the joint. The edge of this is just visible as the white element faces around region (B) that first appear in Figure 6.23(b). It seemed that damage in the 3-direction propagated faster than in the 1-direction (see contour (b)). Final failure occurred in the contour (d) after the load reached the ultimate capacity of the joint.

![Figure 6.23 - 3D damage propagation in the EA9321/aluminum SLJ model (26 weeks degraded, Mises stress contour, smallest mesh size 0.1mmx0.1mmx0.5mm)](image)

6.2.4 3D predictive modelling of EA9321 /composite single lap joint

Similar 3D analyses have been undertaken for the EA9321/composite single lap joint using a 0.1mmx0.1mmx0.5mm mesh refinement model, similar to that shown in Figure 6.19. The scheme with diffusion through the substrates and the same moisture dependent critical strain were used in the modelling. The predicted residual strengths of the joints from the 2D and the 3D models are compared with the experimental data in Figure 6.24. It was found that the predictions from both models were quite close, even for the joint exposed for 26 weeks. This was contributed to the fact that the
moisture distributions in 2D and 3D models were not too dissimilar as the moisture diffused through the composite and adhesive in both models. The loading histories and damage propagation processes of the joint after being exposed for 26 weeks from the 2D and 3D models are compared in Figure 6.25.

Figure 6.24 - Predicted ultimate failure load of the EA9321/composite SLJ from the 2D and the 3D models (smallest mesh size 0.1mmx0.1mmx0.5mm)

Figure 6.25 - Predicted loading history and damage propagation in 3D and 2D EA9321/composite SLJ models (26 weeks degraded, smallest mesh size 0.1mmx0.1mmx0.5mm)
In this case, the joint stiffness for the 2D and 3D model was also quite close. This can be explained as at the longer exposure times the moisture mainly diffused through the composite substrates in both the 2D and 3D models, so that the adhesive layer had similar moisture absorption in both cases. The damage propagation in the adhesive of the EA9321/composite joint has not been shown and was even faster than the EA9321/aluminum joint.

6.3 Summary and conclusion

A strain-based progressive damage failure model has been shown to successfully model the continuum cohesive failure in the EA9321 bonded joints. A moisture dependent critical strain was used to determine the residual strength of adhesively bonded joints. A coupled diffusion-mechanical finite element analysis was implemented using FE package ABAQUS. A mixed mode flexure test was used to calibrate this critical parameter by matching the numerical results with the associated experimental failure loads. The calibrated critical strain then was used with no further modification to model failure in the joints bonded with the same adhesive.

The predicted failure loads of the aluminium single lap joints agree well with the corresponding experimental data for all the exposure times at 95.8%RH/50°C. A reduction was found in the predicted failure load (8%) and stiffness (5%) from the 2D model to the 3D model for the 26 weeks-degraded joint. This was probably due to more moisture absorption in the 3D model than in the 2D model.

Diffusion of the composite substrates had to be incorporated in modelling the aged composite single lap joints. The prediction of the composite SLJ was good for the un-aged joint and the joints aged for 16 and 26 weeks at 95.8%RH/50°C. The prediction for the joints aged for 4 and 8 weeks was however overestimated. This was possibly due to composite failure which was not included in the model. The predicted results for the composite joints from the 2D and 3D models were very close. This can be explained by the fact that the moisture distributions in 2D and 3D
models were not too dissimilar as the moisture diffused through the composite and adhesive in both models.

Another advantage of this cohesive failure modelling method is the ability to predict and study the damage initiation and propagation during the loading. The 2D models showed that the damage initiated from the bi-material corner and then propagated to the fillet and the mid-span of the adhesive overlap in both aluminium and composite SLJs. Failure path was close to the adhesive-substrate interface in both cases. The 3D models showed that the damage initiated around the corner of the outer adhesive and then propagated to both the mid-plane parallel to the loading direction and the central section perpendicular with the loading. The damage propagation to the central section seemed faster than to the mid-plane. Final failure occurred after failure elements went through the outer plane of the adhesive layer.

The critical parameter for the cohesive failure model was mesh dependent. A proportional relationship of the calibrated critical strains between the different mesh schemes at each moisture concentration level has been found and the resulted equation can save work in the use of the model.
It is known that cohesive failure is often found in well made joints bonded with ductile adhesives such as EA9321. In Chapter 6, a strain-based progressive damage failure model has been used successfully to predict the residual strength and damage propagation of the EA9321 bonded single lap joints exposed to a hostile environment. However, it was mesh-dependent and the critical failure parameter had to be calibrated for each specific mesh scheme. Aiming at solving this problem, a continuum damage modelling method was developed to predict the cohesive failure and damage propagation in the same environmentally degraded EA9321 bonded joints. This work is presented in this chapter.

The essential concept of this mesh-independent failure model is the use of a predefined constitutive equation of the damaged material, in which a damage parameter was introduced to account for the degradation according to the displacement in the elements rather than strain. Similar to the strain-based failure modelling, a mixed mode flexure (MMF) test was used to calibrate this damage parameter and it was then used without further modification to model single lap
Chapter 7 Predictive modelling of EA9321 single lap joints using a continuum damage model

joints (SLJ) bonded with the same adhesive but with different substrates (aluminum and composite). In this application, both von Mises and Drucker-Prager plasticity models were used for the response of the adhesive whilst von Mises plasticity was incorporated into the aluminum substrates in both cases. Constitutive data obtained from bulk tensile tests and experimental results of the joint tests are as outlined in Chapter 4.

A comparison between the strain-based failure model and the continuum damage failure model is given in the end of this chapter.

7.1 Mesh-independent continuum damage failure model

The essential concept of the continuum damage failure model is to introduce a damage parameter, $D$, to represent the effect of damage into the constitutive equation of the material. This is achieved by reducing the stress of the undamaged material in proportion to the damage parameter as shown in Equation 7.1. This degradation process is modelled using damage mechanics as shown in Figure 7.1.

$$\sigma_d = (1 - D)\sigma, \quad D = D(\delta_p), \quad 0 \leq D \leq 1$$  \hspace{1cm} 7.1

where $\sigma_d$ and $\sigma$ are the equivalent stress of the damaged and undamaged material respectively. In this work both von Mises and Drucker-Prager equivalent stresses have been used. The function $D$ of the relative equivalent plastic displacement ($\delta_p$) can be defined by the material softening as shown in Figure 7.1.

![Figure 7.1 - A damaged material response according to the equivalent plastic displacement over the characteristic length using a continuum damage failure model](image-url)
The material response is initially linear elastic, a-b, followed by plastic yielding with strain hardening, b-c. Point c identifies the material state at the onset of damage, which is defined by a damage initiation criterion. Beyond this point, the stress-strain response c-d is governed by a specified damage evolution law as shown in Equation 7.1. At point d, the material has lost its load-carrying capacity, i.e., is fully damaged (D=1). In the context of damage mechanics c-d can be viewed as the degraded response of the curve c-d' that the material would have followed in the absence of damage.

The damage parameter, D, is specified in terms of the relative equivalent plastic displacement (over a characteristic length), $\delta_p$, rather than strain, $\epsilon_p$, to ensure no mesh dependency in the modelling. Based on fracture mechanics principles, the strain-softening branch of the stress-strain response cannot represent a physical property of the material. Hillerborg's fracture energy proposal [153] is adequate to allay the concern for many practical purposes. Using brittle fracture concepts, it defines the energy required to open a unit area of crack, $G_f$, as a material parameter.

With this approach, the softening response after damage initiation is characterised by a stress-displacement response rather than a stress-strain response. The implementation of this stress-displacement concept in a finite element model requires the definition of a characteristic length, $L$, associated with an integration point. The fracture energy is then given in Equation 7.2:

$$G_f = \int L\sigma_y \delta_p = \int \sigma_y \delta_p$$  \hspace{1cm} 7.2

This expression introduces the definition of the relative equivalent plastic displacement, $\delta_p$, as the fracture work conjugate of the yield stress after the onset of damage (work per unit area of the crack) as shown in Equation 7.3.

$$\begin{cases} 
\delta_p = 0, \text{ before damage initiation, } D = 0 \\
\dot{\delta}_p = L\dot{\epsilon}_p, \text{ after damage initiation, } 0 < D \leq 1 
\end{cases}$$  \hspace{1cm} 7.3
Chapter 7 Predictive modelling of EA9321 single lap joints using a continuum damage model

This model is available within ABAQUS [146]. The stress-strain material curve illustrated in Figure 7.1 corresponds to the stress-displacement behaviour of a unit size element. The actual behaviour for elements in the model is adjusted according to the characteristic length, \( L \), calculated in terms of the element size. The value of this characteristic length is based on the element geometry: for shell and planar elements the square root of the integration point area is used; for solid elements the cube root of the integration point volume is used. This definition of the characteristic length is used because the direction in which fracture will occur is not known in advance. Therefore, elements with large aspect ratios will have rather different behaviour depending on the direction in which the cracking occurs. Some mesh sensitivity may remain because of this effect, and elements that have aspect ratios close to unity are recommended. This is discussed in detail later. The moisture dependent damage parameter \( D = D(\delta_p) \) (i.e., the damage (softening) curve c-d as shown in Figure 7.1.) requires calibration before use in predictive modelling.

7.2 The continuum damage model calibration using MMF

The same MMF tests as outlined in Chapter 4 (section 4.5) have been used to calibrate the moisture dependent damage (softening) curves of EA9321. It has been noted that to ensure mesh independency of the modelling, elements that have aspect ratios close to unity are recommended. Thus, a 2D FE model with the mesh refinement of 0.1mmx0.1mm along the adhesive layer, shown in Figure 6.2, that was used for the strain-based model calibration in Chapter 6, has also been used to calibrate the damage curve in this chapter. Plane strain four-noded quadrilateral elements were used. A continuum damage model can only be used in conjunction with explicit analysis in ABAQUS. A mass scaling factor of \( 1 \times 10^3 \) was used to prevent dynamic instability. Nonlinear geometric behaviour was included in the modelling.

Von Mises yield for the adhesive was first considered and then a linear Drucker-Prager model was investigated. The moisture dependent experimental
property of the adhesive was as shown in Figure 4.3. The aluminium substrates of the joints were as shown in Figure 4.7. The hydrostatically sensitive parameters of the linear Drucker-Prager model for EA9321 were calibrated using a thick adherend shear test (TAST) in Chapter 5 (section 5.2) and the values of $K=0.9/\beta=25^\circ$ were selected, as shown in Figure 5.7. Both 2D and 3D modelling have been undertaken.

### 7.2.1 Von Mises model calibration (2D and 3D)

To simplify the calibration of the material softening curve of the continuum damage model, a damage initiation point (point c shown in Figure 7.1) and a maximum strain point (corresponding to the zero stress point d shown in Figure 7.1) were chosen as the two failure determining parameters. A straight softening line was assumed between these points. Then the "completed" material property was incorporated into the FE model and the predicted failure load was compared to the experimental result. To further study the effect of the calibration parameters on the predicted results, four selected calibration strain-stress curves for the dry condition and the corresponding predictions were obtained and are shown in Figure 7.2.

![Figure 7.2 - Damage curve calibrations of dry EA9321 using MMF (mesh size 0.1mmx0.1mm, von Mises): (a) calibration curves (of unit-size elements); (b) predicted loading history](image)

It can be seen in Figure 7.2 that an increase of the damage initiation strain or the maximum strain parameter both led to a higher failure load prediction (Calibration 1
As a general observation, the predicted failure load increased as the calibration softening curve moved to the right (Calibration 1 < Calibration 2 and 3 < Calibration 4). Besides, Calibration 2 and Calibration 3, which have different starting and ending positions but similar envelope areas (in Figure 7.2(a)), gave similar predictions. A conclusion can be drawn based on the above observations and other similar analyses, that the envelop area resulting from the enclosed stress-strain curve dominates the continuum damage modelling, regardless of the damage initiation and propagation positions. Considering that the area represented the fracture energy, this observation is not surprising. However, this does not mean that the softening curve can be any shape having the calibrated area. In fact, the curve of Calibration 3, rather than Calibration 2, has been chosen for further predictive modelling, because the damage occurs from the end point of the experimental data and the damage propagation also looks realistic.

It has been mentioned that the material curves in Figure 7.2(a) corresponded to the response of a unit size element. The actual behaviour of the modelled elements was adjusted in terms of a characteristic length based on the element size as illustrated in Equation 7.3. Take calibration 3 in Figure 7.2(a) as an example. The data used in the ABAQUS input files for different mesh sizes were the same as shown in Table 7.1:

<table>
<thead>
<tr>
<th>ABAQUS input file</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>*DAMAGE INITIATION 0.012</td>
<td>Equivalent plastic displacement for unit size element at the damage initiation point ( c ), as shown in Figure 7.1</td>
</tr>
<tr>
<td>*DAMAGE EVOLUTION, TYPE=displacement 0., 0. 1., 0.008</td>
<td>First data column is the damage parameter ( D ). Second data column is the corresponding relative equivalent plastic displacement, measured from the damage initiation, for unit size element</td>
</tr>
</tbody>
</table>

The actual tensile stress-strain responses in three elements of different mesh sizes are shown in Figure 7.3(a). These three elements were then used to model the MMF joint. They each gave the same predicted results, shown in Figure 7.3(b). This demonstrates the mesh independency.
Figure 7.3 - Mesh independence of the continuum damage failure model: (a) actual tensile stress-strain responses in elements with different mesh sizes; (b) predicted loading history

Again, to make the calibrated softening curve more realistic, it was decided to modify the simple straight line shown in Figure 7.2(a) to provide more curvature, as shown in Figure 7.4. Another moisture concentration level (the saturation condition at 95.8%RH/50°C, $m_{\infty} = 3.85\%$, as illustrated in Table 4.1) was also required to calibrate the moisture dependent nature of the damage curve of the material. The selected calibration is also shown in Figure 7.4.

