Prediction of the Performance of Adhesively-Bonded Composite Joints

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By

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Abstract

The use of adhesively-bonded joints instead of the traditional types of joining can give reduced weight and increased stiffness in a structure. However, most industries have concerns about the use of adhesive joints in anything other than secondary structures, due to uncertainties over the long-term service life. This thesis discusses the prediction of the lifetime of adhesively-bonded composite structures.

A fracture mechanics approach was used to characterise the fracture behaviour of an epoxy film adhesive, Cytec FM-300M, mainly using composite substrates prepared using wet peel ply, removing the need for any additional surface treatment. Aluminium alloy substrates were also used for some tests.

Tapered double cantilever beam and double cantilever beam specimens were used to determine the mode I critical strain energy release rate, $G_{IC}$, and end loaded split specimens were tested to obtain the mode II critical strain energy release rate, $G_{IIC}$. Lastly, fixed ratio mixed mode specimens were used to obtain the relationship between $G_{IC}$ and $G_{IIC}$ when a joint undergoes mixed mode failure. For validation purposes, single lap joint and double scarf joint specimens were also tested.

These data were then applied in finite element models using Abaqus. Two different modelling techniques were used, the virtual crack closure technique and cohesive zone modelling, CZM. Simulations of the tests performed were executed, in the process obtaining the CZM fitting parameters. Good agreement with the experimental data was verified for each of the models tested.

Fatigue tests were also performed in order to obtain the mode I and mode II threshold values of the fracture energy below which crack growth did not occur, by executing double cantilever beam and end loaded split tests, respectively. For validation purposes, single lap joint fatigue tests were also performed to determine the threshold maximum load the joint could withstand without failure.

Finally, using the CZM fitting parameters obtained in the quasi-static tests and the experimentally obtained threshold values of the fracture energy, modelling of single lap and double scarf joints was performed in order to predict the maximum load value for which no failure would occur when subject to cyclic loading. These predictions showed excellent agreement with the experimental results, showing that this simpler model can obtain good results.
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Nomenclature

**English alphabet**

A  Area  
a  Crack length  
a_e  Effective crack length  
a_0  Initial crack length  
a_f  Crack length at final failure  
a_p  Crack length after pre-crack  
\( \Delta a \)  Length of the crack elements at crack tip  
B  Width  
\( \Delta \text{Clamp} \)  Clamp calibration  
c  Overlap length of the joint  
C  Compliance  
C_{cs}  Known compliance of a rigid calibration specimen  
C_{sy}  System compliance  
C_{total}  Total compliance  
D_f  Bending stiffness  
dU  Change in total strain energy  
dA  Increase of damaged area  
\( \Delta E \)  Work necessary close the crack along one element side  
E  Young’s modulus  
E_1  Young’s modulus determined from three-point bending test/clamp calibration test  
E_a  Young’s modulus of the adhesive  
E_f  Flexural modulus of the adherend via a DCB test  
E’_f  Flexural modulus of the adherend via a three point bending test  
E_s  Flexural modulus of the adherend  
\( \Delta F \)  Difference in load  
F  Large displacement correction  
G_s  Shear modulus  
G  Strain energy release rate
Nomenclature

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>( G_C )</td>
<td>Critical strain energy release rate</td>
</tr>
<tr>
<td>( G_{IC} )</td>
<td>Mode I critical strain energy release rate</td>
</tr>
<tr>
<td>( G_{II} )</td>
<td>Mode II critical strain energy release rate</td>
</tr>
<tr>
<td>( G_{III} )</td>
<td>Mode III critical strain energy release rate</td>
</tr>
<tr>
<td>( G_{IC}^{\text{mixed}} )</td>
<td>Mixed mode critical strain energy release rate under mode I</td>
</tr>
<tr>
<td>( G_{II}^{\text{mixed}} )</td>
<td>Mixed mode critical strain energy release rate under mode II</td>
</tr>
<tr>
<td>( G_{I/II} )</td>
<td>Mixed mode critical strain energy release rate</td>
</tr>
<tr>
<td>( G_{\text{max}} )</td>
<td>Maximum value of ( G ) in a fatigue cycle</td>
</tr>
<tr>
<td>( G_{\text{th}} )</td>
<td>Threshold strain energy release rate</td>
</tr>
<tr>
<td>( G_{I\text{th}} )</td>
<td>Mode I threshold strain energy release rate</td>
</tr>
<tr>
<td>( G_{II\text{th}} )</td>
<td>Mode II threshold strain energy release rate</td>
</tr>
<tr>
<td>( \Delta G )</td>
<td>Strain energy release rate amplitude</td>
</tr>
<tr>
<td>( h )</td>
<td>Adherend thickness</td>
</tr>
<tr>
<td>( h' )</td>
<td>Height of the TDCB beam</td>
</tr>
<tr>
<td>( K )</td>
<td>Stress intensity factor</td>
</tr>
<tr>
<td>( K_I )</td>
<td>Mode I Stiffness CZM fitting parameter</td>
</tr>
<tr>
<td>( K_{II} )</td>
<td>Mode II stiffness CZM fitting parameter</td>
</tr>
<tr>
<td>( l_1 )</td>
<td>Distance between the centre of the loading pin and the mid-plane of the arm</td>
</tr>
<tr>
<td>( l_2 )</td>
<td>Distance between the centre of the loading pin and the edge of the endblock</td>
</tr>
<tr>
<td>( L )</td>
<td>Free length</td>
</tr>
<tr>
<td>( L' )</td>
<td>Length equal to free length plus clamp correction</td>
</tr>
<tr>
<td>( L_f )</td>
<td>Span of the specimen</td>
</tr>
<tr>
<td>( m )</td>
<td>TDCB constant geometry factor</td>
</tr>
<tr>
<td>( M_0 )</td>
<td>Bending moment per unit width</td>
</tr>
<tr>
<td>( n )</td>
<td>Paris law constant</td>
</tr>
<tr>
<td>( n )</td>
<td>Unit vector normal to ( \Gamma )</td>
</tr>
<tr>
<td>( n_1 )</td>
<td>Threshold region curve fitting constant</td>
</tr>
<tr>
<td>( n_2 )</td>
<td>Fast fracture region curve fitting constant</td>
</tr>
<tr>
<td>( N )</td>
<td>Endblock correction</td>
</tr>
<tr>
<td>( N_f )</td>
<td>Number of cycles to failure</td>
</tr>
<tr>
<td>( P )</td>
<td>Load</td>
</tr>
<tr>
<td>( P_C )</td>
<td>Critical load for a given crack length</td>
</tr>
<tr>
<td>( P_{\text{cal}} )</td>
<td>Maximum load obtained during a fracture test for use in calibration</td>
</tr>
<tr>
<td>( P_{\text{max}} )</td>
<td>Maximum load obtained from the fracture test</td>
</tr>
</tbody>
</table>
Nomenclature

\( r_p \)  Irwin plastic zone radius
\( \Delta s \)  Difference in deflection
\( S \)  Stress
\( S_{\text{max}} \)  Maximum applied stress during a fatigue cycle
\( S_{\text{th}} \)  Maximum applied stress during a fatigue cycle for which there is no damage for an infinite number of cycles
\( t_a \)  Adhesive thickness
\( T_f \)  Monotonic failure load per unit width
\( T_g \)  Glass transition temperature
\( T_i \)  Traction vector
\( T_{\text{max}} \)  Maximum load per unit width
\( \Delta u_{\ell} \)  Shear displacements at node \( \ell \)
\( \Delta u_{2\ell} \)  Differences in shear displacements at node \( \ell \)
\( u \)  Nodal displacement
\( U \)  Paris law constant
\( \Delta w_{\ell} \)  Opening displacements at node \( \ell \)
\( \Delta w_{2\ell} \)  Differences in opening nodal displacements at node \( \ell \)
\( w \)  Nodal displacement
\( w' \)  Strain energy density
\( X_{1\ell} \)  Shear force at nodal point \( \ell \)
\( X_i \)  Shear force at nodal point \( i \)
\( X_{\ell} \)  Force at crack tip
\( X_j \)  Mid forces at nodal point
\( z \)  Slope of a plot of \( C \) versus \( a^3 \)
\( Z_{1\ell} \)  Opening force at nodal point \( \ell \)
\( Z_{\ell} \)  Opening force at nodal point \( \ell \)
\( Z_j \)  Mid forces at nodal point
\( Z_i \)  Force at crack tip

**Greek alphabet**

\( \alpha \)  Power law coefficient
\( \delta \)  Displacement of the cross-head of the machine
\( \delta_{\text{min}} \)  Lower displacement of the cross-head of the machine for fatigue testing
Nomenclature

\( \delta_{\text{mean}} \)  Mean displacement of the cross-head of the machine for fatigue testing
\( \delta_{\text{max}} \)  Upper displacement of the cross-head of the machine for fatigue testing
\( \delta_{n0} \)  Mode I separation at damage initiation CZM fitting parameter
\( \delta_{t0} \)  Mode II separation at damage initiation CZM fitting parameter
\( \Delta_{I} \)  Crack length correction for a non-perfectly built-in beam
\( \Delta_{II} \)  Mode II crack length correction
\( \varepsilon \)  Strain
\( \Gamma \)  Arbitrary contour around the crack tip
\( \eta \)  BK law coefficient
\( \sigma \)  Stress
\( \sigma_{a} \)  Cyclic stress
\( \sigma_{nc} \)  Mode I stress CZM fitting parameter
\( \sigma_{tc} \)  Mode II stress CZM fitting parameter
\( \sigma_{y} \)  Yield stress
\( \theta \)  Scarf angle
\( \nu \)  Poisson’s ratio

Abbreviations

Abaqus/CAE  Pre- and post-processor of the abaqus finite element suite
BK  Benzegagh-Kenane
BS  British standard
BSI  British standard institution
CAE  Chromic-acid etch
CBT  Corrected beam theory
CBTE  Corrected beam theory with effective crack length
CFRP  Carbon fibre reinforced plastic
CNC  Computer numerical control
CZM  Cohesive zone modelling
DCB  Double cantilever beam
DSC  Differential scanning calorimetry
DSJ  Double scarf joint
ECM  Experimental compliance method
ELS  End loaded split
ESIS  European structural integrity society
Nomenclature

FE  Finite element
FRMM Fixed ratio mixed mode
ISO  International standards organization
LEFM Linear elastic fracture mechanics
PTFE Polytetrafluoroethylene
MAAXIMUS More affordable aircraft through extended, integrated and mature numerical sizing
SBT Simple beam theory
SEM Scanning electron microscopy
SERR Strain energy release rate
SIF Stress intensity factor
SLJ Single lap joint
TC4 Technical committee 4 on polymers, composites and adhesives
TDCB Tapered double cantilever beam
VCCT Virtual crack closure technique
1. Introduction

1.1. Introduction

Adhesive bonding is a technique which in recent years has seen a large increase in its use in industry. The use of adhesively-bonded joints instead of the traditional types of joining – mechanical fasteners, welding – leads, among other things, to a reduction in weight and the creation of increasingly complicated shapes. This allows a vehicle to be more aerodynamic, since it results in a smoother surface than there would be using mechanical fasteners, i.e. rivets and bolts [1-7]. It makes it possible to join dissimilar and damage-sensitive materials, and adhesively bonded joints have a far better stress distribution in the joint area compared to mechanical fasteners.

Combining adhesive bonding with the use of fibre composites allows very strong and smooth structures to be produced. However, the main advantage of this pairing is the lightness of the resulting structure, at the same time maintaining or even increasing its structural strength.

Nevertheless, despite the increasing use of adhesively bonded joints in today’s manufacturing businesses, most industries have concerns about their usage in anything other than secondary
structures. This is due to the long-term service life uncertainty of adhesively bonded joints especially when subjected to hot/wet environments under cyclic fatigue loading [8-12].

Taking this into account, it is increasingly important for companies to be able to predict the lifetime of bonded joints. With this in mind, a European project MAAXIMUS (More Affordable Aircraft through eXtended, Integrated and Mature nUmerical Sizing) is underway. Its aim is to achieve the fast development and correct-first time validation of a highly-optimised composite fuselage using virtual structure development and composite technology [13].

This study is part of this MAAXIMUS project, and consists of modelling and predicting the failure of different types of bonded composite joints.

1.2. Aims and objectives

The aim of this project is to be able to predict the failure of an adhesively-bonded composite joint, using a numerical program – in this case Abaqus – without the need to constantly perform experimental tests to verify the loads which a particular joint or structure can withstand.

This would be possible by applying a set of pre-defined parameters for the adhesive, which should be valid for any type of joint that uses the same adhesive with equal thickness.

To obtain the data necessary to create and validate the different numerical models, different adhesively bonded joints had to be tested quasi-statically, namely:

- Tapered double cantilever beam, TDCB, and double cantilever beam, DCB, for measuring the critical strain energy release rate, $G_{IC}$, in mode I loading.

- End loaded split, ELS, for measuring the critical strain energy release rate, $G_{IIC}$, in mode II loading.
- Fixed ratio mixed mode, FRMM, for mixed mode testing with a ratio of mode I to mode II component of 4:3.

- Lap joint, SLJ, and double scarf joint, DSJ, for measuring the shear strength of typical adhesively-bonded joints, and to validate the numerical values obtained.

For comparison purposes, two different modelling techniques were used, the virtual crack closure technique and cohesive zone modelling. For the latter, the modelling parameters for TDCB and DCB simulations were compared since they are both pure mode I type of fracture, and this should help validate whether a pre-defined set of parameters can be found for a specific thickness of adhesive.

Fatigue tests were also performed, since it is necessary to ascertain the fatigue threshold for this particular adhesive. Therefore, fatigue tests were executed for both DCB and ELS specimens, i.e. for the mode I and mode II threshold strain energy release rate. For validation purposes, SLJ were also tested in fatigue in order to be able to compare results with those predicted in the numerical simulation.

A secondary objective was to observe whether the peel ply supplied for the project to use in the experimental tests actually provided a good bonding surface or not, thus allowing cohesive failure of the adhesive.

1.3. Thesis structure

The structure of this thesis is divided between the experimental work and the numerical work. A short summary of each chapter follows:

Chapter 2 presents a literature review giving insight into previous work performed in the area of adhesively bonded joints, regarding different types of joints, and the fracture modes in which an adhesive joint can fail. Two different modelling techniques that are generally used for the prediction of adhesively bonded joint failure are also introduced.
Chapter 3 provides a description of the materials used. The procedures employed in the manufacturing of the carbon fibre reinforced plastic, CFRP, plates and the different adhesively bonded joints are outlined. The various testing methods obtained from ISO/British standards and the protocols that were used for all the experimental tests performed are also described.

Chapter 4 presents the experimental results obtained for the different quasi-static tests performed, from which the values of the critical strain energy release rate were obtained for pure mode I and mode II for use in the numerical simulations. Also, for the pure mode I tests, the influence of the adhesive thickness on the value of $G_{ic}$ was investigated. The influence of different scarf angles on the maximum load of the double scarf joints was also examined.

Chapter 5 provides a description of the numerical simulations used for all of the quasi-static experimental tests described in Chapter 4. The simulation results are presented, and comparisons between the numerical and experimental force versus displacement curves are also made.

Chapter 6 presents all of the experimental results of the fatigue tests performed, namely double cantilever beam, DCB, end loaded split, ELS, and single lap joint, SLJ. Using the threshold values for the strain energy release rate from both mode I and mode II tests, numerical simulations were performed for the DCB, ELS, SLJ, and also double scarf joint, DSJ, to predict the maximum load at which the joint will not fail.

Chapter 7 presents a discussion on the results obtained during this study, comparing them with the available literature.

Chapter 8 presents the conclusions obtained from this study for this novel modelling approach, as well as some suggestions for possible future work.
2. Literature review

2.1. Introduction

With the increase in the use of adhesively bonded joints in composite structures by aerospace and high-end vehicle companies, among others, the need to understand the mechanical behaviour of the adhesives has increased in importance.

In this chapter, an overview of the available types of adhesive on the market is given, as well as the main joint designs, which can be optimised for strengthening of the adhesively-bonded joint. Fracture mechanics are reviewed; brief background information is given on the main failure types of bonded joints, the fracture energy of adhesives, and the methods for obtaining these properties. The importance of the study of the fatigue lifetime of a bonded joint is also summarised.

An overview of current finite element (FE) modelling methods is presented, as well as some current modelling approaches for the prediction of the fatigue lifetime of an adhesively-bonded joint. Taking these into account, a new model is described, which was chosen as the approach used for this project, due to its simplicity when compared to the other approaches.
2.2. Adhesives and adhesive joints

2.2.1. Types of adhesive

There is a large variety of adhesives available for different uses. These can be divided into three categories:

- Structural adhesives
- Semi-structural adhesives
- Non-structural adhesives

Non-structural adhesives have a relatively low strength and poor creep resistance at slightly elevated temperatures [14]. Some examples of this type of adhesive are solvent-based adhesives, water-based adhesives, plastisols and elastosols. These may be used, for example, for aesthetic purposes. Semi-structural adhesives, on the other hand, are much stronger, and can be used when their failure will not provoke a critical result. Some examples of these adhesives are hot melts, polyvinyl acetate (PVA) adhesives and pressure sensitive adhesives [15-17].

Structural adhesives are toughened and therefore they can get much higher toughness than that of an unmodified epoxy. They are expected to provide a type of bonding which will provide a good service life, similar to that of the application in which it is applied. These are very tough adhesives, and some common examples are epoxies, certain urethanes and polyurethane (PU) [15, 17-19].

In this project, only epoxy adhesives have been used. Epoxy adhesives are made from an epoxy resin and a hardener. This allows for different mixtures to be made, providing great versatility in the use of epoxy adhesives. They can be divided into two groups, one-part and two-part adhesives. Two-part adhesives start curing at room temperature, as soon as both parts are mixed, with the possibility of significantly reducing the curing time with an increase of temperature [15]. These adhesives need to have the exact part-ratios when mixing the adhesive components, since otherwise they might not form correctly, with the resulting
properties being different to those for which they were developed, in particular lower strength. On the other hand, one-part epoxy adhesives are already pre-mixed, so that the mixture proportions are always exact. Unlike the two-part adhesives, these ones need to be heated to a temperature that is usually over 100°C, for the curing process to begin. These adhesives exist in liquid, paste or film form [15, 18, 20].

2.2.2. Joint design

When designing a joint, it is necessary to maximise tensile and shear stresses and minimise peel and cleavage stresses, in order to optimise the bonded area where the load is distributed, in order for the joint not to debond when it has a load applied to it. Under stress, the joint can be subject to normal, shear, cleavage or peel stresses, see Figure 2.1. Normal stresses occur when a stress acts perpendicular to the joint plane and they can be either of a tension or compressive nature. Shear stresses occur when a load is applied parallel to a face of a material; cleavage stresses occur when a stress is concentrated at one edge and exerts a force on the bond; peel stresses occur when there is a bending moment to the joint [21-24].

![Figure 2.1 – Types of joint stresses applied to the adhesive layer [23.]](image-url)
In order to avoid joint weakness, a variety of joints exist that are suitable for different types of application, as shown in Figure 2.2. It can be observed that the adhesive layer is either parallel or at an angle to the adherend, thus allowing the adhesive to be loaded in a shear or tension state, which, as previously stated, is when an adhesively-bonded joint can achieve the best performance.

![Types of adhesively-bonded joints](image)

**Figure 2.2** – Types of adhesively-bonded joints used in mechanical design: (a) single lap; (b) double lap; (c) scarf; (d) bevel; (e) step; (f) butt strap; (g) double butt strap; (h) tubular lap [25].

In order to examine and optimise the geometrical parameters of an adhesively-bonded joint, two methods are used, the analytical and finite element methods.

One of the earliest analytical solutions was proposed by Volkersen [26-29], for the single lap joint, since it is one of the most common joints used in practice. His analysis used a simple two dimensional, linear elastic adhesively-bonded joint model, and he introduced the concept of differential shear. It assumes that the adhesive can only be deformed in shear and that the adherends only deform in tension, as they are considered to be elastic and not rigid, see Figure 2.3 [27].
This model and other subsequent models have been developed to optimise the adhesively-bonded joint through the prediction of the shear stress distribution throughout the joint. These models have, in recent decades, been facilitated by the use of finite element analysis. This particular model (Volkersen) was created for a single lap joint, from which the shear stress distribution, exemplified in Figure 2.4, was obtained. This model showed that the peak stress in the adhesive increases with increase of the adhesive shear modulus; it increases with a decrease of the substrate modulus, $E$, and of the adhesive thickness. From this model, it was also possible to see that the joints suffered greater stress influence at the ends of the overlap, with its value diminishing as it progresses towards the centre of the overlap.

Figure 2.3 – Single lap joint analysed by the Volkersen method [27].

Figure 2.4 – Volkersen’s adhesive shear stress distribution [27].
For long overlap joints, the peak adhesive stress is independent of the joint length. However, different designs provide different structural integrity to a joint and have different analytical solutions as they also have different parameters to take into account.

The analysis of these joints is usually performed by considering plane strain only, since most of the bonded joint is subject to this state (across the width), which also provides a more conservative result than that obtained considering plane stress. Initially, this type of study is first run as a two dimensional analysis since this has the advantage of reducing the computational time of a model, which then can be used as reference if a 3D analysis is performed.

### 2.3. Fracture mechanics

#### 2.3.1. Failure modes of adhesively-bonded composite joints

In any structure, cracks are always a big concern, since they can lower its strength below the minimum needed for a particular purpose. For this reason, it became necessary to evaluate the behaviour of adhesively-bonded joints once a crack had initiated. In these joints, failure can occur in three different ways:

- Interfacial failure,
- Interlaminar failure,
- Cohesive failure.

When an adhesively-bonded joint fails at the interface, it is possible to see a clean debond between the surface of the adhesive and that of the substrate. This type of failure occurs when the surface treatment prior to debonding is not effective. An interlaminar failure occurs when the substrate suffers delamination, leaving the adhesive bondline intact. This may happen when the bonded joint is stronger than the substrates. Finally, a cohesive failure occurs when the rupture of the bonded assembly occurs within the adhesive. Figure 2.5 shows the different types of failure of an adhesive joint [30].
For the linear-elastic mode I see Fig. 2.5 – Designation of failure patterns for substrates and adhesives with a) showing interfacial failure; b) interlaminar failure; and c) cohesive failure [30].

### 2.3.2. Modes of failure

For the lifetime prediction of adhesively-bonded joints, the methods used are based on linear-elastic fracture-mechanics (LEFM), for which there are three main modes of loading: mode I or opening mode, mode II or in-plane shearing mode, and mode III or tearing mode, see Figure 2.6.

**Figure 2.6** – Three main modes of loading [31].
Of these failure modes, mode I, the opening mode, is considered to be the most important, since it is the most commonly seen mode of failure due to it being the mode which requires the smallest energy. This is followed by mode II and finally mode III, which requires the greatest energy input.

These three modes of loading are applied to different fracture mechanics tests in which fracture of already existing flaws in the material occurs. One of the main methods used to characterise this crack growth is the energy balance approach [22], which will be discussed in the next section.

### 2.3.3. Fracture energy

An approach applied using a fracture mechanics method, is the energy balance approach [22, 32]. Using this approach, a value of the fracture energy of the adhesive, $G_C$, is obtained, which describes the energy necessary for crack propagation to occur. A general equation that defines this value is:

$$ G_C = \frac{P_C}{2B} \cdot \frac{\partial C}{\partial a} $$

in which $P_C$ is the critical load for a given crack length, $B$ the width of the adhesive, $C$ the compliance – ratio between the displacement and the load –, and $a$ the crack length. The value of the compliance is measured with respect to the value of the crack length.

This is a material property which is specific to the adhesive, as well as its thickness, but also its locus of failure. If the failure is cohesive, then the value of $G_C$ is independent of the locus of failure, and therefore the value obtained will be a good property value for a pure mode or mixed mode $G_C$. However, if it is not cohesive the fracture energy becomes dependent on its failure locus, since it is no longer restricted to the adhesive, and subsequently not the real $G_C$ of the adhesive.
2.3.4. Plastic zone size

A plastic region tends to form at the crack tip, known as the plastic zone. Irwin suggested a model for a circular plastic zone of radius, \( r_p \), shown in Figure 2.7, calculated using Equations 2.2 and 2.3.

\[
\begin{align*}
    r_p &= \frac{E_a G_c}{2\pi \sigma_y^2}, \text{ for plane stress} \quad (2.2) \\
    r_p &= \frac{E_a G_c}{6\pi \sigma_y^2(1-\nu^2)}, \text{ for plane strain} \quad (2.3)
\end{align*}
\]

in which \( E_a \), \( G_c \), \( \sigma_y \) and \( \nu \) are the elastic modulus, the fracture energy, the yield stress and Poisson’s ratio of the adhesive, respectively. Figure 2.8 shows how the plane stress and plane strain plastic zones relate to each other, both in two and three dimensions, where it is easily seen that plane stress is greater than plane strain; however, more importantly, plane strain has a greater effect because it is present across the width of the specimen, rather than only affecting the sides of the specimen.

![Irwin model of plastic zone at crack tip.](image)

**Figure 2.7** – Irwin model of plastic zone at crack tip.
The size of the plastic zone has been found to influence the relationship between the fracture energy and the bondline thickness [34, 35], which is discussed in greater detail in the next section. Figure 2.9 shows the difference in plastic zone obtained for mode I and mode II, and how they propagate through the crack.