Figure 7.4 - Moisture dependent damage curve calibration results of EA9321 using the continuum damage model of MMF (von Mises)
The predicted results for the MMF modelling using the calibrated damage curves are shown in Figure 7.5. A linear interpolation between the dry and saturated data has been assumed in the predictive modelling. Three different mesh refinement sizes were used and compared. The predicted failure loads demonstrated satisfactory mesh independence in the modelling, as shown in Figure 7.5.

![Predicted failure loads of the MMF using the continuum damage model with the different mesh sizes (von Mises)](image)

Figure 7.5 - Predicted failure loads of the MMF using the continuum damage model with the different mesh sizes (von Mises)

To further demonstrate the mesh independence of this continuum damage model and to calibrate the damage parameters for 3D predictive modelling, a 3D FE model of the MMF test with unit aspect ratio elements in the adhesive layer of 0.25mmx0.25mmx0.25mm was created and is shown in Figure 7.6. Half of the specimen was modelled due to the symmetry of the MMF configuration. Eight-noded quadratic elements, explicit analysis, a mass scaling factor of $1 \times 10^5$ and a nonlinear geometric behaviour were as used in the strain-based failure modelling. The predicted failure loads using the calibrated damage curves shown in Figure 7.4 are also shown in Figure 7.5. Good correlation was found and the mesh independence of this modelling method was further demonstrated.
7.2.1 Linear Drucker-Prager model calibration (2D and 3D)

The MMF calibration process for the Drucker-Prager moisture dependent damage curves of EA9321 was carried out in a similar way. The selected calibration results are shown in Figure 7.7. The predicted failure loads of the MMF specimens and the mesh independency of the continuum damage model were also verified for the Drucker-Prager model. Both 2D and 3D calibration modelling were undertaken. The graphs are omitted here because the results were very similar to those obtained from the von Mises calibration work.

![Damage curve calibration results of EA9321 using the continuum damage failure model of MMF (Drucker-Prager, K=0.9, \( \beta =25^\circ \))](image-url)
These calibrated damage parameters for EA9321 were then used, without any modification, to predict the responses of the EA9321/aluminium and EA9321/composite single lap joints.

7.3 Predictive modelling using the continuum damage model

The same EA9321 bonded single lap joints studied in Chapter 6 were modelled in this chapter. The joint configurations are shown in Figures 4.24. The diffusion and material properties of the adhesive and the two substrate materials are as those outlined in Chapter 4 and used in Chapter 6. Both von Mises and linear Drucker-Prager yield models in conjunction with the continuum damage model have been studied in this chapter.

7.3.1 2D continuum damage model for the EA9321/aluminium joints in conjunction with von Mises yield model

The FE model with a 0.05mmx0.05mm mesh scheme, shown in Figure 6.8, has also been used for the continuum damage modelling. The continuum damage material response (shown in Figure 7.1) replaced the critical strain controlled failure criteria used in Chapter 6. The elements were also plane strain four-noded quadrilateral elements and the same Fickian diffusion model was used to obtain the moisture profiles along the overlap of the joint. The moisture dependent damage stress-strain curves calibrated from the NAIF specimen, as shown in Figure 7.4, were used in the modelling. Explicit analysis was applied with a mass scaling factor of $1 \times 10^5$ and geometric nonlinearity was taken into account. The ultimate failure loads of the joint obtained from the experimental results and the finite element predictions are plotted in Figure 7.8, for the full range of exposure times.
Chapter 7 Predictive modelling of EA9321 single lap joints using a continuum damage model

To check the mesh-independence of this modelling method, the FE model with a 0.1mmx0.1mm mesh scheme was also used (with the same moisture dependent damage curves calibrated from the MMF von Mises model). The predicted failure loads of the joint exposed for different time are also shown in Figure 7.8.

It can be seen that the FE modelling predictions for the different mesh schemes agreed well with each other as well as with the experimental results. Relatively lower failure loads were predicted for the joints degraded for 2 weeks and 4 weeks, compared to the average results obtained from the experiments. This may be because the assumption of linear interpolation between the two calibrated damage curves is not entirely appropriate.

The damage initiation and propagation in the joint (within the adhesive layer) can also be predicted using this modelling method. A series of contour plots selected from the joint degraded for 26 weeks at 95.8%RH / 50°C with the 0.05mmx0.05mm mesh scheme are shown in Figure 7.9. The damage parameter, SDEG (stiffness degradation) in ABAQUS, records the degree of damage (D=0 to 1) in the elements.

Figure 7.8 - Predicted ultimate failure load of the EA9321/aluminum SLJ using the continuum damage model with the different mesh schemes (von Mises)
Chapter 7 Predictive modelling of EA9321 single lap joints using a continuum damage model

It can be seen that the damage initiated around the corner of the joint, propagated first along the centre of the adhesive for a short distance (and through the fillet) and then extended mainly along the lower interface of the adhesive layer to the middle of the joint. This is very similar to the modelling results shown in Figures 6.9 - 6.10, using the strain-based failure model, although in those contours, only the completely failed elements can be identified to show the damage. More details of the difference between these two modelling methods are discussed later.

Curves showing the predicted loading history and damage propagation obtained from an undegraded joint and a joint degraded for 26 weeks at 95.8%RH / 50°C with the 0.05mmx0.05mm mesh are shown in Figure 7.10. It was seen that the load in the undegraded specimen increased linearly with applied displacement and peaked at about 9.02kN before suddenly failing. The damage in the adhesive initiated and propagated very quickly. The predicted loading history of the 26 week degraded joint gave a similar loading increase and a catastrophic drop. However there was a slight degree of non-linearity in the damage propagation process. In this degraded model, the damage initiated at an applied displacement level of 0.075mm, and extended over 1.2mm (through the wet adhesive) as the applied displacement increased to 0.1345mm. Failure then went through the rest of the adhesive layer with a very small
increase in the applied displacement (0.0037mm) as the joint reached the ultimate load. It is worth noting that damage initiation in elements is far from the failure in the elements. The elements only fail when the damage parameter reaches the maximum value of 1 as illustrated in Figure 7.9. The damage initiation (D>0) was used in this plot, rather than when an element fails completely (D=1).

Figure 7.10 - Predicted loading history and damage propagation in an undegraded joint and a joint aged for 26 weeks (EA9321/aluminum SLJ model, mesh size 0.05mmx0.05mm, von Mises)

**7.3.2 2D continuum damage model for the EA9321/aluminium joints in conjunction with linear Drucker-Prager model**

Similar analyses were carried out using the linear Drucker-Prager model. The parameters have been calibrated using the MMF data as shown in Figure 7.7. The ultimate failure loads of the joint for the range of selected exposure times are compared with the experimental results in Figure 7.11. The good agreement between the predicted ultimate failure loads with different mesh schemes as well as with the experimental results is evident. The contour of damage initiation and propagation using this material model was very similar to the von Mises model and thus has been omitted. It is worth noting that the continuum damage model can be used together
Chapter 7 Predictive modelling of EA9321 single lap joints using a continuum damage model

with von Mises and linear Drucker-Prager plasticity models whilst the strain-based failure model has only been implemented with the von Mises model.

![Predicted ultimate failure load of the EA9321/aluminum SLJ using the continuum damage model with the different mesh schemes (Drucker-Prager)]

Figure 7.11 - Predicted ultimate failure load of the EA9321/aluminum SLJ using the continuum damage model with the different mesh schemes (Drucker-Prager)

7.3.3 2D continuum damage models for the EA9321/composite joints (von Mises and linear Drucker-Prager)

Again, the FE model of the EA9321/composite SLJ created in Chapter 6 (shown in Figure 6.14) has been used in the predictive modelling with the continuum damage model. The same Fickian diffusion model including diffusion through the composite was used to obtain the moisture profiles along the overlap of the joint. The same moisture dependent damage stress-strain curves calibrated from the MMF von Mises and linear Drucker-Prager models, and shown in Figures 7.5 and 7.7 respectively, were used for the composite SLJ model. The material property of the composite substrates was assumed to be unaffected by moisture. The ultimate failure loads of the joint for the full range of exposure times obtained from both von Mises and linear Drucker-Prager continuum damage model are compared with the experimental results in Figure 7.12.
It can be seen that the predicted failure loads of the undegraded specimen from both von Mises and Drucker-Prager models matched the experimental results very well and the degraded results showed a good agreement at longer ageing times. However, the steady degradation trend in the predicted results does not match that of the experimental data. This may be due to the absence of the composite failure in the modelling, while delamination of the substrates did occur in the joints tested and might have caused the lower failure load obtained from the experiment at intermediate moisture levels. Compared to the prediction using the strain-based failure model with the same mesh scheme (shown in Figure 6.16), the continuum damage modelling results gave better agreement with the experimental data. This was probably because the two calibrated damage (softening) curves described the damaged material better than the two critical strain "points". Furthermore, although only the modelled result with a mesh scheme of 0.05mmx0.05mm is shown, the other mesh schemes (0.1mmx0.1mm and 0.025mmx0.025mm) have also been studied and the mesh independence of the continuum damage model was demonstrated again, as discussed earlier in the other joints modelled.
Chapter 7 Predictive modelling of EA9321 single lap joints using a continuum damage model

The predicted loading history and damage propagation plots of an undegraded joint and a joint degraded for 26 weeks at 95.8%RH/50°C with the 0.05mmx0.05mm mesh scheme and von Mises material model are shown in Figure 7.13.

![Image](image.png)

**Figure 7.13 - Predicted loading history and damage propagation in an undegraded joint and a joint aged for 26 weeks (EA9321/composite SLJ model, mesh size 0.05mmx0.05mm, von Mises)**

As with the EA9321/aluminium model, the load in the undegraded EA9321/composite model increased linearly with applied displacement and peaked at about 9.39kN before suddenly failing. The damage in the adhesive initiated and propagated very quickly. The predicted loading history of the 26 weeks-degraded joint gave a similar loading increase and a catastrophic drop. A slight degree of non-linearity in the damage propagation process was obtained for this degraded joint. The damage initiated at an applied displacement level of 0.068mm, and extended over 0.45mm as the applied displacement increased to 0.113mm, where the ultimate failure of the joint occurred. Again, the predicted loading history and damage propagation plots using the Drucker-Prager model were very similar to the von Mises model and thus have not been included.
7.3.4 3D continuum damage model for the EA9321/aluminium joint in conjunction with von Mises yield model

A 3D model of the EA9321/aluminum single lap joint had been created for the strain-based failure model. However, this model could not be used with the continuum damage model without mesh regeneration, due to the requirement for elements having an aspect ratio close to unity. The mesh refinement in the existing model was 0.1mmx0.1mmx0.5mm along the adhesive layer. Thus, a mesh refinement of 0.1mmx0.1mmx0.1mm along the adhesive layer was generated in the same 3D model. This resulted into a massive FE model with more than $10^8$ integration points to calculate, which was unsolvable. To avoid such a problem, an alternative method is to model and mesh the adhesive layer and the substrates separately, and then constrain or "tie" the contact surfaces together to make a whole model. This was found to achieve a reasonable accuracy. It has also been found that the two constrained surfaces should have at least one side discretized with the same mesh density, as shown in Figure 7.14.

![Figure 7.14 - 3D quarter constrained model of the EA9321/aluminium SLJ and local mesh refinement (smallest mesh size: 0.1mmx0.1mmx0.1mm)](image)

This is a quarter model of the EA9321/aluminium joint with symmetry in the 3-direction and rotational symmetry applied as shown in Figure 7.14. The mesh refinement along the adhesive layer was 0.1mmx0.1mmx0.1mm and 0.1mmx1.0mmx1.0mm in the two substrates adjacent to the adhesive, as shown in
Figure 7.14. Although the same mesh density has been applied in the overlap length direction to both adhesive and substrates, the mesh in the other two directions were distinctly different. This may still result in a loss of accuracy because the integration points may not be properly interpolated. A stress analysis of this constrained model was thus undertaken and compared to the integrated 3D model shown in Figure 6.19 before being used for the predictive modelling. It was found that the responses of the two models were consistent. The contour of shear stress and peel stress along a path in the middle of the adhesive on the symmetry plane for a dry joint have been used to show this agreement. The variation of the stresses along the overlap length from the overlap end in the two models at two different loading levels, 9kN (failure load is around 9.3kN) and 3kN, are plotted in Figure 7.15. It can be seen that the plots from the two models were quite close and the model can thus be used for predictive modelling.

![Graph showing stress variation](image)

**Figure 7.15 - Comparison of the peel stress and shear stress along the middle of the bondline of the EA9321/aluminum SLJ from the integrated model (shown in Figure 6.19) and the constrained model (shown in Figure 7.14) (von Mises)**

The predictive modelling was then applied to this 3D model. The same damage curves calibrated from the MMF von Mises model, as shown in Figure 7.4, were used in this 3D model. The predicted failure loads of the joint exposed for a range of times obtained from the 3D model are compared with the 2D modelling result and
the experimental data in Figure 7.16. It can be seen that the prediction of the 3D model agreed well with the experimental data and the 2D results using the damage curves for EA9321 calibrated from the 2D MMF modelling. The loading histories of the joint after being exposed for 26 weeks at 95.8%RH/50°C, from both the 2D and 3D models, are compared in Figure 7.17.
It was found that the predicted failure load was reduced from the 2D to the 3D model by about 5% and the predicted stiffness was also reduced by a similar amount. This is consistent with the 3D prediction using the strain-based failure model (as shown in Figure 6.23 in Chapter 6). The reduction in stiffness occurs because more of the adhesive absorbed moisture and hence more of the adhesive has a reduced modulus, reducing the overall joint stiffness.

Unlike the strain-based model, the continuum damage model defines damage initiation in the elements once $D$ is greater than 0. Damage propagation was taken from the plane of symmetry in the 3D model to correspond with the 2D (plane strain) model for the sake of comparison. It can be seen that the damage in the 3D model extended during the loading process, initially slowly along the overlap and then rapidly when the joint reached the ultimate load. This matched the damage propagation plot obtained from the 2D model. This showed that the 2D (plane strain) model has been a reasonable simplification of the 3D model for this bonded joint in absence of residual strain.

The spatial damage propagation in the 3D continuum damage model was also investigated and is illustrated using a series of contour plots in Figure 7.18. The contours represent the damage parameter $D$. The arrows in Figure 7.18 indicate the faces exposed to the environment. It can be observed in combination with Figure 7.17 that the damage initiated around the corner of the joint at the saturated edge (A), rather than the slightly less degraded mid-plane section (B), and then, propagated from the saturated corner to the middle (B) and the central section (C) of the adhesive layer rapidly. What is not clear from these figures is that failure also occurred in the lower layer of elements in the middle part of the joint. The edge of this is just visible around region (B) that first appears in Figure 7.17(b). The same observation was made considering Figures 6.23(a) and (b) for the modelling using the strain-based failure model. Final failure is illustrated in contour (d) after the load reached the ultimate capacity of the joint. The critical failure path consisted of fully damaged ($D=1$) elements going through the saturated side (A), similar to the 2D contour plot shown in Figure 7.8. The full damaged elements at the corner of the side
(C) give an indication of this failure path. This is not quite the same compared to the contour of the strain-based failure model shown in Figure 6.23(d). This is probably due to the absence of the moderately damaged \((0 < D < 1)\) elements because the strain-based failure model can only consider full damage \((D = 1)\) and thus cannot describe the real damage propagation sufficiently.

![Diagrams](image)

Figure 7.18 - 3D damage propagation in the EA9321/aluminum SLJ model using the continuum damage model (26 weeks degraded, von Mises)

7.3.5 3D continuum damage model for the EA9321/composite joints in conjunction with von Mises yield model

A 3D EA9321/composite model, similar to that shown in Figure 7.14, was also analysed using the continuum damage model with the same damage curves calibrated from the 2D MMF von Mises model, as shown in Figure 7.4. The predicted failure loads of the joint exposed for a range of times obtained from the 3D model are compared with a 2D modelling result and the experimental data in Figure 7.19. The 3D prediction was found quite consistent with the experimental data. The 3D
prediction was also found very close to that obtained from the 2D model. The same observation was found from the strain-based 2D and 3D models. This was contributed to the fact that the moisture distributions in 2D and 3D models were not too dissimilar as the moisture diffused through the composite and adhesive in both 2D and 3D models.

Figure 7.19 - Predicted ultimate failure load of the EA9321/composite SLJ using the continuum damage 2D and 3D model (von Mises)

The loading histories of the EA9321/composite joint exposed for 26 weeks at 95.8%RH/50°C from the 2D and 3D models are compared in Figure 7.20. The damage propagation was taken on the plane of symmetry in the 3D model to correspond with the 2D (plane strain) model. As with the 2D composite joint model, and the 2D and 3D aluminium joint models, the damage in the 3D composite model initiated early at the applied displacement level of 0.067mm, propagated slowly during the loading process to a displacement level of 0.011mm, and then went through the remaining adhesive layer rapidly as the joint reached the ultimate load. Unlike Figure 7.17, the stiffness of the 2D and 3D models were similar. As discussed above, this is because moisture distributions in 2D and 3D models were similar in the composite joints, where moisture diffuses through the composite.
Chapter 7 Predictive modelling of EA9321 single lap joints using a continuum damage model

Figure 7.20 - Predicted loading history and damage propagation in a 3D and 2D EA9321/composite SLJ model after exposure for 26 weeks (von Mises)

Contour plots of the spatial damage propagation from the 3D EA9321/composite model exposed for 26 weeks are shown in Figure 7.21.

Figure 7.21 - 3D damage propagation in the EA9321/composite SLJ model using the continuum damage model (26 weeks degraded, von Mises)
As with the contours from the 3D aluminium joint model, the damage in the 3D composite joint model initiated from the corner of the saturated edge (A), and then propagated to both the middle (B) and the centre (C) of the adhesive layer at the same time. The load reached the ultimate capacity of the joint when the fully damaged (D=1) elements went through the saturated edge (A) rapidly, as shown in the contour (d). The strain-based failure model can show only the fully damaged elements and thus has not described the damage propagation (shown in Figure 7.21) sufficiently.

7.4 Comparison of the continuum damage model and the strain-based failure model

The strain-based failure model and the continuum damage model have both been demonstrated as efficient and reliable FE modelling methods to predict the cohesive failure in adhesively bonded joints. However, they are different and have different application features as well as advantages and disadvantages. These are summarised and shown in Table 7.2 and Table 7.3.

<table>
<thead>
<tr>
<th>Table 7.2 Strain-based failure model summary</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Full Title</strong></td>
</tr>
<tr>
<td><strong>Category</strong></td>
</tr>
<tr>
<td><strong>Failure criteria</strong></td>
</tr>
<tr>
<td><strong>Damage parameter</strong></td>
</tr>
<tr>
<td><strong>Advantage</strong></td>
</tr>
<tr>
<td><strong>Disadvantage</strong></td>
</tr>
</tbody>
</table>
Table 7.3 Continuum damage model summary

<table>
<thead>
<tr>
<th>Full Title</th>
<th>Mesh-independent continuum damage failure model</th>
</tr>
</thead>
<tbody>
<tr>
<td>Category</td>
<td>Continuum damage modelling method</td>
</tr>
<tr>
<td>Failure criteria</td>
<td>Damage $D = D(\delta_p) . 0 \leq D \leq 1$ and $\sigma_d = (1 - D)\sigma$. Element starts damage when $D &gt; 0$ and fails when $D = 1$. The joint fails when failed elements extending across the joint overlap.</td>
</tr>
<tr>
<td>Damage parameter</td>
<td>Moisture dependent material damage (softening) curve (can be calibrated using FE modelling).</td>
</tr>
<tr>
<td>Advantage</td>
<td>1) coded in ABAQUS, easy to apply; 2) easy for calibration; 3) predict failure loads of the joints; 4) predict the failure initiation and propagation in the adhesive; 5) applicable for both 2D and 3D; 6) available for Drucker-Prager model.</td>
</tr>
<tr>
<td>Disadvantage</td>
<td>Aspect ratio of elements has to be around unity to ensure mesh independence.</td>
</tr>
</tbody>
</table>

Essentially, the strain-based failure model can be taken as a simpler and mesh dependent damage modelling technique than the continuum damage model.

7.5 Summary and conclusion

A continuum damage modelling method has been developed to model the cohesive failure of the EA9321 bonded joints. A damage parameter was introduced as a correction to the material constitutive equation in terms of the equivalent plastic displacement. Compared with the strain based progressive damage failure model, the main advantage of this model is mesh independency. This derives from a displacement-based damage parameter. The MMF has been used to calibrate the mesh independent, moisture dependent damage curves. The predictions from both EA9321 bonded aluminum and composite single lap joints were good for a range of degradation when compared with the experimental results. Both von Mises and linear Drucker-Prager material models have been used with continuum damage model. The strain-based failure model can only be used with von Mises model due to the limitation of the FE software. Both 2D and 3D models were created and studied. The damage initiation and propagation during loading were also predicted from the continuum damage model. Moreover, the damage parameter incorporated in the
model can usefully indicate not only the failure path but also the actual degree of the
damage in elements. This method has been demonstrated to be a highly efficient and
reliable modelling method to predict environmental degradation in ductile adhesive
bonded joints.

There is a disadvantage involved with the mesh independency of the continuum
damage model. The model can be mesh sensitive if the aspect ratio of elements is not
close to unity. This can cause a problem when the damaged regions in the model
have a large aspect ratio and thus require many elements and integration points for
solution. However, this problem can be overcome by modelling and discretising
different parts independently and then constraining ("tie") the contact surfaces
together to make a whole model. It was also found that to achieve a reasonable
accuracy in such a model, the two constrained surfaces should have at least one side
discretised with the same mesh density.

The continuum damage model has been compared with the strain-based failure
model in this chapter. Although both of the models can predict progressive damage
cohesive failure in adhesively bonded joints quite well, the continuum damage model
has shown much greater potential, with the following three advantages: 1) mesh
independence; 2) prediction of a full damage initiation and propagation process; 3)
implemented with both von Mises and linear Drucker-Prager yield models.
VALIDATION MODELLING OF EA9321 BONDED COMPOSITE T JOINTS (REDUCED WIDTH)

A series of EA9321 bonded composite T joints, tested in 3-point bending, were studied for a range of moisture degradation, using FEA modelling. This structural element was recommended in a review [154] of candidate structural element joints with the intention of selecting a structural element that was suitable for validation of the joint lifetime prediction models developed in this project. A polymer matrix composite T joint represented the most appropriate joint design of those found in larger, more complex, bonded structures.

The composite T joints studied in this project were divided into two groups by specimen width. One had reduced width (2mm) specimens. The other involved testing of full width (25mm) specimens. The full width joint tests have been reported in Chapter 4 (section 4.7) and the modelling of these joints is presented in Chapter 9. The purpose of the validation study with the reduced width specimens presented in this chapter is to establish further confidence in the moisture dependent non-linear adhesive models to be used in subsequent predictive modelling.
The experimental work with the reduced width joints was undertaken by a colleague at BAe [152] (in the same research consortium) and is summarised in the first section of this chapter. The joints were tested for in-situ strain and measurement of bond layer deformation, as with the experimental work carried out and reported in Chapter 5 for single lap joints. A validation finite element analysis was then undertaken for these reduced width tests under both the dry and a moisture degraded conditions. Only 3D models were able to analyse the multi-directional polymer matrix composite. Both continuum shell elements and conventional shell elements, implemented in FEA package ABAQUS [146], were used and compared. Both von Mises and linear Drucker-Prager elastic-plastic materials models were considered. The FEA solutions were compared with experimental results and conclusions drawn.

8.1 Experimental testing summary

All the work in this section has been undertaken by Sargent [152] at BAe.

The thin T joint test piece geometry loaded in 3-point bending is shown in Figure 8.1. The specimens were cut from a full width specimen using a Struers Accutom precision saw, which was controlled with an accuracy of about 50 microns. The composite base substrate was a unidirectional base plate of IM7-8552 of 4.16mm thickness. An average value of the adhesive layer between the substrate and the T-section was 0.13mm thick with 1mm thick fillets at either end. The lay-up details of the multi-directional composite T section were same as the full width specimens and have been shown in Figure 4.29 and Table 4.10 (section 4.7). The noodle (shown in Figures 4.29 and 8.1) was another piece of unidirectional composite with the fibre direction normal to the plane of the joint, shown in Figure 8.1. The detail of the noodle is shown in Figure 8.2. The T-section co-cured prior to bond onto the composite base substrate is also shown in Figure 8.1. The specimens were then mounted on a specially designed and constructed 3-point bend loading rig mounted in-situ on a Minimat straining stage of an optical microscope, as photographed in Figure 8.3.
Figure 8.1 - 3-point bending T joint test piece and loading configuration (unit: mm) [152]

Figure 8.2 - Fillet of the noodle part of the composite T joint (BAe [152])

Figure 8.3 - 3-point bending rig on straining stage of optical microscope (BAe [152])
Digital photomicrographs were taken during testing and then subsequently processed in a form suitable for presentation and further data analysis using MATHCAD. One specimen was tested as supplied in its unconditioned state and one was environmentally conditioned at 50°C/95%RH for a period sufficient to give complete saturation across the width of the specimen. Typically, for a specimen with a width of 2mm it was predicted that this would take approximately 71 days, for the geometry and dimensions of specimens used here. This duration was based on values of the diffusion coefficients measured previously (as shown in Table 4.4) and the application of Fickian diffusion [113] as discussed in Chapter 4.

A failure load of $P = 108\text{N/mm}$ was obtained for the unconditioned joints whilst a higher failure load, $P = 151\text{N/mm}$, was obtained for the saturated specimen. The adherend displacements were measured as a function of load using the in-situ optical microscope and are compared with the modelling results later in this chapter. Figure 8.4 shows in-situ images for the adherend edge of an unconditioned joint sample at different loading levels. In this instance, failure initiated near the adherend edge at the upper adherend corner (as shown in Figure 8.4(a)), with a crack propagating both along the horizontal bond line interface between the lower adherend and adhesive, and vertically up the interface between the fillet and adherend edge. Crushing was also evident in the lower adherend adjacent to the central roller loading point but this has not been shown. The conditioned T specimens have shown similar crack propagation and crushing images at increased loads. This is also discussed together with the modelling results later in this chapter.
8.2 FE model and stress analysis

Two FE models were created using different shell elements for the composite matrix. The modelled results were checked and compared before the validation modelling of the experimental work was undertaken.