![Figure 2.8 - Illustration of the crack and plastic zone in 2D and 3D [33].](image)

**Figure 2.8** – Illustration of the crack and plastic zone in 2D and 3D [33].

![Figure 2.9 - Illustration of the plastic zone under: a) mode I; b) mode II.](image)

**Figure 2.9** – Illustration of the plastic zone under: a) mode I; b) mode II.

### 2.3.5. Bondline thickness

When using tough adhesives, such as epoxy adhesives, the thickness of the adhesive layer has been shown to modify the measured value of the fracture energy [36–41]. These studies revealed that when the thickness of the adhesive increases, so does the value of the fracture energy, as shown in Figure 2.10.
Figure 2.10 – Maximum fracture energy versus bondline thickness: 
a) mode I tapered double cantilever beam (TDCB) test; 
b) mode II end-loaded shear joint (ELSJ) test [42].

This increase in the value of the fracture energy is shown to happen when the plastic zone size (see previous section) is originally smaller than the bondline thickness, which means that it is restricted between the substrates, as illustrated in Figure 2.11.

Figure 2.11 – Effect of the bondline thickness on the plastic zone: 
a) thick bondline (>2\(r_p\)); b) thin bondline (<2\(r_p\)) [40].

By increasing the bondline thickness to the same size as the plastic zone, it will no longer be restricted between the substrates, which will increase the value of the fracture energy to its correct value, independent of the plastic zone size, being dependent only on the bondline thickness [36, 40, 43].
2.3.6. Test methods

In order to calculate the adhesive fracture energy mentioned in Section 2.3.2, different tests are performed to obtain the values of fracture energy, whether it is mode I, mode II or mixed mode. These tests follow existing standards or protocols, for which there are specific specimen characteristics to follow, and also the necessary equations for the calculation of $G_C$. An overview of how these tests are performed is described next:

2.3.6.1. Mode I

In order to measure the mode I fracture energy, $G_{IC}$, constant displacement rate tests are performed following a British Standard, BS, and an International Standards Organization, ISO, Standard [44, 45]. This can be done either by using a tapered double cantilever beam, TDCB, specimen, see Figure 2.12a, or a double cantilever beam, DCB, specimen, see Figure 2.12b.

![Diagram of Mode I test specimens](image)

**Figure 2.12** – Mode I test specimens:

a) tapered double cantilever beam, TDCB; b) double cantilever beam, DCB.
The TDCB specimens have a curvature with a constant geometry factor, \( m \), equal to

\[
m = \frac{3a^2}{h^3} + \frac{1}{h'}
\]

(2.4)

in which \( a \) is the crack length, and \( h' \) is the height of the TDCB beam at that crack length. This allows for the compliance to increase linearly throughout the test. This is then used, for example, in the simple beam theory equation for the calculation of the fracture energy:

\[
G_{IC} = \frac{4P_C^2}{E_SB^2} \cdot m
\]

(2.5)

in which \( P_C \) is the critical load applied to the specimen, \( E_s \) is the flexural modulus of the substrate and \( B \) is the width of the specimen.

On the other hand, the DCB specimens are flat, and may or may not have drilled holes, making it necessary to bond endblocks to the adherends, as shown in Figure 2.12b, for thin specimens. From this test, the measured load versus crack length can be obtained, which provides the necessary data for the calculation of \( G_{IC} \) [44, 45]. For example, the simple beam theory equation for the calculation of the fracture energy is:

\[
G_{IC} = \frac{4P_C^2}{E_SB^2} \left( \frac{3a^2}{h^3} + \frac{1}{h} \right)
\]

(2.5)

2.3.6.2. Mode II

In order to measure the mode II critical strain energy release rate, \( G_{IIc} \), constant displacement rate tests are performed. However, unlike for mode I, there is no ISO/BS standard for a mode II test. The most used test configurations are: the end loaded split, ELS, test; the three-point end-notched flexure, ENF, test; and the four-point end-notched flexure, 4-ENF, test.

- **End loaded split (ELS)**

The end loaded split, ELS, test follows an European Structural Integrity Society, ESIS, protocol [46]. This method uses flat specimens and also a clamp which prevents any vertical movement at one end of the specimen, while the other end is subjected to a vertical constant displacement rate, as shown in Figure 2.13.
The data is then collected and applied to the formulations available in the protocol, for the calculation of $G_{IIc}$. One of the calculation methods available for obtaining the value of $G_{IIc}$ is the simple beam theory for which:

$$ G_{IIc} = \frac{9P^2P^2a^2}{4b^2h^3E_s} \tag{2.6} $$

This test has the disadvantage of the ELS specimen being more susceptible to large displacements and requires additional compliance correction due to the use of the clamp.

- **Three-point end-notched flexure (ENF)**

The three-point end-notched flexure, ENF, test is the most common test used to obtain $G_{IIc}$ due to its uncomplicated test fixture and the simplicity of the manufacture of the ENF samples. This test does not follow an ISO standard, but [47] references a Japanese Industrial Standards (JIS K 7086:1993) for this test. Figure 2.14 shows the setup used for this test.

$$\text{Figure 2.13 – Mode II test setup for end loaded split, ELS, specimen.}$$

$$\text{Figure 2.14 – Mode II test setup for an end-notched flexure (ENF) specimen.}$$
The data is then collected and applied to the available formulations for the calculation of $G_{IIc}$. One of the calculation methods available for obtaining the value of $G_{IIc}$ is the simple beam theory for which:

$$G_{IIc} = \frac{9P_C^2 a^2}{16b^2 h^3 E_s} \tag{2.7}$$

A problem is that this test does not have good crack propagation stability, and usually only the crack initiation data can be used to obtain the initiation values of $G_{IIc}$ [48].

- **Four-point end-notched flexure (4-ENF)**

The four-point end-notched flexure, 4-ENF, test is loaded in four point bending unlike the three points used in the ENF. This has the advantage of providing stable crack propagation. Figure 2.15 shows the setup used for this test.

![Figure 2.15 – Mode II test setup for a four point end-notched flexure (4-ENF) specimen.](image)

The data are then collected, and the value of $G_{IIc}$ can be obtained from the compliance calibration:

$$G_{IIc} = \frac{P_C^2}{2b} \frac{dC}{da} \tag{2.8}$$

Although this test has been put forward for an international standard, it has been reported that it suffers from large friction effects, increasing the value of $G_{IIc}$.
Considering these three tests, it was decided that the ELS test would be the one to use in this project, since it would provide the most reliable mode II data for the adhesive.

### 2.3.6.3. Mixed mode

In order to measure the mixed mode critical strain energy release rate, $G_{I/II}$, constant displacement rate tests are performed. The most used test configurations are: the mixed mode bending, MMB, test; the fixed ratio mixed mode, FRMM, test; and the mixed mode flexure, MMF, test.

- **Mixed mode bending (MMB)**

The mixed mode bending, MMB, test is an ASTM standard method [49] for the calculation of $G_{I/II}$ of an adhesive. This method can be used to determine the value of $G_{I/II}$ for various different ratios of mode I/II loading, and it also delivers mode mix values which remain constant with crack growth [50]. Figure 2.16 shows the setup used for this test.

![Mixed mode test setup for mixed mode bending, MMB, specimen.](image)
The setup used for this test is quite complicated, and needs particular attention to the design, as it is necessary to reduce the geometric non-linear effects which the loading lever introduces. Another issue is the instability of the crack growth that can occur [50].

- **Fixed ratio mixed mode (FRMM)**

In order to measure the mixed mode fracture energy, \( G_{II/C} \), the method selected was the fixed ratio mixed mode, FRMM, test which provides a \( G_I/G_{II} \) ratio of 3:4. This test is performed under a constant displacement rate, and is performed following an ESIS protocol [51]. Its setup is very similar to that which is used for mode II (see Figure 2.13), except for the placement of the specimen, which is inverted compared to the ELS test, as shown in Figure 2.17.

![Figure 2.17 – Mixed mode test setup for fixed ratio mixed mode, FRMM, specimen.](image)

The data is then collected and applied to the equations available in the protocol. Since this is a mixed mode test, it is necessary to obtain values for the mode I ratio of the fracture energy, \( G_{IC}^{mixed} \), and mode II ratio of the fracture energy, \( G_{II/C}^{mixed} \). The value of the fracture energy, \( G_{II/C} \), for this mixed mode ratio is then obtained by adding the values of \( G_{IC}^{mixed} \) and \( G_{II/C}^{mixed} \). One of the calculation methods to obtain these values is the simple beam theory for which:

\[
G_{IC}^{mixed} = \frac{3P_C^2a^2}{B^2E_s h^3} \tag{2.9}
\]

\[
G_{II/C}^{mixed} = \frac{9P_C^2a^2}{4B^2E_s h^3} \tag{2.10}
\]
\[ G_{I/II} = G_{IC}^{mixed} + G_{II}^{mixed} \]  

(2.11)

- **Mixed mode flexure (MMF)**

The mixed mode flexure, MMF, test, uses a simple test rig, similar to that of the ENF test (see Figure 2.14), as shown in Figure 2.18. The mixed mode ratio present in this test is equal to that in the ELS test \( G_I/G_{II}=4/3 \).

![Figure 2.18 – Mixed mode setup for mixed mode flexure, MMF, specimen.](image)

The value of \( G_{I/II} \) for this mixed mode ratio can be obtained from:

\[ G_{IC}^{mixed} = \frac{3P_C^2a^2}{4B^2E_s h^3} \]  

(2.12)

\[ G_{II}^{mixed} = \frac{9P_C^2a^2}{16B^2E_s h^3} \]  

(2.13)

Examining these different mixed mode tests, it was decided that the FRMM test would be the one to use in this project, since it would provide reliable mixed mode data for the adhesive, and also simplify the modelling since, as described, both ELS and FRMM have a similar setup.

With these data from the mode I, mode II and mixed mode tests, a failure envelope can be created, which allows deduction of whether the relationship between the mode I and mode II fracture energy is linear, convex or concave, as illustrated in Figure 2.19.
This relationship can be obtained using different criteria, for example, the linear criterion, the power law and the BK law, that follow Equations 2.14, 2.15 and 2.16, respectively.

\[
\left( \frac{G_{IC}^{mixed}}{G_{IC}} \right) + \left( \frac{G_{IIIC}^{mixed}}{G_{III}} \right) = 1 \tag{2.14}
\]

\[
\left( \frac{G_{IC}^{mixed}}{G_{IC}} \right) \alpha + \left( \frac{G_{IIIC}^{mixed}}{G_{III}} \right) \alpha = 1 \tag{2.15}
\]

\[
G_{C}^{mixed} = G_{IC} + (G_{IIIC} - G_{IC}) \left( \frac{G_{II}^{mixed}}{G_{IC}^{mixed} + G_{II}^{mixed}} \right)^{\eta} \tag{2.16}
\]

As is shown in the equations, it is only necessary for there to be three points of reference to build a failure envelope of an adhesive, and typically epoxy adhesives have a linear relationship rather that a convex or concave one [52, 53]. The critical strain energy release rate values have been shown to have \( G_{IIIC} \) much larger than \( G_{IC} \) [54], with the value for the mixed mode critical strain energy release rate fitting in between \( G_{IC} \) and \( G_{IIIC} \) [52, 53].
2.4. **Fatigue life of adhesively-bonded joints**

Materials are subject to vibrating or oscillating forces in many applications, e.g. aeroplanes, cars, machinery. This condition affects the behaviour of the material, especially compared to conditions when only a static load is applied. Over time these forces weaken the material and cause failure of the component [55].

This progressive and localised structural damage is called fatigue and is the primary reason for the failure of structural components. The fatigue crack growth rate is affected by many factors, amongst them the frequency of the cyclic stress, the operating environment and the magnitude value of the cyclic stress applied to the component. Fatigue crack propagation behaviour has three phases: initiation, propagation and finally fracture, see Figure 2.20.

![Fatigue crack growth area](image)

**Figure 2.20** – Fatigue failure in a circular shaft [56].

Figure 2.21 shows the fatigue behaviour obtained using a fracture mechanics test (DCB), of the crack growth rate, \( \text{da/dN} \), as a function of the maximum value of \( G \) in a fatigue cycle, \( G_{\text{max}} \).
The fatigue behaviour of most materials can be divided into three regions, as shown in Figure 2.21. Below region I, the maximum strain energy release rate, \( G_{\text{max}} \), is too low to propagate a crack; region II covers data in which the rate of crack growth changes almost linearly with change in stress intensity variation; in region III, small increases in stress intensity produce a relatively large increase in crack growth rate since the material is nearing the point of fracture, in this case \( G_{\text{IC}} \) [11, 58-60].

There are two ways of viewing fatigue data: (1) by representing the crack growth rate, \( da/dN \), against the maximum strain energy release rate, \( G_{\text{max}} \); (2) using S-N curves, where the maximum applied stress during a fatigue cycle, \( S_{\text{max}} \), is matched against the number of cycles to failure, \( N_f \).

The first method uses data obtained from fracture mechanics tests such as the tapered cantilever beam (TDCB), in which quasi-static and fatigue tests are performed to obtain the fatigue properties of an adhesive system. These data can then be used to create a crack growth rate versus maximum strain energy release rate similar to what is shown in Figure 2.21.
The crack propagation for the linear part, region II, can be represented by the Paris law as reported in [61]:

\[
\frac{da}{dN} = U (G_{\text{max}})^n
\]  

(2.10)

where \( a \) is the crack length, \( N \) is the number of cycles and \( U \) and \( n \) are fitting constants which depend on the material, the environment and the loading. \( G_{\text{max}} \) is used in preference to the strain energy release rate amplitude, \( \Delta G (\Delta G = G_{\text{max}} - G_{\text{min}}, \text{in which } G_{\text{min}} \text{ is the minimum value of } G \text{ in a fatigue cycle}) \), because, with some polymeric materials, fracture surface interference on unloading may artificially raise the value of \( G_{\text{min}} \) and therefore reduce the value of \( \Delta G \) [62].

This equation is used to fit the middle region, region II, (see Figure 2.21) of the fatigue crack growth data. However, the sigmoidal form of the relationship between logarithmic \( G_{\text{max}} \) and \( da/dN \), as seen in Figure 2.21. A lower-bound occurs at the fatigue threshold, \( G_{\text{th}} \), (region I) where crack growth is negligible, and an upper-bound occurs which is equivalent to the inter-laminar fracture energy, \( G_c \), (region III) measured at a constant displacement-rate [61, 63]. Therefore, a better relationship between logarithmic \( G_{\text{max}} \) and \( da/dN \) is expressed by [61, 63, 64]:

\[
\frac{da}{dN} = U G_{\text{max}}^n \left[ 1 - \left( \frac{G_{\text{th}}}{G_{\text{max}}} \right)^{n_1} \right] \left[ 1 - \left( \frac{G_{\text{max}}}{G_c} \right)^{n_2} \right] 
\]  

(2.11)

where \( U, n, n_1 \) and \( n_2 \) are constants obtained by fitting the above expression to the experimental data. The use of Equation 2.11 instead of Equation 2.10 has been suggested and used in recent studies, e.g. [57, 64, 65]. This method can be used to predict the S-N curves used in the second method.

The other method makes use of S-N curves. Here the amplitude of the cyclic stress, \( S \), is matched against the number of cycles to failure, \( N_f \), showing the crack initiation and propagation period until failure occurs, see Figure 2.22. The S-N curves are normally used to represent the fatigue performance of adhesively-bonded joints like the single lap joint [66-68].
2.5. Modelling methods

The increase in the usage of bonded joints, as well as the reduction of experimental testing for time and cost reduction purposes, has meant that the use of finite element analysis, FEA, for bonded joints has increased. An accurate FE model of an adhesively bonded joint provides information on the differences in the basic mechanical properties, the existence of high stress gradients in certain regions of the joints, among other data the user may request. These help future applications of adhesive bonding by allowing different parameters to be selected, thus improving the process for joint manufacture [69]. The most typical methods used in the prediction of the failure of a bonded joint are: the virtual crack closure technique, VCCT; the J-Integral; and cohesive zone modelling, CZM.

Figure 2.22 – S-N Curve for a single lap joint [66].
2.5.1. Virtual crack closure technique (VCCT)

The virtual crack closure technique, VCCT, is an analytical method commonly used in the prediction of failure in composite structures based on fracture mechanics, see Figure 2.23. This method is used to calculate the strain energy release rate, and it assumes that when a crack grows, the energy release is equal to that which is required to close the crack. This method has two different ways of being applied:

- Crack closure using two analysis steps
- The modified crack closure method

![Figure 2.23 – The virtual crack closure technique (VCCT) [26].](image)

The first method, as the name suggests, consists of two steps and is based on Irwin’s crack closure integral, as reported in [70]. It assumes that the energy released by the opening of the crack from position 1 to 2 (see Figure 2.23) is equal to energy necessary to close the crack again.

In the first step, see Figure 2.24a, node ℓ is originally closed in order to record the forces applied here in mode I and mode II. In the second step, see Figure 2.24b, node ℓ is opened, creating node ℓ and ℓ* and allowing for the displacements of these nodes to be measured [26, 70].
Figure 2.24 – Two step virtual crack closure method: a) First step; b) Second step [70].

The energy needed to close the crack can then be calculated using the opening forces and displacements of the nodes at the crack tip. The work $\Delta E$ necessary to close the crack along one element side can be calculated as:

$$
\Delta E = \frac{1}{2} [X_{1\ell} \cdot \Delta u_{2\ell} + Z_{1\ell} \cdot \Delta w_{2\ell}]
$$

(2.12)

where $X_{1\ell}$ and $Z_{1\ell}$ are the shear and opening forces at nodal point $\ell$ to be closed, respectively, as shown in Figure 2.24a, and $\Delta u_{2\ell}$ and $\Delta w_{2\ell}$ are the differences in shear and opening nodal displacements at node $\ell$, respectively, as shown in Figure 2.24b.

The second method uses the same assumptions as the crack closure method using two analysis steps. It assumes that the crack extension does not significantly alter the state at the crack tip, when propagating from node i to node k – see Figure 2.25.
Figure 2.25 – One step virtual crack closure method [70].

Therefore the displacements behind the crack tip at node $i$ are approximately equal to the displacements behind the original crack tip at node $\ell$ [26, 70]. The work, $\Delta E$, necessary to close the crack along one element side can be calculated as:

$$\Delta E = \frac{1}{2} [X_i \cdot \Delta u_\ell + Z_i \cdot \Delta w_\ell] \quad (2.13)$$

where $X_i$ and $Z_i$ are the shear and opening forces at nodal point $i$, respectively, and $\Delta u_\ell$ and $\Delta w_\ell$ are the shear and opening displacements at node $\ell$, respectively, as shown in Figure 2.25.

The equations for the calculation of the strain energy release rate, $G_I$ and $G_{II}$ for mode I and mode II respectively, are defined in Equations 2.12 and 2.13 for four-noded elements and in Equations 2.14 and 2.15 using eight-noded elements [70-72].

$$G_I = -\frac{1}{2\Delta a} Z_i (w_l - w_l^*) \quad (2.14)$$

$$G_{II} = -\frac{1}{2\Delta a} X_i (u_l - u_l^*) \quad (2.15)$$

$$G_I = -\frac{1}{2\Delta a} [Z_i (w_l - w_l^*) + Z_j (w_m - w_m^*)] \quad (2.16)$$

$$G_{II} = -\frac{1}{2\Delta a} [X_i (u_l - u_l^*) + X_j (u_m - u_m^*)] \quad (2.17)$$
where $\Delta a$ is the length of the elements at the crack tip, $X_i$ and $Z_i$ are the forces at the crack tip (nodal point $i$), $X_j$ and $Z_j$ are the forces at the mid-side node in front of the crack tip, and $u$ and $w$ are the nodal displacements for the different nodes, as illustrated in Figure 2.26.

**Figure 2.26** – Virtual crack closure technique for two dimensional solid elements considering: a) four noded elements; b) eight noded elements [70].

This method is largely used due to its simplicity and accuracy [60]. However, it has some disadvantages; in particular, when applying the VCCT method, it is necessary to know beforehand the exact location of the debonding [73]. Another big concern with the use of this method has to do with its application when a crack occurs at a bi-material interface, i.e. interfacial failure in an adhesive joint. Because of the different material properties, the fracture energy demonstrates a non-uniform and oscillatory value as the mesh around the crack tip becomes smaller, which has the consequence of the mixed mode ratio being undefined [22].
2.5.2. J-integral

The J-integral method is used to calculate the fracture energy of non-linear elastic materials. This method was developed by Rice [74], and defines an arbitrary contour around the tip of the crack, $\Gamma$, as shown in Figure 2.27.

![J-integral contour path surrounding a crack-tip](image)

**Figure 2.27** – J-integral contour path surrounding a crack-tip [75].

The J-integral is defined as:

$$ J = \int_{\Gamma} w' \, dy - T_i \frac{\partial u_i}{\partial x} \, ds $$  \hspace{1cm} (2.18)

where $w'$ is the strain energy density, $T_i$ is the traction vector, $ds$ is the increment of the arc length along the contour $\Gamma$ and $u$ is the displacement vector. The strain energy density, $w$, and the traction vector, $T_i$, are defined as:

$$ w' = \int_0^{\varepsilon_{ij}} \sigma_{ij} \, d\varepsilon_{ij} $$  \hspace{1cm} (2.19)

$$ T_i = \sigma_{ij} n_j $$  \hspace{1cm} (2.20)

where, $\sigma$, $\varepsilon$ and $n$ are the stress, strain, and the unit vector normal to $\Gamma$, respectively.

For linear elastic materials [76-79], the J-integral, $J$, is the same as the strain energy release rate, $G$, which is related to the stress intensity factor, $K$, as can be seen in the following equations:
\[
J = G = \begin{cases} 
\frac{K^2}{E} & \text{Plane stress} \\
\frac{K^2}{E} (1 - \nu^2) & \text{Plane strain}
\end{cases}
\] (2.21)

where \(\nu\) is Poisson’s ratio and \(E\) is the Young’s modulus of the material.

Many papers in the literature affirm that the J-integral has the excellent advantage of being able to be easily implemented into finite element codes like Abaqus, and also that by using this method, the need to make extrapolated guesses of the exact displacement field in the vicinity of the crack tip is no longer required [22, 77, 80].

However, just like the VCCT method, the J-integral also needs the exact location of the debonding to be known [73], and since the VCCT is also an easier and quicker method to implement in Abaqus and not as mesh sensitive, it was chosen over the J-integral as a comparison method.

### 2.5.3. Cohesive zone modelling (CZM)

Cohesive zone models were introduced by Barenblatt and Dugdale for elastic-plastic fracture in ductile metals, and later by Hillerborg et al. for quasi-brittle materials [81, 82]. In recent decades, the importance of cohesive zone models for describing fracture in engineering materials has been recognised. Cohesive zone models have been used with excellent success when the crack path is already known, either due to the structure of the material, e.g. a laminate composite, or from experimental data.

In CZM, the degrading mechanisms next to a crack tip are viewed as a discrete line or plane and a traction-separation curve across this line/plane represents the degrading mechanisms in the fracture process zone [26], see Figure 2.28. For ductile fracture, the two most important parameters in the cohesive zone model appear to be the tensile strength, \(f_t\), and the fracture energy, \(G_c\) – with dimensions, \(J/m^2\) – which is the work needed to create a unit area of a fully-developed crack [83].
The fracture energy is defined as:

$$G_c = \int \sigma du$$ \hspace{1cm} (2.22)

where $\sigma$ and $u$ are the stress and the displacement across the fracture process zone. For solids such as ceramics, that follow a more brittle decohesion relation, as can be seen in Figure 2.28(c), the shape of the traction-separation curve plays an important role in determining the mechanical behaviour of the interface in addition to the other interfacial parameters such as the cohesive energy [84]. However, the shape of the traction-separation curve requires previous experimental “tuning” to obtain good results, and, as was the case in the previous methods, it is necessary to know beforehand where the failure will occur.

It is possible to consider two cohesive zone model behaviours: element based (cohesive elements) and surface based (cohesive contact).

### 2.5.3.1. Cohesive elements

In Abaqus, cohesive elements primarily address two problems: adhesive joints and delamination [85]. Cohesive elements do not correspond to any physical material, but represent the cohesive forces which occur when the material elements are being pulled apart, defined by the traction-separation curve [86]. Figure 2.29 illustrates the use of cohesive elements in substitution of the adhesive layer for a mode I failure.
Cohesive contact is a simplified and easier way to model a cohesive behaviour, using contact interactions and a traction-separation constitutive model. It is defined as the interaction between two surfaces, in which these two surfaces are paired and give a cohesive property. One of the surfaces is defined as the master surface whilst the other is the slave surface, forming a line of nodes, which are bonded together in the simulation, as exemplified in Figure 2.30 [88].