8.2.1 Shell elements and FE models
Two shell elements in ABAQUS [146] can be used to model multi-directional material structures in which one dimension, the thickness, is significantly smaller than the other dimensions. Conventional shell elements use this condition to discretise a body by defining the geometry at a reference surface. In this case the thickness is defined using a section property definition. Conventional shell elements have displacement and rotational degrees of freedom. In contrast, continuum shell elements discretise the entire three-dimensional body. The thickness is determined from the element nodal geometry. Continuum shell elements have only displacement degrees of freedom. From a modelling point of view continuum shell elements look like three-dimensional continuum solids, but their kinematic and constitutive behaviour is very similar to conventional shell elements. Figure 8.5 [146] illustrates the differences between a conventional shell and a continuum shell element.

Figure 8.5 - Conventional versus continuum shell element (ABAQUS [146])

The two FE models of the reduced width composite T joint using these two shell elements for the T-section are shown in Figure 8.6. In both models, the adhesive layer, the unidirectional composite substrate and the noodle part were modelled using solid elements. The model using continuum shell elements was created as a whole part. The mesh refinement along the adhesive layer was 0.4mmx0.13mmx1.0mm as shown in Figure 8.6(a). The T-section and the other parts in the model using conventional shell elements were created separately and then combined with each other by constraining the displacements for the contacting surfaces using a "tie"
technique. This technique has been discussed and demonstrated on the 3D single lap joint model in Chapter 7 (section 7.3.4). It was found that to achieve a reasonable accuracy the two constrained surfaces should have at least one side discretised with the same mesh density, as shown in Figure 8.6(b). The coarsest and finest mesh sizes were 1.0mmx1.0mmx1.0mm for the composite base substrate and T-section and 0.4mmx0.13mmx1.0mm for the adhesive layer, respectively, in the model.

Figure 8.6 - FE models of the reduced width EA9321/composite T joint: (a) continuum shell elements; (b) conventional shell elements
The three multi-directional composite sheets of the T-section were laminated as shown in Figure 4.29 and Table 4.10. The mechanical properties of the materials used in the model were all reported in Chapter 4. The shell elements were 8-noded quadratic and the solid elements were 20-noded quadratic with full integration. Implicit analysis was used. Nonlinear geometry was included. Before modelling the experimental data, checks on the FE modelled results and a comparison of the two shell element modelling techniques were undertaken.

In spite of their differences, the behaviours of the two models were very similar. Neither conventional nor continuum shell elements can show the multi-directional layers explicitly. However, the lay-up properties have been incorporated in the calculation and the results can be displayed individually for each layer. To demonstrate the lay-up function of both shell elements, the different property orientations for three different material angles in part of Layer B are shown in Figure 8.7. It can be seen that different laminated layers gave specified material orientations.

![Figure 8.7 - Modelling lay-up property orientations in Layer B of the EA9321/composite T joint: (a) continuum shell elements; (b) conventional shell elements](image-url)
Chapter 8 Validation modelling of EA9321 bonded composite T joints (reduced width)

The behaviour of the laminated composite layers modelled by both shell elements was also inspected. Commercial software called "laminated analysis programme" (LAP) has been used to verify the results. In LAP, with an input file of material property, lay-up details and loading data for a laminated layer, stress and strain distributions through this layer can be calculated and output. Arbitrary examination was undertaken for the three laminated layers in both models. The distributions of the strain component (E22) through Layer B obtained from the two models are shown in Figure 8.8, as an example of the comparison of the FE modelling and the LAP analytical solution. It can be seen that the modelled results from the two shell elements were very similar to each other as well as the LAP solution.

![Figure 8.8 - A verification example of the FE modelling laminated composite layers using LAP: (a) FE modelled strain distributions; (b) LAP solution](image)

Figure 8.9(b) shows the shear and peel stress distributions along the adhesive layer of the composite T joint at a load 108N/mm obtained from the two models. It can be seen that there was little difference between the two shell element modelling results except the area around the middle. At the mid-point, the peel stress distribution of the continuum shell element model (green dash line) gave a simple peak, which was probably due to the noodle at the middle joint, whilst the peel stress of the conventional shell element model (pink dotted line) showed an oscillation which was believed as a result of the "tie" technique affected by the noodle. The shear stress
distribution of the continuum shell element model (red dash line) was also a little smoother than that of the conventional shell element model (blue dotted line) at the middle of the overlap, due to the same reason.

Figure 8.9 - Comparison of stress distribution using a closed form sandwich analysis: (a) the end-loaded sandwich; (b) modelled stress distribution along the adhesive layer

These results have then been compared with the analytical solutions from a closed form approximate analysis of an end loaded adhesive-substrate sandwich as shown in Figure 8.9(a). The end-loaded sandwich represents the end parts of the overlap region. The fillets which were present in the FE models can not be included in the analytical solution. The end loads have been determined from the bend loading of the T joint. The T-section of the joint was represented by a 1mm thick upper adherend with homogeneous mechanical property. The end-loaded sandwich was thus a very approximate analysis but the stresses from both FE models were of the correct order of magnitude and shape, as shown in Figure 8.9(b). The shear stress did not reduce to zero in the two FE models as the joint had a central shear load which was not included in the closed form analysis.

The final aspect investigated was the deflection of the whole T joint. The maximum deflection of a simple supported beam with a load at the mid-span can be easily calculated as shown in Equation 8.1.
\[ \delta_m = \frac{FI^3}{48El} \]  

Where \( F \) is the mid-span load, \( I \) is the beam length, \( E \) is Young's modulus and \( I \) is the 2nd moment of area of the beam section. Thus, considering only the composite substrate with a load 108N/mm at the mid-point, the maximum deflection at the centre is

\[ \delta_m = \frac{FI^3}{48El} = \frac{108 \times 2 \times 72^3}{48 \times 160000 \times 2 \times 4.16^3} \approx 0.875 \text{ mm} \]

The deflections at the corresponding position of the T joint FE models with the different shell elements were both approximately 0.57mm as shown in Figure 8.10. The FE modelled deflection seemed reasonable, being somewhat lower than the simple beam solution, as the stiffness is somewhat larger (as the T section was not included in the application of Equation 8.1).

![Figure 8.10 - Deflection of the FE modelled T joint at the load 108N/mm (unit: mm): (a) continuum shell element; (b) conventional shell element](image-url)
These simple comparisons established some confidence in the complex FE model.

8.2.2 Validation modelling

The FE models shown in Figure 8.6 were loaded with the experimental loads and compared with the experimental data for both dry and conditioned composite T joints. Both a linear Drucker-Prager model (parameters as calibrated in Chapter 5) and a von Mises model were incorporated for the adhesive. The relative displacements between the substrate and the T-section at the bondline at the end of the overlap were compared. Both shell elements were used for the validation modelling. There was no difference in the relative displacements between the two models. Thus, only the modelled results using conventional shell elements have been presented in the comparison with the experimental data in this subsection.

8.2.2.1 Validation modelling for the dry specimen

A comparison of the relative displacements between the substrate and the T-section as a function of load for the dry specimen is shown in Figure 8.11.
It can be seen that there was very little difference between the von Mises (VM) model prediction and the linear Drucker-Prager (DP) model prediction at three loading levels. This is because there was little plasticity in the adhesive for both models, even at the experimental failure load of 108N/mm, as shown in Figure 8.12. However, in the experimental observation, failure around the fillet was apparent at this loading level, as shown in Figure 8.4(b). This was not included in this validation modelling.

Comparing the FE modelling results and the experimental data, it can be seen that the displacement distribution curves were quite different. Only the match at the adhesive edge at low loads was good. A study of the failed joints (shown in Figure 8.4) reveals that there was a void or defect near the edge between the upper adherend and the adhesive layer, as shown in Figure 8.5. This may explain the difference between the two curves and also the lack of plasticity of the adhesive at the experimental failure load. The correlation at the edge has established some confidence in the FE modelling of the joints.

For levels of load where there was no damage in the adhesive, the strain distribution maps from the experiments can be compared with the strain contours from the FE models. The experimental strain field map of the noodle region for the joint loaded at
97N/mm is compared with the FE modelling result (von Mises) as shown in Figure 8.13.

Due to the stress concentrations at the noodle corners and differences in resolution, it was difficult to directly compare the sets of contours from the experiment and the modelling. It was also noted that significant inhomogeneity was present in the experimental strains. However, a coarse comparison has still been undertaken. It seemed that the modelled strain perpendicular to the base (LE22) was quite close to the experimental value with zones of compression in both cases. The modelled strain parallel with the base (LE11) and the shear strain (LE12) were both lower than the corresponding experimental data. It should be noted that ABAQUS outputs shear strain as engineering shear strain, which is twice the mathematic shear strain used in the experiment.

8.2.2.2 Validation modelling for the conditioned specimen
Figure 8.14 shows a comparison between the experimental and predicted substrate displacement differences for the conditioned joints. It can be seen that at low loads there was little difference between the von Mises (VM) model prediction and the Drucker-Prager (DP) model prediction. This was expected as there was limited plasticity at these levels of load. As the load increased the VM model produced a greater shear displacement (Ux) than the DP model. The yielding was somewhat suppressed with the DP model at this load level, as shown in Figure 8.15. The DP model produced a higher peel displacement (Uy) than the VM. Both these trends (Ux and Uy) have been consistently seen in the validation modelling of the single lap joints presented in Chapter 5.

Figure 8.14 - Interface displacement difference along the overlap as a function of load for the conditioned EA9321/composite T joint (solid line-von Mises, dashed line-Drucker-Prager)

Figure 8.15 - Equivalent plastic strain in the adhesive of the reduced width EA9321/composite T joint model at the load of 139N/mm (conditioned): (a) von Mises; (b) linear Drucker-Prager
Comparing the FE modelling results and the experimental data, the relative shear displacements ($U_x$) at the edge from the experiment and the VM model matched well for all loading levels. The relative peel displacements ($U_y$) at the edge matched reasonably at lower loads between the experimental and FE results. At higher load, the experimental peel displacement exceeded the predicted results from both VM and DP models. A similar void was found in the experiment, near the edge between the upper adherend and the adhesive layer of the joint (cut from the same one full size specimen). It should be mentioned that the plasticity occurred only around the end of the adhesive layer when the FE models reached the experimental failure load of 151N/mm, as shown in Figure 8.16. The experimental relative displacements were not available at this load level and thus not included in the comparison. Comparing with the contour plots obtained from the dry joint models at their experimental failure load of 108N/mm (shown in Figure 8.12), the maximum equivalent plastic strains were about 3 times higher in the conditioned models at their failure load.

\[
\begin{align*}
\text{PEEQ} &
\begin{array}{c}
(\text{Ave. Crit.: } 75\%) \\
+5.887e-02 \\
+5.308e-02 \\
+4.729e-02 \\
+4.151e-02 \\
+3.572e-02 \\
+2.993e-02 \\
+2.415e-02 \\
+1.836e-02 \\
+1.257e-02 \\
+6.787e-03 \\
+1.000e-03 \\
+0.000e+00
\end{array}
\end{align*}
\]

Figure 8.16 - Equivalent plastic strain in the adhesive of the reduced width EA9321/composite T joint model at the load of 151N/mm (conditioned): (a) von Mises; (b) linear Drucker-Prager

The strain distribution of the conditioned joint was also studied. The experimental strain field map of the noodle for the joint loaded at 114N/mm is compared with the FE modelling result as shown in Figure 8.17.
Comparing the FE modelling results and the experimental data for the conditioned one, the modelled strain perpendicular to the base (LE22) was, again, quite similar to the experimental value, whilst the other two components of the strain were both lower than the corresponding experimental results. Comparing the experimental data of both dry and conditioned joints, it can be seen that the $\varepsilon_x$ (corresponding to the modelled LE22) and the shear strain (corresponding to the LE12) were similar whilst $\varepsilon_y$ (corresponding to the modelled LE11) was different. The significant inhomogeneity in the strain $\varepsilon_y$ of the tested dry joint might imply that damage had occurred. The modelled contours for the dry and conditioned joints were similar for each strain. This was expected since no damage was included in the FE modelling.

The validation model of the thin EA9321 bonded composite T joint has given some reasonable results, although other details were still unclear due to the complications in the experimental specimens.

8.3 Summary and conclusion
A validation analysis has been undertaken for a set of the reduced width EA9321/composite T joints loaded in 3-point bending. The joints consisted of the adhesive layer, a unidirectional composite base plate and a laminated composite T-section. Two moisture conditioned levels were considered and the adherend displacements were measured as a function of load during the testing.