Since these two CZM models take into consideration different aspects of the tested specimen, i.e. the cohesive element has the thickness of the adhesive while the cohesive contact has only surface interaction, both models have the same shape of the traction-separation curves, but with different CZM parameters. The consequence, once the CZM fitting parameters for the traction-separation curve are obtained, is that both models give the same result [89, 90], as shown in Figure 2.31.
Some disadvantages in the implementation of the cohesive zone model are the numerical instabilities often encountered in the simulations – this happens when the mesh size is too coarse, leading to oscillations in the global load-displacement behaviour of the structure or even a diverging analysis [87, 91, 92]. Another issue that occurs when using cohesive zone modelling are the errors that arise when carrying out mixed mode simulations, even though the pure mode simulations provide good results [93].

Initially both VCCT and CZM models were used for comparison purposes. However, the final numerical simulations were performed using only CZM, since these were bonded joints that had no initial crack. Therefore, it was not possible to use VCCT, as it requires an initial crack for it to work.

Figure 2.31 – Comparison between numerical results using cohesive elements and cohesive contact (also known as cohesive surfaces) [88].
2.6. Fatigue prediction approach

2.6.1. Available methods

In previous research, different modelling approaches have been used for the prediction of the fatigue life of a joint [61, 94-97], but for the objectives of this project, there are three approaches that stand out:

1. The modelling approach proposed by Hadavinia et al. [95], predicts the S-N curve via the integration of the modified Paris law. The method uses the modified Paris law (Equation 2.11) to describe fracture mechanics data. This equation can be rearranged and integrated to obtain the number of cycles to failure, $N_f$.

\[
N_f = \int_{a_0}^{a_f} \frac{1}{DG_{max}^n} \frac{1}{1-\left(\frac{G_{th}}{G_{max}}\right)^{n_2}} da
\]  

where $a_0$ and $a_f$ are the initial and final crack lengths, respectively. The variable $G_{max}$ is a function of the crack length, $a$, and of the maximum load per unit width applied in a fatigue cycle, $T_{max}$.

\[
G_{max} = G_{max}(T_{max}, a)
\]

The relationship between $G_{max}$ and $a$ for a given $T_{max}$, is obtained from analytical solutions available for the specific joint design, for example, for the single lap joint, various models can be used [95, 98]: the Kinloch-Osiyemi (KO) model [57] – considers that mode I is the main type of failure. The Fernlund, Papini, McCammond and Spelt (FPMS) model [99, 100] – considers contributions from the axial strain, generated by the load applied to the joint, and from the induced bending moments, caused by rotation of the substrates. The Krenk and Hu (KH) model [101, 102] – considers both mode I and mode II contributions, and the reduction of bending moment due to the rotation of
the substrates. Finally, the relevant solution to Equation 2.24 is implemented in Equation 2.23 for each model, and the combined expression is integrated to give a prediction for the lifetime of the bonded joint [95]. Figure 2.32 shows the result obtained on the prediction of the variation of the maximum applied load per unit width applied in a fatigue cycle to monotonic failure load, $T_{\text{max}}/T_f$, versus log $N_f$ curve of a single lap joint, considering different analytical models.

![Figure 2.32](image)

**Figure 2.32** – Comparison between the experimental result and different analytical model results of the variation of the maximum applied load per unit width applied in a fatigue cycle to monotonic failure load, $T_{\text{max}}/T_f$, versus the logarithmic number of cycles, log $N_f$ [95].

As can be seen, all predictions give very conservative results, and only one result is close to the experimentally obtained results. Therefore, even though these models give reasonable predictions, for more accurate and quicker results, this would not be an ideal model to follow.

2. A fatigue life prediction approach proposed by Quaresimin and Ricotta [103], describes the joint lifetime as the sequence of a crack nucleation phase followed by a propagation phase. An FE model is performed using property
and geometry data of the materials used in the joints, from which a generalised stress intensity factor, normalised with respect to the normal tensile stress, SIF $H_0$, can be calculated. Once this is found, the number of cycles can be obtained from a reference scatter band. Different probabilistic of survival (P.S.) of the fatigue lives to crack initiation can be evaluated, as shown in Figure 2.33.

![Figure 2.33](image)

**Figure 2.33** – Experimental 10-90 % scatter band of the life to crack initiation for spew fillet joints compared to the model predictions (at 50% probability of survival) [103].

For the prediction of the propagation phase, non-geometrical FE analyses are necessary for the calculation of the strain energy release rate, SERR; due to the large out-of-plane displacements of the cracked joints. This in turn needs lots of computational time to calculate the SERR trends as a function of crack length and applied stress. Integration limits are then defined for use in the Paris law, which is then integrated, thus obtaining the number of cycles for a crack to propagate. Once again, a probability of survival can be applied for the prediction, as shown in Figure 2.34.
As can be seen, good agreement is obtained when using this method. However, it is a complex and time consuming approach.

3. Abdel Wahab et al. [104], propose a fatigue crack propagation model based on numerical integration of the fatigue crack growth law from an initial to a final crack growth size. This method aims to predict an load versus number of cycles to failure, L-N, curve, using crack growth data obtained from fracture mechanics tests, and applying them in FE models. First, it is necessary to determine the number of crack increments required for the numerical integration. Then the value of SERR, $G$, can be determined for any crack length, using the finite element analyses. From this, two possible criteria may be considered: the total SERR ($G_{I}=G_{I}+G_{II}$), or the mode I SERR ($G_{I}$). These values are then used for calculating $da/dN$, which is then used to calculate the number of cycles to failure between an initial crack length and a final crack length.

$$\frac{da}{dN} = 1.25 \times 10^{-35} \times G^{13.4}$$  

(2.25)


\[ N_f = \int_{a_0}^{a_f} \frac{1}{\frac{da}{dN}} \, da \] (2.26)

Figure 2.35 shows the results obtained for the prediction of the lifetime of the single lap joint, considering total SERR, \( G_T \), and also only mode I SERR, \( G_I \).

\[ \text{Figure 2.35} \quad \text{Load versus number of cycles to failure for single lap joints} \quad [104]. \]

From this model, it is possible to obtain reasonable predictions of the L-\( N_f \) curves. However, it easily overpredicts or underpredicts the fatigue life of a joint depending of the criteria used (\( G_T \) or \( G_I \)).

4. The final approach was proposed by Robinson et al. [97]. This method predicts the S-N curve via the application of interface elements. This is obtained by performing a modified cohesive zone model. The approach assumes that the area under the traction-separation curve, equivalent to \( G_C \), which characterises the cohesive elements, decreases with increase of the number of fatigue cycles until it reaches a minimum value, as illustrated in Figure 2.36.
This method allows the modelling of crack initiation as well as crack propagation. Although this technique has had successful application in the prediction of delamination growth in CFRP, as shown in Figure 2.37, only the linear part of the experimental curve of \( da/dN \) versus \( \Delta G \) (region II in Figure 2.21) is obtained [97].

With the linear section of the \( da/dN \) versus \( G_{\text{max}} \) obtained, the rest of the curve (region I and III) may be deduced. This can then be used to predict the S-N curve, as discussed in Section 2.4.
2.6.2. Proposed method

After analysing the existing approaches to this problem, see section 2.6.1, an approach was devised, which was thought to be best for the research to be undertaken, since the available approaches used for developing a fatigue life-prediction model today are complex, and it was necessary to reduce the intricacy of a method as much as possible.

The intention of this model was to predict the value of $S_{th}$ (maximum applied load during a fatigue cycle for which there is no crack propagation for an infinite number of cycles) from a linear logarithmic plot of the magnitude of a cyclical stress, $S$, plotted against the number of cycles to failure, $N_f$, see Figure 2.38.

![Figure 2.38 – S-N curve for a single lap joint.](image)

In order to achieve this objective, a series of modifications was made to existing methods. The method will not, for example, predict the whole S-N curve, but will give the part of interest, the threshold stress, $S_{th}$. This implies the application of fracture mechanics tests (Tapered Double Cantilever Beam – TDCB, Double Cantilever Beam – DCB, End Loaded Split – ELS and Fixed Ratio Mixed Mode – FRMM) to obtain the fracture energy values for mode I, $G_{IC}$, and mode II, $G_{IIIC}$, as well as the relationship between $G_{IC}$ and $G_{IIIC}$ when a joint is under mixed mode loading, as shown in Figure 2.39, using a uni-directional composite and performing these tests at room temperature and with ambient humidity conditions.
Fatigue tests are also performed, in order to obtain part of the graph of da/dN versus $G_{\text{max}}$, see Figure 2.40, from which only the threshold values of the mode I and mode II fracture energy values, $G_{\text{Ith}}$ and $G_{\text{IIth}}$, respectively (region I), are important, thus making it unnecessary to calculate the Paris law constants or to obtain the linear section in region II.

Figure 2.39 – Scheme of the relationship between the fracture energy values of modes I and II, knowing the values for $G_{\text{IC}}$, $G_{\text{IIIC}}$ and for a mixed mode ratio of 4:3.

Figure 2.40 – Fatigue crack growth data obtained from the DCB test where da/dN is plotted versus Gmax [57].
Two different joint designs were analysed in this project, namely a single lap joint, and a double scarf joint, as shown in Figure 2.41, since these are two of the most common joints used in structural and aerospace applications. In the case of the double scarf joint, the design was tested using different scarf angles, θ, namely 7, 30 and 45 degrees, in order to know if this modification provides a stronger joint. These angles are typically used for scarf joint specimens [105-107], as they provide a wide range for testing, without making the scarf too long. Quasi-static tests and fatigue tests allow the plotting of the S-N curve for both these joint designs.

![Diagram of joint designs](image)

**Figure 2.41** – Joint designs: (a) Single lap joint; (b) Double scarf joint.

Using the data obtained from the fracture mechanics tests, i.e. the values of $G_{IC}$, $G_{IIC}$, $G_{Ith}$, $G_{IIth}$, as well as the location of where the adhesive fails experimentally, an accurate finite element (FE) model is then developed for a single lap joint and a double scarf joint, using the FE modelling program Abaqus. The results are then compared with the S-N curves obtained experimentally for these joints, in order to validate the result.
2.7. Chapter summary

In this chapter, a review of the available types of adhesive on the market is given, as are the main joint designs, which can be optimised for strengthening of the adhesively-bonded joint. Fracture mechanics is reviewed, with brief information given on the main failure types of bonded joints, the fracture energy of adhesives, and the methods of obtaining these properties. The importance of the study on the fatigue lifetime of a bonded joint is also summarised.

An overview of the current finite element (FE) modelling methods are presented, as well as some current modelling approaches for the prediction of the fatigue lifetime of an adhesively-bonded joint. Current modelling approaches are complex and take some time to obtained the desired S-N curve. The proposed modelling approach aims to predict, in an easy and quick way, the threshold load for which there will be no crack propagation. It will use a calibrated set of cohesive zone modelling, CZM, adhesive properties specific to the material used, which can then be applied to any type of joint. This will reduce, not only the complexity of the modelling approach, but also the number of simulations required can be reduced to two. These simulations firstly obtain the maximum load, and secondly the threshold maximum load in fatigue for a given joint.
3. Materials, manufacture and testing methods

3.1. Introduction

In this chapter, the materials supplied to be experimentally tested and numerically simulated for the MAAXIMUS project – film adhesive, peel ply and carbon fibre reinforced plastic – are briefly described.

The procedure for preparing and curing the carbon fibre reinforced plastic plates, of different thicknesses, used as substrates for the various tests, is described. A description of the bonding procedure for the various types of bonded joints necessary to obtain the critical strain energy release rate of the supplied adhesive considering different fracture modes is given. The bonding of single lap joints and double scarf joints, which will be used to validate the numerical simulations, is also described.
Finally, the testing procedures used in this study are described in detail. These were, when possible, obtained either from ISO/British standards or from protocols, since these are verified methods for obtaining the data necessary for this project.

### 3.2. Materials

In this section, a brief description of the materials used throughout this study is given. This includes their mechanical properties and, in some cases, a more detailed look into the benefits of their use.

#### 3.2.1. Adhesive

The adhesive used for this project is FM–300M manufactured by Cytec Technologies Inc., New Jersey, USA. This material is an epoxy film adhesive that belongs to the family of the FM-300 adhesives, which are designed for bonding metal-to-metal and sandwich composite structures, having particularly good fatigue resistance [108]. Figure 3.1 shows a sheet of this adhesive.

![Figure 3.1—Photograph of a sheet of FM-300M film adhesive.](image-url)
This particular adhesive (Cytec FM–300M) has a nominal thickness of 0.13 mm – 0.1 mm after curing – and contains what the manufacturers describes as a moisture-resistant polyester random mat carrier for easier handling. The material properties at room temperature for this adhesive are shown in Table 3.1; these values were obtained from [109, 110], where only mean values are quoted, so the standard deviation is not known.

<table>
<thead>
<tr>
<th>Material</th>
<th>E [GPa]</th>
<th>G [GPa]</th>
<th>(\sigma_y) [MPa]</th>
<th>(\nu)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Cytec FM-300M</td>
<td>3.12</td>
<td>1.13</td>
<td>41</td>
<td>0.33</td>
</tr>
</tbody>
</table>

The recommended procedure for curing this adhesive is to heat it up, over a period of one hour, to 175°C, maintain it at that temperature for one hour and then let it cool down. The curing should be performed at a pressure between 0.10 and 0.69 MPa [108]. Differential scanning calorimetry, DSC, tests were performed for this adhesive. These were carried out on different adhesive samples which had had different curing cycles. The tests were performed using a two cycle temperature sweep at a rate of 10°C/min for both heating and cooling, in which the temperature ranged between 60°C and 220°C. The value obtained for the glass transition temperature, \(T_g\), was 168°C ± 1.3°C.

### 3.2.2. Carbon fibre reinforced plastic (CFRP)

The carbon fibre reinforced plastic, CFRP, used for this study was a unidirectional Prepreg T800S/M21 from Hexcel, Duxford, UK. Specifically, the CFRP used for all the tests was Hexply M21/34%/UD134/T800S, where M21 is the resin type; 34% represents the resin content by weight; 134 is the fibre weight (in g/m²); and T800S is the fibre type. This is a high performance composite, toughened epoxy resin system with a very tough epoxy matrix for use in aerospace structures (the manufacturer does not divulge the toughener used in this CFRP) [111]. A sheet of this material is shown in Figure 3.2.
The properties of this CFRP were supplied by [112, 113]. The main mechanical properties of interest for this project, which will be used for the numerical simulations, were supplied from a MAAXIMUS partner and are displayed in Table 3.2. These properties follow the directions shown in Figure 3.3.

**Figure 3.2** – Photograph of a sheet of T800S/M21.

**Figure 3.3** – Directions considered for the properties of the composite.

**Table 3.2** – Mechanical properties of T800S/M21 CFRP [112, 113].

<table>
<thead>
<tr>
<th>Material</th>
<th>$E_{11}$ [GPa]</th>
<th>$E_{22}$, $E_{33}$ [GPa]</th>
<th>$G_{12}$, $G_{13}$ [GPa]</th>
<th>$\nu_{12}$, $\nu_{13}$</th>
<th>$G_{1C}$ [KJ/m²]</th>
<th>$G_{II\text{C}}$ [KJ/m²]</th>
</tr>
</thead>
<tbody>
<tr>
<td>T800S/M21</td>
<td>170 ± 5.36</td>
<td>8.1 ± 0.2</td>
<td>4.8</td>
<td>4.01</td>
<td>0.33 ± 0.02</td>
<td>0.01</td>
</tr>
</tbody>
</table>
3.2.3. Peel ply

The peel ply provided was Hysol EA 9895 wet peel ply. This peel ply has a polyester carrier and is cured at 177°C for 90 to 120 minutes. Figure 3.4 shows the peel ply before curing, with a blue protective layer on top and a white layer on the bottom.

![Figure 3.4 – Photograph of sheet of Hysol EA 9895 peel ply.](image)

The difference between this type of peel ply and a conventional (dry) peel ply is the way that it is applied. As shown in Figure 3.4, the wet peel ply comes already impregnated with the resin and ready for curing, unlike the dry peel ply which needs resin to be added after positioning the peel ply. Current dry peel plies tend to leave contaminated fibres on the composite surface, which creates potential weak spots in the bondline, and to improve the decontamination of the surface, grit blasting could be used [114, 115].

This peel ply was used when preparing the composite panels, since it allows for a consistent bonding surface across the composite for all the experimental tests, as there is no need to perform any additional surface treatment after its removal. Figures 3.5 and 3.6 show the surfaces of the composite, using scanning electron microscopy, SEM: one with no surface treatment and no peel ply, and the other with no surface treatment but having removed the peel ply.
Figure 3.5 – SEM image of the composite surface with no surface treatment and no peel ply.
Figure 3.6 - SEM images of the composite surface with the peel ply removed and no surface treatment.
As can be seen from Figure 3.6, the surface from which the peel ply was removed has a much better bonding surface than that in Figure 3.5. There is no indication of any fibres having been left behind from the removal of the peel ply, giving a contamination free surface onto which the adhesive can be applied, leaving a thin layer of resin with the imprint of the peel ply fibres. This imprint allows for an easy way to identify whether the failure observed is interfacial or interlaminar. There are two ways: the first one is to verify the fracture surface using an optical microscope, since the direction of the composite fibres is unidirectional and the peel ply fibre imprints are interweaved; the second way, is by using an SEM and measure the fibre diameters of the fracture surface, since the CFRP and the peel ply fibres have different diameters, 5 μm and 25 μm, respectively. It is also possible to observe different patterns in the interstitial sites. The ones located in the middle and right side of the image, show possible agglomerations of toughener which could affect the results from the test – tests were performed on specimens that were given surface treatment instead of just removing the peel ply, and the same experimental results were obtained.

Taking into consideration previous research studies using peel ply which did not provide a clean surface area, especially when using peel ply with a nylon support carrier [115, 116], and also taking into account that all experimental tests in this project which use composite also use a wet peel ply, a validation on the effectiveness of this peel ply providing a good bonding surface, without the need for any further surface treatment, is undertaken in this study.

### 3.3. Manufacturing

In this section, the procedure for preparing and curing the carbon fibre reinforced plastic plates for the various tests is described. In addition, a detailed description of the bonding procedure for the various types of bonded joints – DCB, ELS, FRMM, SLJ and DSJ used in this study is given.
3.3.1. CFRP plates

In order to undertake the tests using T800S/M21, it was necessary to prepare panels of this composite. Depending on the test to be performed, different thicknesses were necessary – 2, 3 or 4 mm. For this material, the measured thickness of each ply after curing is 0.125 mm.

The panel was made so that all the plies follow the same direction, in order to make a uni-directional 600 mm wide CFRP panel. In the process of making the composite, each set of four plies was put under vacuum with the purpose of removing all the air bubbles trapped between the plies. These sets were then joined in pairs and put again under vacuum. This process was repeated until the desired number of plies had been reached. To finish preparing the plate, a layer of peel ply (Hysol EA 9895) was then added to the top of the panel. Note that in the case of the panel made for the CFRP used in the double scarf joints, both the top and bottom surfaces were covered with a layer of peel ply as this geometry involves bonding to both sides. The panel was cured following the curing cycle shown in Figure 3.7 with the temperature being held at 177°C.

![Figure 3.7 – Curing cycle for the T800S/M21 CFRP [111].](image)

A curing temperature of 177°C was chosen as this was the curing temperature of the peel ply, and it is more important that the peel ply creates a good surface for bonding than ensuring the CFRP reaches the specified 180°C cure temperature. After curing, a wet saw was used to cut the panel into smaller pieces with the specific dimensions for the various different tests.
3.3.2. Bonded joints

This project is centred on bonded joints, whether experimentally or numerically. In order to be able to perform all the different tests, it was necessary to make the bonded joints as described in this section.

3.3.2.1. Tapered double cantilever beam (TDCB)

The pure mode I tapered double cantilever beam, TDCB, specimen is shown schematically in Figure 3.8. The 10 mm thick 2014-T6 aluminium alloy substrates – mechanical properties shown in Table 3.3 – were machined using a CNC machine, since it is the best method to obtain a good and constant curvature in the beams. The substrates are shaped such that the change of compliance with crack length is linear, using the equation shown in Figure 3.8, with a geometry factor of \( m = 2 \, \text{mm}^{-1} \).

![Figure 3.8](image)

**Figure 3.8** – Geometry of the adhesively-bonded tapered double cantilever beam (TDCB) specimen [117] \((m = 2 \, \text{mm}^{-1})\).

**Table 3.3** – Young’s modulus, shear modulus and Poisson’s ratio of the 2014-T6 aluminium alloy [118].

<table>
<thead>
<tr>
<th>Material</th>
<th>E [GPa]</th>
<th>G [GPa]</th>
<th>( \nu )</th>
</tr>
</thead>
<tbody>
<tr>
<td>Aluminium Alloy</td>
<td>72.4</td>
<td>28</td>
<td>0.33</td>
</tr>
</tbody>
</table>
Before bonding, the aluminium alloy plates were degreased by wiping with a paper towel wetted with acetone followed by dry grit-blasting (using 180/220 micron mesh alumina grit at 5 bar). They were then degreased using trichloroethylene, Triklone N, (5 minutes in the liquid side of the solvent degreaser machine, followed by 5 minutes outside the machine to cool and a final 5 minutes in the vapour side). The plates were then etched in a chromic acid etching bath, which had been preheated to 68°C, for 30 minutes. This CAE bath was made-up of [119, 120]:

- 40 litres distilled water
- 7.20 litres Analar quality sulphuric acid (s.g.1.84)
- 3.87 kg sodium dichromate
- 60 g aluminium swarf
- 0.10 kg copper sulphate

Following this, the substrates were removed from the etch bath, given a tap water rinse and placed in a tank with running cold water for 15 minutes, after which the bonding area was rinsed using distilled water. They were subsequently placed in an oven to dry at 60°C for 10 minutes.

Strips of the Cytec FM-300M adhesive were cut with a knife using a spare piece of substrate as a template, making them slightly wider than the actual bonding surface. The cover of the film adhesive was then removed, and the adhesive placed on one of the substrates. A strip of polytetrafluoroethylene, PTFE, film, with a thickness of 12.7 μm, was positioned on the adhesive, covering the first 100mm of the bonding surface, obtaining an initial crack length, \(a_0\), of 90mm. The second substrate was then positioned over the adhesive and film.

The jig used to bond the TDCB can hold up to three specimens. Before placing any specimen in the jig, the jig was given a coating of release agent (Frekote 55-NC, Henkel), which would stop the excess adhesive from sticking to the jig.

The TDCB specimens were then enclosed in the jig using 3 mm thick silicone rubber pads in the contact points between the jig and the aluminium substrates, to ensure a good load distribution. A torque of 5 N.m was used to close the jig. Figure 3.9 shows the jig used for making the TDCB specimens.
The jig used to make TDCB specimens, showing the specimens positioned in the jig.

Figure 3.9 – Photograph of the jig used to make TDCB specimens, showing the specimens positioned in the jig.

The jig containing the specimens was placed in a fan oven and the recommended curing procedure described in Section 3.2.1 was followed. To monitor the temperature, a thermocouple was inserted into the bonding area of one of the TDCB specimens. After following different procedures, it was found that the optimised technique to ensure that the adhesive temperature reached 175°C within one hour, was to turn on the oven and set the temperature at 220°C until the thermocouple read 160°C, at which point the set temperature was reduced to 175°C. After holding at 175°C for 1 hour, the jig was left to cool in the oven overnight.

When making TDCBs with two layers of adhesive instead of one, the same bonding procedure was performed with the difference of adding another strip of adhesive after placing the PTFE film.

Finally, the excess adhesive that flowed from the bondline was removed using emery paper (80 grit). Afterwards, the bondline thickness was measured using an optical microscope, for later use in the modelling and also to know whether it was constant throughout the specimen. After this, the specimen was given a coating of typewriter correction fluid over the bondline.
of the sample – this helps with locating the crack position, and a paper scale was glued to this area just under the bondline.

### 3.3.2.2. Double cantilever beam (DCB), end loaded split (ELS), fixed ratio mixed mode (FRMM)

The procedure for bonding the double cantilever beam (DCB), end loaded split (ELS) and fixed ratio mixed mode (FRMM) specimens is exactly the same. The difference is the dimensions of the specimens.

Before bonding, the composite pieces were placed in an oven at 75°C for approximately 18 hours. This ensures that there is no moisture in the composite which could affect the curing of the adhesive. The composite substrates were removed from the oven and allowed to cool before bonding. The film adhesive was cut to the correct dimensions using a spare piece of substrate as template.

The peel ply was then removed from the composite and a strip of FM-300M adhesive was placed on one of the substrates. A strip of PTFE film was positioned on the adhesive such that it covered the first 50 or 80 mm of the bonding surface depending on the type of specimen to be bonded. The second substrate was then positioned on the adhesive and film, with the result that can be seen schematically in Figure 3.10.

![Figure 3.10](image)

**Figure 3.10** – Geometry of adhesively bonded composite for testing: a) DCB specimens; b) ELS and FRMM specimens.
The jig used to bond the DCB, ELS and FRMM specimens is shown in Figure 3.11. Before placing any specimen in the jig, it was given a coating of release agent, Frekote 55-NC, to stop excess adhesive from sticking to the jig.

![Image](image_url)

**Figure 3.11** – Photograph of the jig used to make DCB/ELS/FRMM specimens.

The specimens were placed in sets of three in the jig using 3 mm thick silicone rubber (RS code 506-3315, which can withstand temperatures between -40 and 200°C) between the jig and the CFRP substrates, to ensure a good load distribution. The jig was placed in a fan oven, and a mass of 80 kg was placed on top of it to give the required curing pressure (of approximately 15 psi), while the curing took place.