Two ABAQUS shell elements, conventional and continuum, were used to model the laminated lay-ups in the T-section. A similar response was obtained for the two types of elements. The other parts of the joint were modelled using solid elements. The validity of the FE models using different shell elements were also examined and discussed. Comparing with the experimental data, parts of the modelling results were good for both the unconditioned and the conditioned joints, whilst other part of the correlation was not as good, due to a defect in the experiment. Some confidence has been established for the predictive modelling of the full size joints using the progressive damage models and that work is presented in the following chapter.
A validation FE analysis for the reduced width EA9321 bonded composite T joints has been carried out and presented in Chapter 8. Some confidence and useful information concerning two shell elements (conventional and continuum) to model the laminated composite T-section of the joint has been achieved. Based on the validation work, a full width T joint bonded with the same adhesive and composite, tested in 3-point bending, was modelled using the two cohesive failure models (strain-based failure model and continuum damage model) developed in this research. This work is presented and discussed in this chapter. The experimental work of the full width joints was undertaken by colleagues at MBDA and has been reported in Chapter 4. The failure parameters of EA9321, calibrated using the MMF test configuration and reported in Chapter 6 and Chapter 7, were used in the predictive modelling of this test configuration. Only the dry joint was studied because no moisture diffusion model is available for water transfer between solid element and shell element (either continuum or conventional) in the FE package ABAQUS [146]. The predicted results were compared with the experimental data. The two cohesive failure modelling methods were further studied and compared based on this analysis.
9.1 Predictive modelling of the full width EA9321/composite T joints using a strain-based failure model

Initially, a strain-based failure model was used to predict the residual strength of the full width EA9321 bonded composite T joint in the dry condition. Both continuum and conventional shell elements were used and a non-damage stress analysis was undertaken before the failure model was incorporated.

9.1.1 FE model and stress analysis

Two FE models created for the full width EA9321/composite T joint are shown in Figure 9.1. Due to symmetry, only half of the joint was modelled. Fillets at the two ends of the adhesive layer were not considered due to a lack of data. As with the reduced width joint FE models, continuum shell elements and conventional shell elements have been used to model the laminated T-section of the joint using ABAQUS, as shown in Figures 9.1(a) and (b), respectively. The adhesive layer, composite base plate and noodle were modelled using solid elements. The separately created parts were combined to form a whole model by constraining the displacements for the various contact surfaces in the conventional shell element model, as with the 3D single lap joint model (Chapter 7) and the reduced width T joint model (Chapter 8). The coarsest and finest mesh sizes in both models were 1.0mm×1.0mm×1.0mm and 0.25mm×0.13mm×1.0mm, respectively, as shown in Figure 9.1.

Stress analyses were carried out for the joint models before incorporating the strain-based failure model, as with the reduced width joint models. Von Mises plasticity and implicit analysis were used. Nonlinear geometry was included in all cases. 20-noded full integration solid elements were adopted for the stress analysis. Both shell element models were verified, again, as with the models presented in the previous chapter.
However, to incorporate the strain-based failure model, explicit analysis must be used with ABAQUS and, using this, only eight-noded reduced integration solid elements were available for the adhesive and the unidirectional composite. Hourglassing effects have been found in the models using this reduced integration element, as illustrated in Figure 9.2. It can be seen that unstable stress distributions occurred along the adhesive overlap in both models when using reduced rather than full integration elements. However, there was little hourglassing at the overlap ends and these areas were critical for the damage initiation of the joint, as the experiment suggested. It was considered appropriate to continue with the subsequent predictive modelling using the reduced integration solid elements.
9.1.2 Strain-based failure model calibration

A strain-based cohesive failure model has been proposed and used to predict failure of the EA9321 bonded single lap joints in Chapter 6. A single moisture-dependent failure parameter, the critical strain, was introduced to describe the failure of the adhesive. Material followed the non-linear constitutive response until the equivalent plastic strain (defined corresponding to Mises equivalent stress) reached this critical (maximum) value at any element integration point, as shown in Figure 6.1. An element with all integration points in excess of this critical strain will fail. The failed elements form a natural failure path in the model. The model has been used not only to predict the failure load of the joints but also to study the damage initiation and propagation. Only von Mises plasticity can be used in conjunction with strain-based failure model in ABAQUS.

A series of critical maximum strains have been calibrated for EA9321 at different levels of moisture concentration, using mixed mode flexure (MMF) tests, as presented in Chapter 6. However, this parameter is mesh dependent and the previous
calibration for a different mesh scheme cannot be used directly to this one. Thus, a 3D MMF test model with the same mesh scheme (0.25mmx0.125mmx1.0mm) as for the adhesive layer in the T joint was created for the calibration. The FE model and mesh refinement is shown in Figure 9.3. The calibrated critical strain for the dry condition is shown in Table 9.1, together with the calibration result from the 0.1mmx0.1mmx0.5mm mesh scheme used in the single lap joint model. The predicted MMF failure load using these two calibration models are also listed and compared with the experimental result in Table 9.1.

\[ \text{Critical strain calibration results of EA9321 using the MMF test model with different mesh schemes (Dry)} \]

<table>
<thead>
<tr>
<th>Mesh schemes</th>
<th>Critical strain</th>
<th>Predicted MMF failure load*</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.25mmx0.125mmx1.0mm</td>
<td>0.040</td>
<td>0.715 kN</td>
</tr>
<tr>
<td>0.1mmx0.1mmx0.5mm</td>
<td>0.046</td>
<td>0.726 kN</td>
</tr>
</tbody>
</table>

*Experimental MMF failure load was 0.69kN

It has been observed in Figure 6.5 that the critical strain increases as the mesh size decreases. Comparing the two critical strains shown in Table 9.1, it was found that the reduction was around only 13% from the mesh scheme of 0.1mmx0.1mmx0.5mm to the mesh scheme of 0.25mmx0.125mmx1.0mm. This may be explained by the critical role of the mesh in the 2-direction in the element failure, in this case. The
mesh size in the 2-direction increased from 0.1mm to 0.125mm for the two adhesive mesh schemes used for the MMF model.

9.1.3 Strain-based failure model prediction

The FE models of the EA9321/composite T joint used for the previous stress analysis were now incorporated with the strain-based failure model. The MMF calibrated critical strain shown in Table 9.1 for the mesh scheme of 0.25mmx0.125mmx1.0mm was used for the prediction. Explicit analysis was applied with the von Mises model. In this case, a mass scaling factor of $1 \times 10^6$ was enough to prevent dynamic instability. Geometric nonlinearity was taken into account. The predicted loading history and damage propagation in the adhesive layer of the joint, obtained from the continuum shell element model and the conventional shell element model, are shown in Figure 9.4, together with the experimental failure load.

![Figure 9.4 - Predicted loading history and damage propagation in the model of EA9321/composite T model (dry)](image)

It was found that the predictions using different shell elements for the T-section were very similar, as concluded earlier. The predicted failure load of the FE models was
about 17% higher than the experimental data. This is possibly because the failure of the composite substrate, which had been observed in the experiment (as reported in Chapter 4), was not included in the model. The failure process proceeded rapidly as shown in Figure 9.4. A series of contour plots showing the spatial damage propagation in the adhesive layer of the joint model using conventional shell element are shown in Figure 9.5. The contours of the continuum shell element model were very similar and were thus omitted.

It can be seen that the failure (white elements) initiated from one of the edges first and then the other. The failure propagated from the two sides to the centre rapidly.
and it was faster at the initiation side than the opposite one. When the failure length at both sides exceeded half of the adhesive layer, the joint reached its maximum load and failed. The predicted damage initiation was similar to the experimental observation as illustrated in Figure 4.32.

9.2 Predictive modelling of EA9321 bonded composite T joints using continuum damage model

A continuum damage model was used to predict the residual strength of the EA9321 bonded composite T joint in dry condition in this section.

9.2.1 Reduced width FE model

Chapter 7 produces full details of this mesh-independent cohesive failure model. The essential concept of this model is to achieve mesh independence by the introduction of a damage parameter in terms of element displacement rather than strain. To further ensure mesh independence, the aspect ratio of elements is required to be around unity. It was also found that to achieve a reasonable accuracy in a model where parts are "tied", the two "tied" surfaces should have at least one side discretised with the same mesh density, to ensure a proper interpolation between the integration points. Due to the high ratio of the adhesive length to the adhesive thickness (50mm to 0.13mm ≈ 385), this would result in too many elements and thus cause a problem when running the program with a 3D half model as shown in Figure 9.1. On the other hand, shell elements for the laminated lay-ups require a 3D model. To solve the problem, 3D models were created with the width reduced to 0.13mm (the same as the adhesive thickness). Both continuum and conventional shell elements were used, as shown in Figure 9.6. A symmetrical boundary condition was assigned to the mid-plane of the model.

As shown in Figure 9.6, the coarsest and finest mesh sizes in the two models were 1.0mm×0.13mm×0.13mm for the composite base substrate and T-section, and 0.13mm×0.13mm×0.13mm for the adhesive layer, respectively. Stress analyses were
carried out for these FE models, using von Mises plasticity and implicit analysis, before incorporating the continuum damage model. The analysis results were very similar to the full width joint models in Section 9.1.1 and the validation models in Chapter 8. The predicted results of the two models with different shell elements were also same. As with the strain-based failure model, to incorporate a continuum damage model, explicit analysis must be used with ABAQUS and thus only eight-noded reduced integration solid elements were available. Hourglassing effects were found around the mid-span of the overlap in these continuum damage models. Again, it was expected that this would not affect the predictive modelling of the failure load and the damage initiation.

\[ \text{Figure 9.6 - FE model of the EA9321/composite T joint with reduced width of 0.13mm (a) continuum shell element; (b) conventional shell element} \]

9.2.2 Continuum damage model prediction
Both von Mises and linear Drucker-Prager models were considered in this predictive modelling. Due to the mesh independence of the continuum damage method, the damage parameters previously calibrated from the MMF modelling and applied to the single lap joints (as shown in Figures 7.4 and 7.7) were used for the composite T joint model without further modification. Explicit analysis was applied with a mass scaling factor of $1 \times 10^6$ and geometric nonlinearity was included. The predicted results of the two models with different shell elements were very similar and thus only the conventional shell element model has been discussed below. The predicted loading history and damage propagation in the adhesive layer of the dry joint, obtained from both the von Mises and linear Drucker-Prager models (conventional shell elements) are shown in Figure 9.7, together with the experimental failure load. It should be noted that in Figure 9.7 the damage propagation plots were counted with their initially damaged elements ($D>0$) rather than fully damaged elements ($D=1$).

![Figure 9.7 - Predicted loading history and damage propagation in the EA9321/composite T joint model using continuum damage model (dry, conventional shell element, damage counted from $D>0$)](image)

Comparing the results of the two material models, it can be seen that the linear Drucker-Prager model produced failure load about 15% lower than the von Mises
model for the dry condition. This is due to the sensitivity of the adhesive EA9321 to hydrostatic stress. Comparing the FE modelling results with the experimental data, the Drucker-Prager results agreed well with the experimental data whilst the von Mises model prediction was about 14% higher than the experimental value. A small degree of non-linearity can be seen in the damage propagation plots of both models. The damage propagated slowly at the beginning, went quickly as the joint reached the ultimate load and then, became slow again and stopped somewhere past the middle of the adhesive layer after the joint failed.

A series of contour plots for the spatial damage propagation in the adhesive layer of the joint, using the continuum damage model with von Mises plasticity, are illustrated in Figure 9.8. It is clear that the damage initiated from the same edge as observed in the strain-based failure model, and then propagated mainly from this edge to the centre rapidly. This agreed well with the experimental observation. However, as discussed above, the damage propagation became slow and stopped somewhere past the middle of the adhesive when the applied displacement increased continuously after failure. This was different from the experiment in which the damage propagated through the central bondline. The exclusion of the composite delamination in the model may explain this difference. Damage also occurred at the other edge but it stopped at a lower level (D<0.5) within 10 mm from the end. Such details of damage cannot be seen in the strain-based failure model because the critical strain can show only two damage states: failed or intact.

Figure 9.8 - Damage propagation in the adhesive layer of the EA9321/composite T joint model using continuum damage model (dry, von Mises)
The predicted failure loads with von Mises plasticity, obtained from the strain-based failure model and the continuum damage model, are compared in Figure 9.9. The damage propagation plot taken from the continuum model was counted with its fully damaged elements (D=1) for this comparison. It can be seen that both the failure load and the loading stiffness predicted from the strain-based model were little higher (0.6%) than the predictions of the continuum damage model. This may be due to the difference between the full width model and the reduced width model, or simply a small numerical error. The applied load dropped quickly as the damage propagation length increased from the initiation in both cases.

Figure 9.9 - Comparison of the predictions obtained from the strain-based failure model and the continuum damage model of the EA9321/composite T joint (dry, von Mises, conventional shell element, damage counted till D=1)

9.3 Summary and conclusion

The full width EA9321/composite T joint loaded in 3-point bending was studied using the two cohesive failure predictive models developed in this research. Only the
dry condition was considered due to a problem in modelling the moisture diffusion between shell element and solid element using ABAQUS. Stress analyses were undertaken before incorporating the failure predictive modelling. The laminated composite T-section of the joint was modelled using both continuum shell elements and conventional shell elements and the modelled results were very similar and reasonable.

The full size FE model was used to incorporate the strain-based failure model. An EA9321/aluminium MMF model with the same mesh scheme was created for the critical strain calibration of the adhesive. Only the von Mises model was implemented due to a software limitation. The predicted failure load was higher than the experimental result. This was probably due to absence of the composite failure in the modelling. The predicted damage initiation and propagation process partially agreed with the experimental observation.

The continuum damage model can be mesh-independent only if the element is equally sized in all directions. To avoid solution problems arising from an excessive number of cube elements, a very thin section model was created to incorporate the continuum damage model. The calibrated damage parameters for EA9321 were used to predict the residual strength of the composite T joint directly. Both the von Mises and the linear Drucker-Prager model were considered in this case. The failure load predicted from the Drucker-Prager model was 15% lower than the prediction of the von Mises model and agreed well with the experimental data. The predicted damage initiation and propagation process showed more details than the strain-based failure model.
The continuum damage modelling method has been used to model cohesive failure in joints bonded with the ductile adhesive EA9321 with considerable success, as presented in Chapter 7 and Chapter 9. Permatron E32 is also a ductile adhesive which exhibits toughness, retains strength at elevated temperatures and yields durable bonds over a wide temperature range. Cohesive failure has been found in E32 bonded butt joints aged in a wide range of environmental degradation conditions. A continuum damage model has been applied to these E32 bonded joints and this work is presented in this chapter. It should be noted that all experimental work and the moisture diffusion model calibration have been done by Hambly [122] and reported in Chapter 4.

The concept and the failure parameters of the continuum damage model have been explained in Chapter 7. A similar analysis process was employed in modelling the E32 joints. The two targeted joints were steel and aluminium bonded butt joints. A dry and a partially saturated steel butt joints were used to calibrate the moisture dependent damage parameters of the adhesive, and then the calibrated damage curves were used without further modification to predict the failure of the other
environmentally degraded steel butt joints and all the aluminum butt joints. The FEA package ABAQUS was used to implement the coupled mechanical-diffusion analyses. The specimens were either 2.5mm or 2.9mm thick and thus 3D modelling was adopted to incorporate a more realistic moisture diffusion scheme into the modelling. A von Mises yield model was used and is discussed first. Then, to incorporate the hydrostatic stress, the linear Drucker-Prager plasticity model was incorporated.

10.1 FE model and moisture diffusion

The steel butt joint configuration shown in Figure 4.33(a) has been chosen to calibrate the moisture dependent damage parameter for the adhesive E32. A 3D FE model of the joint is shown in Figure 10.1. Considering the 3 planes of symmetry of the joint and the equivalent effect of remote loading on the adhesive layer, only an eighth of a local geometry (10mmx4mmx2.9mm) around the adhesive layer was modelled, as shown in Figure 10.1.

![Local FE model and mesh refinement of the steel butt joint](image)

In a continuum damage model, elements with aspect ratios close to unity are recommended to ensure mesh independency. Therefore, the mesh size along the
adhesive was 0.12mmx0.12mmx0.12mm. A coupled diffusion-mechanical analysis was undertaken using this model. A two-stage Fickian diffusion model was used as discussed in Chapter 4 (Equations 4.1 - 4.3). This model consisted of a single stage Fickian model with an instantaneous boundary equilibration condition superimposed on a second single Fickian diffusion with an evaporative boundary condition. Diffusion occurred over the two exposed adhesive edges as shown in Figure 10.1. Using the best-fit parameters for E32 listed in Table 4.2 and the corresponding time of immersion listed in Table 4.11, each of the two stages was directly and separately implemented in ABAQUS using a standard analogue thermal solution. Then the moisture concentration values were simply added together at each node using a FORTRAN post-processing programme. The experimental fit of the moisture uptake of E32 using both the analytical solution and the FE modelling has been shown in Figure 4.5. Another simple 1D bulk diffusion model, as shown in Figure 10.2, was developed to validate the moisture distribution using the two-stage diffusion model. The arrows show the moisture diffusion directions in the model. The predicted diffusion profile was compared to the analytical solution as shown in Figure 10.3. It can be seen that the results from the two different analysis tools are identical.

![Figure 10.2 - FE bulk model of E32 used to validate the two-stage Fickian diffusion model](image)

![Figure 10.3 - Validated diffusion profile using the two-stage Fickian diffusion model](image)
Chapter 10 Predictive modelling of E32 bonded butt joints using a continuum damage model

The moisture concentration contour for the total uptake in the adhesive layer of the 3D steel joint exposed for 18.7 days is shown in Figure 10.4. This was used along with the corresponding dry joint data to determine the adhesive damage parameters.

![Figure 10.4 - Moisture concentration contour (for the total uptake) of the 3D model for the E32/steel joint degraded for 18.7 days (water at room temperature)](image)

10.2 Predictive modelling using continuum damage model

The von Mises material model was considered first and then a linear Drucker-Prager model was used for the predictive modelling of E32 bonded butt joints using continuum damage model. The continuum damage model is only implemented in ABAQUS Explicit [146]. A mass scaling factor of $1 \times 10^5$ was used to prevent dynamic instability. This value provided a time efficient solution but did not significantly affect the accuracy of the static analyses. Nonlinear geometric behaviour was included in the modelling.

10.2.1 Von Mises model

To simplify the calibration of the damage (softening) curve of the adhesive, a damage initiation point and a maximum strain point (corresponding to zero stress) were chosen as the two critical parameters. These formed a straight line defining softening for the damaged adhesive. The completed material property was then
incorporated into the FE model. For the dry steel joint, three different softening curves and the corresponding predictions using von Mises model are shown in Figure 10.5.

![Figure 10.5 - Calibration for E32 (von Mises, dry) using the continuum damage model: (a) calibrated material curves; (b) predicted loading histories](image)

It has been observed for EA9321 (in Chapter 7) that the predicted failure load increased as the calibration softening curve moved to the right in the continuum damage model. The same conclusion was obtained for E32 as shown in Figure 10.5. It can be seen that calibration 3 gave the best prediction comparing with the experimental data. However, it can also be seen that the damage initiation and the maximum strain points for this curve were quite close to the elastic region of the material. This did not agree with the observed experimental behaviour. This is an indication that von Mises may not be an appropriate model for this adhesive system.

The saturated ($m_w=9.7\%$) adhesive damage curve was calibrated using the joint degraded for 18.7 days. A similar quasi-elastic calibrated result was required to fit the experimental failure load as shown in Figure 10.6. The E32 material calibrated curve for dry condition is also shown in Figure 10.6. A linear interpolation between the dry and saturated data was assumed and implemented into the predictive modelling. The predicted results for the steel joint degraded for 75 days, using the
calibrated damage curves, are shown in Figure 10.7. It can be seen that good predictions were obtained using the previously calibrated damage curves (as shown in Figure 10.6).

![Graph showing tensile stress vs. equivalent tensile strain](image)

**Figure 10.6 - Moisture dependent calibration damage curves of E32 using the continuum damage model (von Mises)**

![Graph showing failure load vs. degradation time](image)

**Figure 10.7 - Predicted failure loads for the steel butt joints using the continuum damage model (von Mises)**

Although the prediction was good, the calibrated damage curves were limited by being close to the elastic region of the material stress-strain response. This did not
seem to be reasonable when compared to the experimental bulk tensile data. It is likely that this is because failure in polymers is often promoted by hydrostatic tension and the von Mises model does not include this effect. For this reason, no further discussion has been included for the FE results assuming von Mises adhesive material. To include the hydrostatic effect for the E32 bonded butt joints, a linear Drucker-Prager model was used and this is discussed in the following section.

10.2.2 Drucker-Prager model

To consider the hydrostatic response exhibited by adhesives, a linear Drucker-Prager plasticity model was used in this section. This model has been introduced for EA9321 in Chapter 5, and illustrated in Figure 5.4(a) and Equations 5.1 - 5.3.

In addition to experimental bulk adhesive uniaxial tensile data, triaxial yield and plastic flow data are needed to define the Drucker-Prager plasticity. The two key parameters are $K$ (the ratio of the yield stress in triaxial tension to the yield stress in triaxial compression) and $\beta$ (the friction angle of the material), as illustrated in Figure 5.4(a). Determination of these two parameters has been carried out for E32 by Hambly [122] and is summarised as follows.

In any case $K$ must lie between 0.778 and 1.0. To simplify the characterisation, $K$ was assumed to be unity. By using this unit value, Equations 5.2 and 5.3 simplify to:

\[ t = q \]  
\[ d = \left(1 + \frac{\tan \beta}{3}\right)\sigma_r = \left(1 - \frac{\tan \beta}{3}\right)\sigma_c \]

The ratio of the uniaxial compressive to uniaxial tensile yield strength for dry E32 was found as 1.4 in [155], i.e.:

\[ \frac{\sigma_c}{\sigma_t} = 1.4 \]
Applying this value to Equation 10.2, the value of friction angle, $\beta$, was determined as follows:

$$\left(1 + \frac{\tan \beta}{3}\right)\left(1 - \frac{\tan \beta}{3}\right) = \frac{\sigma_c}{\sigma_i} = 1.4 \rightarrow \beta = 26.56^\circ$$

Thus, $K=1.0$ and $\beta = 26.56^\circ$ were taken as the Drucker-Prager model parameters. It was assumed that these parameters were moisture independent.

With the incorporation of the Drucker-Prager material model, a similar calibration process for the moisture dependent damage curves was carried out. The selected calibration curves for the damaged bulk dry and saturated E32 tensile behaviour are shown in Figure 10.8. These were obtained using the residual strength of the dry joint and the joint degraded for 18.7 days.

![Figure 10.8 - Moisture dependent calibration damage curves of E32 using the continuum damage model (linear Drucker-Prager, $K=1.0, \beta = 26.56^\circ$)](image)

It can be seen from Figure 10.8 that the calibrated material curves had reasonable hardening regions (prior to softening) and fitted the experimental bulk tensile data (before the damage occurred) much better than the curves calibrated using von Mises...
plasticity (as shown in Figure 10.6). This demonstrated the sensitivity of butt joints to the hydrostatic stress in the adhesive.

A linear interpolation between the dry and saturated bulk tensile data shown in Figure 10.8 was assumed in the predictive modelling. The predicted result of the steel joint degraded for 75 days are shown in Figure 10.9. The aluminium joints (as shown in Figure 4.33(b)) were also modelled using the same damage parameters. A similar local 3D FE model was developed for the continuum damage model prediction with a similar mesh scheme. The predicted failure loads are shown in Figure 10.10.
Figures 10.9 and 10.10 have shown good predictions for the steel butt joints and the aluminium joints at all different degraded levels using the continuum damage model.

### 10.2.3 Prediction of damage initiation and propagation in the E32 bonded butt joints

The continuum damage modelling method predicted not only the ductile failure of the joints but also the damage initiation and propagation in the adhesive. Only the modelling using the Drucker-Prager material model is discussed as the von Mises modelling has been shown to be unrepresentative.

The dry and a wet (exposed for 18.7 days) steel joint models are taken as examples to study the damage initiation and propagation in the butt joints. The modelled loading history of these two joints is shown in Figure 10.11. The equivalent plastic strain and damage parameter contours are shown in Figures 10.12 and 10.13, respectively. The arrows show the moisture diffusion directions into the adhesive. The point "P" on the loading history curve in Figure 10.11 was the point at which the adhesive started to yield. This yielding occurred at the edge of the adhesive layer, as shown in Figure 10.12. It was found that the joints achieved the maximum loads at the same time as the damage initiated in both dry and wet conditions. This is marked as "F" in Figure 10.11. This is probably the result of the quick transfer of accumulated plastic strain through the adhesive layer due to the limited width (2.9mm) of the joint.

![Figure 10.11 - Loading history of the steel butt joint models using the continuum damage model (linear Drucker-Prager)](image-url)
For the dry joint, the damage initiated at the edge of the adhesive and then propagated from the edge to the centre of the model as shown in Figure 10.13(a). For
Chapter 10 Predictive modelling of E32 bonded butt joints using a continuum damage model

the wet joint, however, the damage initiated from the centre of the modelled adhesive layer (a quarter part of the whole joint) and then propagated to the edge and centre from this initiation locus. A quicker propagation to the edge than to the centre was observed, as shown in Figure 10.12(b). The damage imitation in the wet joint is further discussed in the following section.

10.2.4 A verification of the damage initiation in the E32 bonded butt joints

The damage initiation in the interior instead of the edge (where plastic strain was higher) for a wet joint can be verified quantitively from the moisture dependent material damage properties as shown in Figure 10.14.

Figure 10.14 - Plots of the damage initiation locus for a wet (exposed for 18.7 days) steel butt joint model: (a) actual equivalent plastic strain distribution at the damage initiation point; (b) critical damage initiation plastic strain allocation in the adhesive layer; (c) damage initiation occurs at the locus where (a)>(b)
The mathematical software MATLAB 6.5 was used to process the ABAQUS results and generate the 3D graphs as shown in Figure 10.14. The equivalent plastic strain (PEEQ) distribution at the damage initiation point is shown in Figure 10.14(a). It can be seen that higher strain occurred near to the long outer border and symmetry edge whilst lower values distributed at the short outer border and symmetry centre. This was consistent with the contours shown in Figure 10.12. The critical equivalent plastic strain for the damage initiation at each integration point is shown in Figure 10.14(b). This has been calculated based on the moisture concentration value at each integration point (as shown in Figure 10.4) and the calibrated damage initiation strain as a function of the moisture uptake (as shown in Figure 10.8). This showed the damage threshold for each element and did not change with increasing load. However, the actual equivalent plastic strain in each element (as shown in Figure 10.14(a)) increased with increasing load. When the actual strain in an element exceeded the corresponding interpolated damage initiation value (shown in Figure 10.14(b)), damage initiated in this element. This is shown as the positive part of the plot shown in Figure 10.14(c).

Figure 10.14 clearly explained the damage initiation contour in Figure 10.13, which showed that damage did not first occur to the outer border where the actual strain was largest, or the central edge where the damage initiation strain is lowest, but at an interior part where the ratio of the actual strain to the damage initiation strain was highest.