The curing procedure described in Section 3.2.1 recommends that the specimens should be heated up to 175°C in 30-60 minutes, after which time the temperature is maintained at 175°C for 60 minutes. However, since the curing temperature of the substrates is 180°C, it was not permissible to overshoot the temperature to 220°C as had been done for the TDCB specimens – see Section 1.3.2.1. This meant that the curing cycle was increased to 6 h including the 60 minute period at 175°C. A thermocouple was inserted in the bonding area of one of the
specimens to monitor the adhesive temperature. Once cured, the specimens and the jig were left to cool in the oven overnight.

To add the aluminium endblocks that can be seen in Figure 3.12, the specimens were cleaned to remove excess adhesive left after the curing cycle using an engineer’s file. The specimens and the endblocks were then cleaned using a paper towel soaked in acetone to wipe off any dirt. The endblocks were then dry grit-blasted (using 180/220 micron mesh alumina grit at 5 bar) on the surface to be bonded, and the specimens were wet grit-blasted (using 180/220 micron mesh alumina grit at 5 bar) on the area to be bonded. Once a good bonding surface was achieved for both pieces, they were cleaned again using acetone, and a mix of Araldite 2014-1 adhesive was prepared and spread on both surfaces. The specimens were then positioned in the jig shown in Figure 3.13, and compressed.

Figure 3.12 – Geometry of the adhesively bonded:
  a) DCB; b) ELS; c) FRMM specimens.
This jig ensures alignment and an even bondline between the aluminium endblocks and the CFRP. The jig was then placed in a fan oven, and the adhesive was cured at 40°C for 1h 45m. Afterwards, the FM-300M bondline thickness was verified using an optical microscope, for later use in the modelling and also to know whether it is constant throughout the specimen.

Finally, each specimen was given a coating of typewriter correction fluid over the side of the sample. For the DCB specimens, a paper scale was glued to this area just under the bondline, and for the ELS and FRMM specimens, a scale with intervals of 2.5 mm was drawn on the coated specimen using a black pen with a 0.01 mm diameter tip.

### 3.3.2.3. Single lap joint (SLJ)

To prepare the SLJ specimens, the composite pieces were dried for 12 h at 75°C, after which the peel ply was removed and a strip of 12.5 x 25 mm FM-300M adhesive was placed on one of the substrates. The second substrate was then positioned on the adhesive, with the result that can be seen schematically in Figure 3.14, which follows the design from the ISO standard [121].

![Figure 3.13 – Photograph of the jig for attaching endblocks.](image)

![Figure 3.14 – Geometry of the adhesively bonded single lap joint (SLJ) specimen.](image)
The jig used to bond the SLJ is shown in Figure 3.15. Before placing any specimens in the jig, it was given a coating of release agent, Frekote 55-NC. The SLJ was enclosed in the jig using 3 mm thick silicone rubber between the jig and the CFRP substrates, to ensure a good load distribution.

![Figure 3.15 – Photograph of the jig used to make SLJ specimens.](image)

Specimens were made in sets of six, and while the curing took place a mass of 70 kg (approximately 56.9 psi) was placed on the jig for compression. To monitor the temperature, a thermocouple was inserted into the bonding area in one of the specimens, and the specimens were cured at 175°C for 6 h, then left to cool in the oven overnight.

The endtabs that can be seen in Figure 3.16 were added after curing and were also made of the T800S/M21 composite. The newly bonded specimens were cleaned using acetone and the bonding area was wet grit-blasted using 180/220 micron mesh alumina grit at 5 bar, and then cleaned with a cloth soaked in acetone.

![Figure 3.16 – Geometry of the adhesively bonded single lap joint (SLJ) specimen.](image)
The endtab was not given any surface treatment since it was only necessary to remove the peel ply. A mix of Araldite 2014-1 was prepared and placed on both bonding surfaces and compressed, either using the jig shown in Figure 3.15 or using G-clamps, after which the specimens were placed in an oven for 1 h 45 min at 40°C to cure the adhesive.

Finally, the sides of the specimens were cleaned to remove the excess of both adhesives, but the fillet was left untouched in its natural state on all specimens. After that, the bondline thickness was measured using an optical microscope, for later use in the modelling.

3.3.2.4. Double scarf joint (DSJ)

The bonding of the DSJ follows exactly the same procedure as described for the SLJ in Section 3.3.2.3. The only difference is that there were more CFRP pieces to bond to each other, as shown schematically in Figure 3.17. Although the only bond of interest is the central joint with the scarf angle, all pieces were bonded using FM-300M since in this way the DSJ would be manufactured all in one go. This method also ensured a constant adhesive thickness throughout the specimen.

![Figure 3.17](image)

**Figure 3.17** – Geometry of the adhesively bonded double scarf joint (DSJ) specimen.

The scarf angle was cut using a milling machine with a electroplated diamond surface planer and a table which allows different cutting angles. Figure 3.18 shows the three angles that were tested: 7, 30 and 45 degrees. For the 7° pieces, it was necessary to leave an offset equivalent to the thickness of a ply of composite (0.125 mm), so that no damage occurs either on the cutting table or to the substrate due to the removal of the peel ply.
The jig surface was thermo during hydraulic grips of the universal testing machine used. To stop the specimens sliding out of the

**Figure 3.18** – Scarf angles used for the DSJ tests (dimensions in mm).

The jig used to bond the DSJ is shown in Figure 3.19. To ensure good bonding between each surface, silicone rubber was placed on the bonding areas to guarantee good loading in that area. The jig was given a coating of Freekote 55-NC prior to using it. The DSJ specimens were made in sets of five, and the bolts of the jig were tightened to a torque of 11 Nm. The jig was then inserted in an oven for 6 h at 175°C, and to monitor the temperature, a thermocouple was inserted in the bonding area of one of the specimens. The specimens and the jig were left in the oven overnight to cool.

**Figure 3.19** – Photograph of the jig used to make DSJ specimens.

During initial testing, problems were encountered with the specimens slipping out of the hydraulic grips of the universal testing machine used. To stop the specimens sliding out of the
grips, 2 mm thick aluminium alloy endtabs of length 40 mm and width 20 mm were bonded to the specimens. Before bonding, these were given the same dry grit-blast and degrease surface treatment as the endblocks mentioned in Section 3.3.2.2. The bonding areas of the DSJ specimens were wet grit blasted. A mix of Araldite 2014-1 was prepared and applied to both surfaces, after which the specimens were put in the jig shown in Figure 3.19 to get perfect alignment with the DSJ specimen. Once more silicone rubber was placed on the bonding areas to guarantee even loading in that area, following which the jig was closed and put under pressure with a torque of 11 Nm. The adhesive was cured as described for the endtabs in Section 3.3.2.3.

Finally, the sides of the specimens were cleaned using a file and emery paper (120 grit), to remove the excess adhesive from both the FM-300M and 2014-1 adhesives; however, the fillet was left untouched in its natural state on all the specimens. Afterwards, the bondline thickness of the FM-300M adhesive was measured using a microscope, for later use in the modelling.

### 3.4. Testing methods

In this section, the methods used for the testing of the adhesively bonded joints are described in detail. These procedures were obtained either from ISO/BSI standards or from existing testing protocols, and were performed at room temperature of 21°C ± 2°C and ambient room humidity of approximately 55% RH.

#### 3.4.1. Tapered double cantilever beam (TDCB) and double cantilever beam (DCB)

The tapered double cantilever beam, TDCB, specimens were tested using a universal testing machine, an Instron 5584, with the setup shown in Figure 3.20. The test was performed according to the ISO and BSI standards [44, 45], with a loading rate of 0.1 mm/min and an unloading rate of 0.5 mm/min.

The test was carried out in two phases. The first consisted in loading the specimen to allow the crack to propagate up to 2 mm, after which it was unloaded and the pre-crack was
examined to see whether it was equal on both sides of the specimen. Phase two is the test itself, in which the load, displacement and crack propagation are recorded.

![Setup for testing TDCB specimens.](image)

**Figure 3.20** – Setup for testing TDCB specimens.

The computer linked to the testing machine recorded the load and displacement during the test. The measurement of the crack length was performed manually by using a travelling microscope and a PIP marker linked to the testing machine. This device, when pressed, sends a signal to the computer, recording that point for a specific load and displacement in the test.

The mode I critical strain energy release rate, $G_{IC}$, was calculated following the available standards [44, 45]. There are three ways to calculate this value, either using the simple beam theory, SBT, the corrected beam theory, CBT, or the experimental compliance method, ECM. Before applying these methods to any of the types of tests (TDCB, DCB, ELS or FRMM), it is necessary to identify the onset of cracking from the data. These will be three points: the NL – point of deviation from linearity; the VIS – first point at which the crack is observed to move from the crack-tip; and the MAX/5% – the 5 % value as point on the load–displacement curve at which the compliance has increased by 5 % of its initial value $C_0$, as shown in Figure 3.21 [44].
Figure 3.21 – Schematic load–displacement curve for the DCB test showing initiation points NL, VIS, MAX/5 %, and propagation points (PROP) [44].

Of these three analyses, SBT is the more conservative and less accurate method, since it neglects the crack root rotation. The value of $G_{IC}$ is given by:

$$G_{IC} = \frac{4P^2}{ESB^2} \left( \frac{3a^2}{h^3} + \frac{1}{h} \right) = \frac{4P^2}{ESB^2} \cdot m$$  \hspace{1cm} (3.1)

where $a$ is the crack length, $h$ is the thickness of the substrate beam at $a$, $P$ is the measured load, $B$ is the width of the specimen, $E_s$ is the flexural modulus of the substrate, and $m$ is the specimen geometry constant which has a value of 2 mm$^{-1}$ [117].

The CBT and ECM are more accurate methods for the determination of $G_{IC}$, and usually have quite similar values. For the CBT method,

$$G_{IC} = \frac{4P^2m}{ESB^2} \left[ 1 + 0.43 \left( \frac{3}{ma} \right)^{\frac{1}{3}} \right]$$  \hspace{1cm} (3.2)

For the ECM method,

$$G_{IC} = \frac{p^2}{2B} \cdot \frac{dc}{da}$$  \hspace{1cm} (3.3)
where $C$ is the compliance of the specimen. The value of $dC/da$ is calculated from the slope of the plot of $C$ versus $a$, using only propagation values. These two methods tend to have similar results. They are considered to be more accurate than the SBT method since it does not consider the positions of the loading pins in relation to the surrounding material when deriving Equation 3.1, and a further correction applied in the CBT method, is the issue of the DCB specimens not behaving as perfectly built-in cantilever beams. The ECM method makes no assumptions as it simply uses the measured compliance [44, 45].

For testing the double cantilever beam, DCB, specimens, the procedure and standards are the same as for the TDCB specimens. The only difference is the loading rate, which was increased to 0.5 mm/min and an unloading rate of up to 5 mm/min. This is due to the DCB specimens being more compliant due to the thickness of the beams, an also because they are made out of composite and not metal. Figure 3.22 shows the setup used for the DCB testing.

![Figure 3.22 – Setup for testing DCB specimens.](image)

A measurement of the machine compliance also had to be performed. This uses the same setup as the TDCB/DCB specimens, and a rigid calibration specimen of known compliance, $C_C$, is used. The specimen is loaded at a rate of 0.05 mm/min up to a load of $P_{cal}$ (equivalent to the maximum load obtained during the fracture testing). Once this load is reached, the test
is stopped and the sample unloaded. From the results, a load versus displacement graph is plotted, and a line is drawn through the linear part as shown in Figure 3.23.

![Figure 3.23](image)

**Figure 3.23** – Schematic load-displacement trace obtained during the system compliance measurement [45].

The total compliance is then calculated using the following equation:

$$C_{total} = \frac{\delta_{cal}}{P_{cal}}$$  \hspace{1cm} (3.4)

And therefore system compliance, $C_{sy}$, is equal to

$$C_{sy} = C_{total} + C_{cs}$$  \hspace{1cm} (3.5)

This value is then used for correcting the displacement values measured during the fracture test; to obtain a more accurate value of $G_{IC}$. There are three methods described in the standard that are used to analyse the data: the SBT, the CBT, or the ECM methods.