Similar analyses were carried out for the other ageing conditions and the aluminum joints and the same conclusion was drawn. The modelled loading history of the dry and a wet (degraded for 13.8 days) aluminium butt joint model is shown in Figure 10.15. The point "P" and "F" on the loading history curve for each joint indicated the onset of plasticity in the adhesive and the point of the damage initiation. The damage propagation contour in the aluminium joints was quite close to that shown in Figure 10.12 and thus has not been included here.
10.3 Summary and conclusion

A continuum damage model has been successfully used to predict cohesive failure of E32 bonded butt joints for different substrates and periods of ageing. The butt joint configuration is quite different from a mixed mode flexure test specimen or a single lap joint configuration. In the butt joint, the hydrostatic stress has a significant effect on the strength of the joints. Thus a material model such as linear Drucker-Prager model has to be used to include this hydrostatic effect. This illustrates one advantage of the continuum damage model compared with the strain-based failure model, as the latter has only been implemented with von Mises yielding in ABAQUS.

Damage initiation and propagation during the loading were also predicted for the butt joints. It was found that the joints started to yield from the edge of the adhesive layer for both the steel and the aluminium joints. Moreover, the joints reached the maximum loads at the same time as the damage initiated for dry and wet conditions in both cases. This has been explained as the result of a rapid transfer of plastic strain through the adhesive layer in these thin joints. It was also found that damage in the wet joint models initiated in the interior of the adhesive, not the outer border where
the actual strain was largest, or the center edge where the damage initiation strain was lowest. This is because the damage initiation was determined by a combination of moisture concentration and strain distribution. The equivalent plastic strain exceeded the corresponding damage initiation strain first in the interior of the adhesive layer.

The predictive modelling of the E32 bonded butt joints in this chapter has further demonstrated the potential of the continuum damage model for modelling environmental degradation in ductile adhesive bonded joints.
Chapter 11 Swelling in predictive modelling of moisture degraded EA9321 bonded joints

CHAPTER

11

SWELLING IN PREDICTIVE MODELLING OF MOISTURE DEGRADED EA9321 BONDED JOINTS

It is known that the swelling of adhesives (and composites if present) due to moisture uptake may cause significant stresses in bonded joints and in turn have an effect on the joint strength. In previous chapters, a number of different adhesive bonded joints have been studied using FE modelling but no swelling has been considered. In this chapter, the swelling analysis of the adhesive and the composite (due to the moisture absorption) have been incorporated in the EA9321 bonded joints. This is discussed in the context of the strain-based cohesive failure model presented in Chapter 6. The moisture-dependent critical strains calibrated from the aged mixed mode flexure (MMF) test in Chapter 6 (no swelling) were used to predict the strength of the lap joints when swelling was included. The mechanical properties of the adhesive and the substrates including the swelling coefficients, obtained from bulk specimens, have been presented in Chapter 4. Only 3D models for the longest exposure time were considered and von Mises plasticity was incorporated for all the joints.

11.1 Swelling in predictive modelling of the saturated MMF
Chapter 11 Swelling in predictive modelling of moisture degraded EA9321 bonded joints

The 3D FE model of the MMF joint with mesh refinement of 0.1mmx0.1mmx0.5mm (shown in Figure 6.6), used for the critical strain calibration, was used for the predictive modelling with swelling of the saturated MMF. The same critical strain shown in Figure 6.5(b) was used with the swelling coefficient of EA9321 incorporated in FE modelling. As with the work in Chapter 6, explicit analysis was applied with a mass scaling factor of $1 \times 10^5$ to prevent dynamic instability. Nonlinear geometric behaviour was included. The predicted results are compared in Figure 11.1.

It can be seen that the predicted initial failure load with swelling reduced about 16% from the previously modelled result when swelling was excluded. This implied that the swelling effect in the MMF modelling had reduced the residual strength of the joint. The load in both models increased linearly with applied displacement and then dropped quickly after reaching the initial failure load. The model without swelling reached a maximum load of 447.3N at the displacement around 0.55mm. The model with swelling reached a maximum load of 372.7kN at the displacement around 0.46mm. Both models showed some residual strength in the specimen after the initial failure. However, the trend was difficult to follow due to the instability of explicit
analysis after failure. The crack grew at a high rate for about 9mm during the sharp drop in the load for both models. Further investigation found that the stress caused by the swelling strain, had exceeded the yield threshold in the adhesive before the loading was applied, as shown in Figure 11.2. This explained the lower failure load and earlier crack growth predicted from the swelling model.

The difference between the predicted results from the swelling and non-swelling MMF models has indicated the necessity of incorporating swelling of the adhesive into the critical strain calibration for the strain-based cohesive failure model.

**11.2 Swelling in predictive modelling of the aluminium single lap joint exposed for 26 weeks**

The 3D EA9321/aluminium single lap joint model shown in Figure 6.19 was used to study the swelling effect of the adhesive in the joint exposed for 26 weeks, using the strain-based failure model calibrated without swelling. The moisture-dependent critical strains for the mesh scheme of 0.1mmx0.1mmx0.5mm were used as shown in Figure 6.5(b). The predicted results from the swelling and non-swelling models are compared in Figure 11.3.
Chapter II - Swelling in predictive modelling of moisture degraded EA9321 bonded joints

It was found that the failure load predicted by the swelling model was about 13% lower than the prediction of the non-swelling model. This trend was consistent with the comparison of the two saturated MMF models. The lower rate of reduction (14%) from the SLJ model, comparing with the value of 16% from the MMF models, may be attributed to the lower moisture concentration in the SLJ joint than in the saturated MMF specimen. The stress caused by the swelling in this SLJ model had also exceeded the yield threshold near to the corner of the adhesive layer before the loading was applied. This is illustrated in Figure 11.4.

Figure 11.3 - Comparison of the predicted results of the EA9321/aluminium SLJ with and without swelling (26 weeks, smallest mesh size 0.1mmx0.1mmx0.5mm)

Figure 11.4 – Equivalent plastic strain caused by the swelling in the EA9321/aluminium SLJ exposed for 26 weeks
It could be expected that the prediction of the SLJs with swelling would be good if the critical strains are calibrated from the MMF with swelling. However, the computer work needed for this modelling would be very time-consuming and could not be undertaken within this research work.

11.3 Swelling in predictive modelling of the composite single lap joint exposed for 26 weeks

The 3D EA9321/composite single lap joint model (with mesh refinement of 0.1mmx0.1mmx0.5mm) used in Chapter 6 has been used to study the swelling effect in the joint with the longest exposure time (26 weeks) and is reported in this section. The composite substrates were also susceptible to the ingress of water and their swelling effect was thus considered in this research. The coefficients of hygroscopic expansion for the adhesive and the composite have been reported in Chapter 4. The critical strains used here were the same as shown in Figure 6.5(b). The moisture diffusion model was also the same as used for the non-swelling model in Chapter 6. To study the swelling effects of the different parts in the joint, the predicted results from a "partially swelling" model (incorporating only the adhesive swelling), a "fully swelling" model (incorporating both adhesive and composite swelling) and the non-swelling models are compared in Figure 11.5.

It can be seen that the predicted failure load with the adhesive swelling was about 13% lower than the value obtained from the non-swelling model. This reduction was consistent with both the MMF predictive modelling and the aluminium SLJ prediction. The incorporation of the composite swelling has shown little effect on the predicted results. This was probably because that the composite substrates only swelled in the transverse direction (data shown in Figure 4.8). The predicted residual strength with the composite swelling was a little (0.4%) higher than the value with only the adhesive swelling. This can be explained by the composite swelling counteracting the swelling in the adhesive layer. The contours of equivalent plastic strain in the adhesive layer, obtained from the partially and the fully swelling models
of the composite joint, are shown in Figure 11.6. The little difference of the two contours indicated that the composite swelling had little effect in the model.

Figure 11.5 - Comparison of the predicted results of the EA9321/composite SLJ with and without swelling (26 weeks, smallest mesh size 0.1mm x 0.1mm x 0.5mm)

Figure 11.6 - Equivalent plastic strain caused by the swelling in the EA9321/composite SLJ exposed for 26 weeks: (a) only adhesive swelling; (b) fully swelling (including composite)
Again, the predictive modelling of the composite SLJs with swelling using the critical strains calibrated from the MMF with swelling is worth studying and this will be discussed in the future work.

11.4 Summary and conclusion

The swelling effect of the adhesive and the composite substrates were investigated for the EA9321 bonded joints using the strain-based failure model. The critical strains calibrated from the MMF without swelling were used to predict the response of the MMF specimen and the single lap joints with swelling incorporated. The predicted failure loads were consistently reduced with the introduction of the swelling. The reduction was about 16% for the saturated MMF, 14% for the 26 week degraded aluminium SLJ and 13% for the 26 week degraded composite SLJ. This led to a reasonable expectation that the prediction of the SLJs with swelling can be good if the critical strains are calibrated from the MMF with swelling. The swelling of the composite substrates in the SLJ model had a small (recovery) effect on the predicted residual strength. Further study on predictive modelling of the adhesive bonded joints with swelling can be valuable. This is discussed in the future work.
Chapter 12 Conclusions and future work

CONCLUSIONS AND FUTURE WORK

The research work presented in this thesis has made a useful contribution to the durability modelling of environmental degradation in adhesively bonded joints, especially the modelling of the progressive cohesive failure in ductile adhesives. All objectives proposed in Chapter 1 were achieved. The conclusions from the research and suggestions for future work are summarised in the following two sections, respectively.

12.1 Conclusions

Two progressive damage cohesive failure models have been proposed to model the bulk degradation in adhesively bonded joints exposed to hostile environments. One is strain-based failure model, in which a critical equivalent adhesive plastic strain is introduced as the only moisture dependent failure parameter. This parameter has been easily calibrated using a simple test configuration, the mixed mode flexure (MMF) test. However, it is mesh dependent due to the strain-based nature. The other progressive damage model used is a continuum damage model in which a damage variable ($D = 0$–$1$) is defined to describe the state of damage in the adhesive, as a function of displacement in the elements rather than strain. A characteristic length of
element is also defined to facilitate the mesh-independence of the method. This leads to a restriction for the element aspect ratio to be around unity.

Both models have been successfully integrated with the coupled diffusion-mechanical FE analysis and used in the predictive modelling of a range of ductile adhesive EA9321 bonded joints aged to various degrees. A difficult point in such cohesive failure prediction was to locate the location of damage initiation and the propagation path. Success has also been achieved in this area when using these two methods to model different joints. Two E32 bonded butt joints were also analysed using the continuum damage model. This modelling showed the importance of using Drucker-Prager yielding model for certain adhesives. Although limitations exist in both cohesive failure models, the continuum damage model seems to have a higher potential than the strain-based failure model in modelling environmental degradation of adhesive bonded joints.

A newly developed cohesive zone model (CZM) with an interfacial rupture element has been successfully modified to incorporate the plasticity of the bonded substrates. A single lap joint bonded with the adhesive AV119 was studied using this method. The mixed mode flexure (MMF) test was used to calibrate the two moisture dependent fracture parameters, fracture energy and tripping traction, using finite element analyses. The significantly enhanced prediction of the single lap joint (SLJ) for a range of moisture degradations demonstrated the necessity of incorporating non-linear substrate properties.

Validation modelling work with stress analysis excluding damage has been undertaken for various FM73 and EA9321 bonded single lap joints as well as a 3-point bend loaded EA9321/composite T joint. Both 2D and 3D models were considered. Different elastic-plastic models (von Mises and linear Drucker-Prager), and element types (plane strain, plane stress; solid element, shell element) were used and discussed. The modelled results were compared with the experimental data and useful conclusions and information were achieved.
Testing of a series of FM73 bonded aluminium - composite double lap joints was carried out for two controlled environmental degradation conditions. A simple method to quantitatively characterise the failure surfaces was proposed. It was found that interfacial failure increased with the extended exposure time, whilst less degraded joints shows more cohesive failure. The experimental results have been used for validation modelling carried out by a co-worker in the research group.

12.2 Future Work

Hygroscopic swelling of the adhesive and the composite have been found important in the predictive modelling of a range of EA9321 bonded joints using the strain-based cohesive failure model. It will be useful to incorporate the swelling of the adhesive into the MMF calibration and then use the calibrated parameters with swelling in the prediction of other joints bonded with the same adhesive with swelling included. Both the strain-based failure model and the continuum damage model can be applied in this study. Other joints bonded with EA9321 or other adhesives with known bulk specimen swelling properties should also be studied.

Further research is required for the two cohesive failure prediction models developed in this research, in order to extend the use of these models in more general configurations of adhesively bonded systems. For the strain-based failure model, it is important to be able to include hydrostatic stress into the material model, such as in the linear Drucker-Prager model. For the continuum damage model, it will be useful if the characteristic length can be defined according to the aspect ratio of elements and thus allow joint systems with largely different aspect ratios to be modelled efficiently. This could be accomplished by creating user defined subroutines within context of the FE package ABAQUS.

A successful combination of an interfacial failure model (such as cohesive zone model) and a cohesive failure model (such as continuum damage model) will provide
great improvement for modelling general environmental degradation in adhesively bonded joints. Failure of the composite, especially delamination in lay-ups, has often been found in adhesive/composite bonded structures. A further combination of composite failure model and adhesive failure model will be of great help in fully predicting the failure of such bonded systems. To achieve this goal, a composite damage model based on a cohesive zone model can be incorporated with the interfacial and cohesive continuum damage models. The development of a moisture diffusion model for laminated composite is also needed.

More detailed experimental work will be useful to further assess the validity and integrity of the FE modelling. The moisture diffusion performance through the interface has been found different from bulk diffusion. A more accurate diffusion model for the interface would be useful to assess both the experimental data and the FE modelling results.

The effect of stress and time on environmental degradation has not yet been fully understood. Much work needs to be done in both the experimental and computer simulation areas.

It would also be of great interest to apply the developed model to a full scale application for the sake of validity. This would however require a lot of computer power and cannot be carried out on personal computers.
References


[3] BSI (British Standards Institution) standard


References


[27] Huntsberger J.R. Adhesion of Plasticized Poly (Vinyl Butyral) to Glass. The Journal of Adhesion, 12, p. 3-12, 1981


[34] Orman S. and Kerr C. Aspect of Adhesion, ed. Alner D.J. University of London, p. 64, 1971


References


References


238
References


[138] Chiang M. Y. M. and Fernandez-Garcia M. Relation of Swelling and Tg Depression to the
References


1146  ABAQUS Analysis user's manual, 6.5, 2005


1149  Liljedahl C.D.M. PhD thesis, University of Surrey, 2006

1150  Wagman R. Characterising the Stress-strain Response of Adhesive EA9321. Final year report, University of Surrey, 2005


1152  Sargent J.P. Structural Joints and Joining Technologies: Structural Adhesive Bonding Model element test results, BAE SYSTEMS report, Advanced Technology Centre, Sowerby, December 2005


## Appendix 4.1

### Aluminium-FM73-composite double lap shear adhesive joints

#### Test Samples

<table>
<thead>
<tr>
<th>QinetiQ Sample Number</th>
<th>Mean Bondline Thickness</th>
<th>Mean Overlap Length</th>
</tr>
</thead>
<tbody>
<tr>
<td>M1540/S/01123/3</td>
<td>0.15</td>
<td>12.60</td>
</tr>
<tr>
<td>M1540/S/01123/5</td>
<td>0.14</td>
<td>12.66</td>
</tr>
<tr>
<td>M1540/S/01123/6</td>
<td>0.13</td>
<td>12.65</td>
</tr>
<tr>
<td>M1540/S/01139/1</td>
<td>0.10</td>
<td>12.51</td>
</tr>
<tr>
<td>M1540/S/01139/2</td>
<td>0.13</td>
<td>12.59</td>
</tr>
<tr>
<td>M1540/S/01139/6</td>
<td>0.16</td>
<td>12.84</td>
</tr>
<tr>
<td>M1540/S/01142/1</td>
<td>0.10</td>
<td>12.76</td>
</tr>
<tr>
<td>M1540/S/01142/2</td>
<td>0.06</td>
<td>12.63</td>
</tr>
<tr>
<td>M1540/S/01143/1</td>
<td>0.09</td>
<td>12.63</td>
</tr>
<tr>
<td>M1540/S/01143/2</td>
<td>0.14</td>
<td>12.54</td>
</tr>
<tr>
<td>M1540/S/01143/3</td>
<td>0.12</td>
<td>12.46</td>
</tr>
<tr>
<td>M1540/S/01143/6</td>
<td>0.13</td>
<td>12.49</td>
</tr>
<tr>
<td>M1540/S/01144/1</td>
<td>0.12</td>
<td>12.58</td>
</tr>
<tr>
<td>M1540/S/01144/3</td>
<td>0.17</td>
<td>12.67</td>
</tr>
<tr>
<td>M1540/S/01144/4</td>
<td>0.18</td>
<td>12.71</td>
</tr>
<tr>
<td>M1540/S/01144/5</td>
<td>0.15</td>
<td>12.67</td>
</tr>
<tr>
<td>M1540/S/01144/6</td>
<td>0.12</td>
<td>12.61</td>
</tr>
<tr>
<td>M1540/S/01145/1</td>
<td>0.08</td>
<td>12.60</td>
</tr>
<tr>
<td>M1540/S/01145/2</td>
<td>0.15</td>
<td>12.66</td>
</tr>
<tr>
<td>M1540/S/01145/3</td>
<td>0.15</td>
<td>12.67</td>
</tr>
<tr>
<td>M1540/S/01145/6</td>
<td>0.15</td>
<td>12.56</td>
</tr>
<tr>
<td>M1540/S/01150/1</td>
<td>0.12</td>
<td>12.59</td>
</tr>
<tr>
<td>M1540/S/01150/3</td>
<td>0.10</td>
<td>12.61</td>
</tr>
<tr>
<td>M1540/S/01150/4</td>
<td>0.13</td>
<td>12.53</td>
</tr>
<tr>
<td>M1540/S/01150/5</td>
<td>0.11</td>
<td>12.53</td>
</tr>
<tr>
<td>M1540/S/01150/6</td>
<td>0.14</td>
<td>12.52</td>
</tr>
<tr>
<td>M1540/S/01151/1</td>
<td>0.08</td>
<td>12.53</td>
</tr>
<tr>
<td>M1540/S/01151/3</td>
<td>0.12</td>
<td>12.57</td>
</tr>
<tr>
<td>M1540/S/01151/4</td>
<td>0.13</td>
<td>12.64</td>
</tr>
<tr>
<td>M1540/S/01151/5</td>
<td>0.05</td>
<td>12.60</td>
</tr>
<tr>
<td>M1540/S/01152/1</td>
<td>0.13</td>
<td>12.58</td>
</tr>
<tr>
<td>M1540/S/01152/4</td>
<td>0.15</td>
<td>12.69</td>
</tr>
<tr>
<td>M1540/S/01152/5</td>
<td>0.09</td>
<td>12.59</td>
</tr>
<tr>
<td>M1540/S/01152/6</td>
<td>0.15</td>
<td>12.54</td>
</tr>
<tr>
<td>M1540/S/01178/1</td>
<td>0.16</td>
<td>12.37</td>
</tr>
<tr>
<td>M1540/S/01178/2</td>
<td>0.15</td>
<td>12.38</td>
</tr>
<tr>
<td>M1540/S/01178/3</td>
<td>0.15</td>
<td>12.62</td>
</tr>
<tr>
<td>M1540/S/01178/4</td>
<td>0.14</td>
<td>12.50</td>
</tr>
<tr>
<td>M1540/S/01179/2</td>
<td>0.14</td>
<td>12.41</td>
</tr>
<tr>
<td>M1540/S/01179/3</td>
<td>0.13</td>
<td>12.53</td>
</tr>
<tr>
<td>M1540/S/01179/4</td>
<td>0.14</td>
<td>12.53</td>
</tr>
<tr>
<td>M1540/S/01180/1</td>
<td>0.13</td>
<td>12.49</td>
</tr>
<tr>
<td>M1540/S/01180/4</td>
<td>0.08</td>
<td>12.47</td>
</tr>
<tr>
<td>M1540/S/01181/1</td>
<td>0.13</td>
<td>12.53</td>
</tr>
<tr>
<td>M1540/S/01181/4</td>
<td>0.11</td>
<td>12.55</td>
</tr>
<tr>
<td>M1540/S/01181/5</td>
<td>0.09</td>
<td>12.51</td>
</tr>
</tbody>
</table>

**Materials:**

- Aluminum/FM73/CFRP

**Geometries:**

- Test pieces, nominally 84 x 25 mm
- End-tabs, nominally 38 x 25 mm
- Overlap 12.5 mm
## Appendix 4.2

### Aluminium-FM73-composite double lap shear adhesive joints

#### Test Programme

**Unit: mm**

<table>
<thead>
<tr>
<th>Sample No</th>
<th>Adhesive layer thickness</th>
<th>Mean</th>
<th>Aberration</th>
<th>Condition</th>
</tr>
</thead>
<tbody>
<tr>
<td>01144/4</td>
<td>1</td>
<td>0.127 0.867</td>
<td>0°C</td>
<td></td>
</tr>
<tr>
<td>01144/5</td>
<td>1</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>01151/5</td>
<td>1</td>
<td></td>
<td></td>
<td>0°C</td>
</tr>
<tr>
<td>01142/2</td>
<td>1</td>
<td>0.123 0.786</td>
<td>0°C</td>
<td></td>
</tr>
<tr>
<td>01123/5</td>
<td>1</td>
<td></td>
<td></td>
<td>0°C</td>
</tr>
<tr>
<td>01144/3</td>
<td>1</td>
<td></td>
<td></td>
<td>0°C</td>
</tr>
<tr>
<td>01180/4</td>
<td>1</td>
<td>0.127 0.533</td>
<td>0°C</td>
<td></td>
</tr>
<tr>
<td>01150/6</td>
<td>1</td>
<td></td>
<td></td>
<td>0°C</td>
</tr>
<tr>
<td>01139/6</td>
<td>1</td>
<td></td>
<td></td>
<td>0°C</td>
</tr>
<tr>
<td>01123/6</td>
<td>1</td>
<td></td>
<td></td>
<td>0°C</td>
</tr>
<tr>
<td>01143/2</td>
<td>1</td>
<td></td>
<td></td>
<td>0°C</td>
</tr>
<tr>
<td>01143/1</td>
<td>1</td>
<td>0.12  0.5</td>
<td>0°C</td>
<td></td>
</tr>
<tr>
<td>01151/3</td>
<td>1</td>
<td></td>
<td></td>
<td>0°C</td>
</tr>
<tr>
<td>01178/3</td>
<td>1</td>
<td></td>
<td></td>
<td>0°C</td>
</tr>
<tr>
<td>01142/1</td>
<td>1</td>
<td></td>
<td></td>
<td>0°C</td>
</tr>
<tr>
<td>01144/6</td>
<td>1</td>
<td>0.123 0.417</td>
<td>0°C</td>
<td></td>
</tr>
<tr>
<td>01145/6</td>
<td>1</td>
<td></td>
<td></td>
<td>0°C</td>
</tr>
<tr>
<td>01181/4</td>
<td>1</td>
<td></td>
<td></td>
<td>0°C</td>
</tr>
<tr>
<td>01144/1</td>
<td>1</td>
<td>0.127 0.333</td>
<td>0°C</td>
<td></td>
</tr>
<tr>
<td>01143/3</td>
<td>1</td>
<td></td>
<td></td>
<td>0°C</td>
</tr>
<tr>
<td>01152/5</td>
<td>1</td>
<td></td>
<td></td>
<td>0°C</td>
</tr>
<tr>
<td>01139/2</td>
<td>1</td>
<td>0.123 0.462</td>
<td>0°C</td>
<td></td>
</tr>
<tr>
<td>01145/2</td>
<td>1</td>
<td></td>
<td></td>
<td>0°C</td>
</tr>
<tr>
<td>01143/6</td>
<td>1</td>
<td></td>
<td></td>
<td>0°C</td>
</tr>
<tr>
<td>01143/5</td>
<td>1</td>
<td></td>
<td></td>
<td>0°C</td>
</tr>
<tr>
<td>01150/3</td>
<td>1</td>
<td></td>
<td></td>
<td>0°C</td>
</tr>
<tr>
<td>01150/1</td>
<td>1</td>
<td>0.123 0.417</td>
<td>0°C</td>
<td></td>
</tr>
<tr>
<td>01123/3</td>
<td>1</td>
<td></td>
<td></td>
<td>0°C</td>
</tr>
<tr>
<td>01181/5</td>
<td>1</td>
<td></td>
<td></td>
<td>0°C</td>
</tr>
<tr>
<td>01150/4</td>
<td>1</td>
<td>0.127 0.538</td>
<td>0°C</td>
<td></td>
</tr>
<tr>
<td>01178/1</td>
<td>1</td>
<td></td>
<td></td>
<td>0°C</td>
</tr>
<tr>
<td>01152/1</td>
<td>1</td>
<td></td>
<td></td>
<td>0°C</td>
</tr>
<tr>
<td>01178/4</td>
<td>1</td>
<td></td>
<td></td>
<td>0°C</td>
</tr>
<tr>
<td>01151/1</td>
<td>1</td>
<td></td>
<td></td>
<td>0°C</td>
</tr>
<tr>
<td>01151/4</td>
<td>1</td>
<td>0.123 0.452</td>
<td>0°C</td>
<td></td>
</tr>
<tr>
<td>01178/2</td>
<td>1</td>
<td></td>
<td></td>
<td>0°C</td>
</tr>
<tr>
<td>01145/1</td>
<td>1</td>
<td></td>
<td></td>
<td>0°C</td>
</tr>
<tr>
<td>01179/3</td>
<td>1</td>
<td>0.123 0.536</td>
<td>0°C</td>
<td></td>
</tr>
<tr>
<td>01152/4</td>
<td>1</td>
<td></td>
<td></td>
<td>0°C</td>
</tr>
<tr>
<td>01181/1</td>
<td>1</td>
<td></td>
<td></td>
<td>0°C</td>
</tr>
<tr>
<td>01150/5</td>
<td>1</td>
<td></td>
<td></td>
<td>0°C</td>
</tr>
<tr>
<td>01179/6</td>
<td>1</td>
<td>0.133 0.286</td>
<td>0°C</td>
<td></td>
</tr>
<tr>
<td>01152/8</td>
<td>1</td>
<td></td>
<td></td>
<td>0°C</td>
</tr>
<tr>
<td>01139/1</td>
<td>1</td>
<td></td>
<td></td>
<td>0°C</td>
</tr>
<tr>
<td>01180/1</td>
<td>1</td>
<td>0.123 0.308</td>
<td>0°C</td>
<td></td>
</tr>
<tr>
<td>01179/2</td>
<td>1</td>
<td></td>
<td></td>
<td>0°C</td>
</tr>
</tbody>
</table>

**Average**

- 1 1 2 4 3 2 5 9 6 9 2 1 1

\[xx = \text{Number of weeks}, \ C = \text{Control}, \ D = \text{Dry}, \ yy^\circ\text{C} = \text{Environmental temperature}\]