From these three analyses, SBT is still a more conservative and less accurate method, and to calculate its value, Equation 3.6 is used.

$$G_{IC} = \frac{4P^2}{ESB^2} \left(\frac{3a^2}{h^3} + \frac{1}{h}\right)$$  \hspace{1cm} (3.6)

The CBT and ECM are more accurate methods for the determination of $G_{IC}$, and usually have similar values. Equations 3.7 to 3.9 are the equations used for the CBT method.
where $\Delta I$ is the crack length correction for a non-perfectly built-in beam, $F$ is the large displacement correction, and $N$ is the endblock correction. These last two variables can be calculated using the following equations:

\[
F = 1 - \frac{3}{10} \left( \frac{\delta}{a} \right)^2 - \frac{3}{2} \left( \frac{l_1 \delta}{a^2} \right)
\]

\[
N = 1 - \left( \frac{l_2}{a} \right)^3 - \frac{9}{8} \left[ 1 - \left( \frac{l_2}{a} \right)^2 \right] \frac{l_1 \delta}{a^2} - \frac{9}{35} \left( \frac{\delta}{a} \right)^2
\]

where $\delta$ is the displacement of the cross-head of the machine, $l_1$ is the distance between the centre of the loading pin and the mid-plane of the arm, $l_2$ is the distance between the centre of the loading pin and the edge of the endblock.

The value of $\Delta I$ can be experimentally obtained by plotting a graph of the cube-root of the normalized compliance $(C/N)^{1/3}$ versus the crack length. By making a linear fit to the data, the value of $\Delta I$ is equal to the distance between the origin and the X-axis intercept point [45].

For calculating $G_{IC}$ using the ECM method (when using endblocks)

\[
G_{IC} = \frac{nP \delta}{2Ba} \cdot \frac{F}{N}
\]

$n$ is the slope obtained from plotting log $C$ versus log $a$.

The flexural modulus can be calculated and compared with the independently measured flexural modulus, and is given by

\[
E_f = \frac{8(a + |\Delta I|)^3}{C N Bh^3}
\]

where $C$ is the compliance, and $E_f$ is the flexural modulus of the substrate calculated via the DCB mode I crack propagation test, which is used as useful check on the procedure, since its value is independent of the crack length.
3.4.2. End loaded split (ELS), fixed ratio mixed mode (FRMM)

The end loaded split, ELS, specimens were tested using a universal testing machine, an Instron 5584. Figure 3.24 shows the setup used for this test. As can be seen, the clamp stops any vertical movement of the clamped side of the specimen, while the opposite side is maintained on the horizontal plane with the use of a clamp with a linear bearing, and is pulled in the vertical plane to promote a mode II type of fracture.

![Image](image.png)

**Figure 3.24** – Setup for testing ELS specimens.

This test was performed according to an ESIS TC4 protocol [46]. Following this protocol meant that it was necessary to pre-crack the specimens with a crack growth of between 2 and 5 mm, before they were tested in mode II. This could be done either in mode I (using the setup and procedure described in Section 3.4.1) or mode II with a loading rate of 1 mm/min and a free length calculated from the equation,

\[ L = a_0 \frac{4}{3} \]  

(3.12)
in which \( L \) is the free length and \( a_0 \) is the initial crack length. From these two methods, the mode I method was preferred, as it allowed the crack location to be positioned more accurately.

After pre-cracking the specimens, mode II tests were performed using a loading rate of 0.5 mm/min, an unloading rate of 5 mm/min and with the clamp bolts tightened to a torque of 8 Nm. A new free length was calculated for every specimen using the following equation:

\[
\frac{a_p}{L} > 0.55 \quad \text{Hence} \quad L < \frac{a_p}{0.55}
\]  

(3.13)

in which \( a_p \) is the new crack length after pre-cracking the specimen.

The computer linked to the testing machine recorded the load and displacement during the test. The measurement of the crack length was performed manually by using a travelling microscope with a magnification of x15 and a PIP marker linked to the testing machine. This device, when pressed, sends a signal to the computer, recording that point for a specific load and displacement in the test.

For both ELS and FRMM testing, it is also necessary to carry out some calibrations due to the use of the clamp. This was done using a specimen with no crack, and loading it at a rate of 1 mm/min until a force of 250 N was reached, to ensure that the specimen remained elastic after which it was unloaded. This procedure was repeated for different free lengths (distance between the centre of the loading pin and the clamp).

This information was analysed by following the ESIS TC4 protocol [46] for the calculation of the value of the mode II critical strain energy release rate, \( G_{IIc} \). There are four possible ways for calculating this value, using SBT, CBT, ECM or the corrected beam theory with effective crack length, CBTE.

From these four analyses, SBT is still a more conservative and less accurate method, and to calculate its value, Equation 3.14 is used.

\[
G_{IIc} = \frac{9p^2a^2}{4B^2h^3E_1}
\]  

(3.14)
where \( P \) is the applied load, \( a \) is the measured crack length, \( E_I \) is the elastic modulus determined from three-point bending (flexural) test or from the clamp calibration test, \( h \) is the thickness of the each specimen arm, and \( B \) the specimen width. The value of \( E_I \) is obtained from the clamp calibration test, being deduced from the slope of the linear plot of \( C^{1/3} \) versus \( L \) (as used to obtain \( \Delta_{\text{Clamp}} \)) and applying Equation 3.15. However, this does not take into account large displacement or load-block effects [46].

\[
E_I = \frac{1}{2B(h \times \text{slope})^3} \quad (3.15)
\]

The value of \( E_I \) obtained via the clamp calibration test, was equal to 168 ± 17 GPa, which is similar to what was obtained in the DCB calculations as well as the three-point bending test, i.e. 165 ± 20 GPa and 155 ± 5 GPa, respectively.

The CBT, CBTE and ECM are more accurate methods for the determination of \( G_{IIc} \), and usually have similar values. Equations 3.16 to 3.19 are the equations used for the CBT method.

\[
G_{IIc} = \frac{9P^2(a+\Delta_{II})^2}{4B^2h^3E_I} \times F \quad (3.16)
\]

where \( \Delta_{II} \) is the mode II length correction, which is given by \( \Delta_{II} = 0.42 \times \Delta_I \), where \( \Delta_I \) is the value of the mode I correction measured in the DCB test (see Section 4.3.1), and \( F \) is the large displacement correction factor, which is given by:

\[
F = \left[1 - \theta_1 \left(\frac{\delta}{L}\right)^2 - \theta_2 \left(\frac{\delta l_1}{L^2}\right)\right] \quad (3.17)
\]

in which \( l_1 \) is the distance between the centre of the loading pin and the mid-plane of the arm, \( \delta \) is the displacement of the cross-head of the testing machine, and \( L \) is the free length of the arm [46]. The \( \theta_1 \) and \( \theta_2 \) factors are given by:

\[
\theta_1 = \frac{3}{20} \left[\frac{15+50\left(\frac{a}{L}\right)^2 + 63\left(\frac{a}{L}\right)^4}{1+3\left(\frac{a}{L}\right)^3}\right]^2 \quad (3.18)
\]

\[
\theta_2 = \frac{-3\left(\frac{L}{a}\right)\left[1+3\left(\frac{a}{L}\right)^2\right]}{1+3\left(\frac{a}{L}\right)^3} \quad (3.19)
\]
The CBTE method uses an effective crack length approach, and therefore the equations are:

\[ G_{IIc} = \frac{9p^2a_e^2}{4B^2h^3E_1} \]  

(3.20)

where \(a_e\) is the effective (calculated) crack length, and is given by:

\[ a_e = \left[ \frac{1}{3} \left( 2BCh^3E_1 - (L + \Delta_{Clamp})^3 \right) \right]^{1/3} \]  

(3.21)

where \(L\) is the free length, \(\Delta_{Clamp}\) is the clamp correction, and \(C\) is the compliance of the specimen, given by [46]:

\[ C = \frac{\delta}{P} = \frac{3(a_e)^3 + (L + \Delta_{Clamp})^3}{2Bh^3E_1} \]  

(3.22)

To determine the value of \(\Delta_{Clamp}\), the clamp correction, it is necessary to perform linear regression of the data from the clamp calibration tests, and calculate the compliance, \(C\), for each value of free length, \(L\). These values are used to plot a \(C^{1/3}\) versus \(L\), graph, to which linear regression is applied. This is then extrapolated to the X-axis (values of free length), and the value of the intercept is \(\Delta_{Clamp}\), as shown with two examples in [46, 122, 123]. Its value is specific to this sample, and dependent on the rotation and deflection of the specimen at the clamp. For the clamp and specimens used in this test, the \(\Delta_{Clamp}\) obtained was 16 mm.

Figure 3.25 – Clamp calibration data from the ELS test (\(\Delta_{Clamp}\) of 16 mm).
For the ECM method,

\[ G_{HIC} = \frac{3P^2 a^2 z}{2B} \]  

(3.23)

in which \( z \) is the slope of a plot of \( C \) versus \( a^3 \), and \( a \) is the measured crack length \([46, 122, 123]\).

The procedure for testing the fixed ratio mixed mode, FRMM, specimens follows the same steps as described above for the ELS testing. As is shown in Figure 3.26, the difference between the ELS and FRMM tests is the positioning of the endblock to be loaded in relation to the machine crosshead. The endblock is positioned in the top surface of the specimen.

![Figure 3.26 – Setup for testing FRMM specimens.](image)

From the protocol \([51]\), for mixed mode testing, the only difference is the calculation of the free length for the mixed mode test:

\[ \frac{a_p}{L} > 0.41 \quad \text{Hence} \quad L < \frac{a_p}{0.41} \]  

(3.24)

The data from the test was then analysed by following protocol for the calculation of the value of the mixed mode critical strain energy release rate, \( G_{UIC} \). Again, there are four
possible ways for calculating this value, using SBT, CBT, CBTE, or ECM. For the SBT method, the following equations apply:

\[ G_{IC}^{\text{mixed}} = \frac{3P^2a^2}{B^2Eh^3}F \]  \hspace{1cm} (3.25)

where \( G_{IC}^{\text{mixed}} \) is the mode I component of \( G_C \), \( P \) is the applied load, \( E \) is the modulus parallel to the fibre direction, \( a \) is the measured crack length, \( B \) is the width of the specimen, \( F \) is the large displacement correction factor, and \( h \) is the thickness of the substrate beam. The mode II component of \( G_C \), \( G_{II}^{\text{mixed}} \), is given by:

\[ G_{II}^{\text{mixed}} = \frac{9P^2a^2}{4B^2Eh^3}F \]  \hspace{1cm} (3.26)

where

\[ F = \left[ 1 - \theta_1 \left( \frac{\delta}{L} \right)^2 - \theta_2 \left( \frac{\delta l_1}{L^2} \right) \right] \]  \hspace{1cm} (3.27)

where \( \delta \) is the crosshead displacement, \( l_1 \) is the distance between the centre of the loading pin and the mid-plane of the arm, and \( L \) is the free length. The \( \theta_1 \) and \( \theta_2 \) factors are given by:

\[ \theta_1 = \frac{3}{20} \left[ \frac{15 + 150 \left( \frac{a}{L} \right)^2 + 367 \left( \frac{a}{L} \right)^4}{1 + 7 \left( \frac{a}{L} \right)^3} \right]^2 \]  \hspace{1cm} (3.28)

\[ \theta_2 = -3 \left( \frac{L}{a} \right) \left[ \frac{1 + 7 \left( \frac{a}{L} \right)^2}{1 + 7 \left( \frac{a}{L} \right)^3} \right] \]  \hspace{1cm} (3.29)

The mixed mode fracture energy, \( G_{I/II} \), for the mixed mode test is then obtained by applying the following equation:

\[ G_{I/II} = G_{IC}^{\text{mixed}} + G_{II}^{\text{mixed}} \]  \hspace{1cm} (3.30)

The calculations to obtain the value of \( G_{I/II} \), using the CBT analysis, are performed using the following equations:

\[ G_{IC}^{\text{mixed}} = \frac{3P^2(a+\Delta l)^2}{B^2Eh^3}F \]  \hspace{1cm} (3.31)
where $\Delta_I$ is the correction for the mode I component – determined from mode I tests (see Section 3.4.1)

$$G_{IIC}^{mixed} = \frac{9P^2(a+\Delta_{II})^2}{4B^2Eh^3} F$$

(3.32)

in which, $\Delta_{II}$ is the correction for the mode II component ($\Delta_{II} = 0.42 \Delta_I$) [51, 71]. And finally,

$$G_{I/III} = G_{IC}^{mixed} + G_{IIC}^{mixed}$$

(3.33)

The calculations of the value of $G_{I/III}$ using CBTE analysis, are performed using the following equations:

$$G_{IC}^{mixed} = \frac{3P^2a_e^2}{B^2Eh^3} F$$

(3.34)

$$G_{IIC}^{mixed} = \frac{9P^2a_e^2}{4B^2Eh^3} F$$

(3.35)

$$a_e = \left[\frac{1}{7} \left\{2BCh^3E - (L + \Delta_{Clamp})^3 \right\}\right]^{1/3}$$

(3.36)

$$G_{I/III} = G_{IC}^{mixed} + G_{IIC}^{mixed}$$

(3.37)

where $a_e$ is the effective (calculated) crack length, $h$ is the thickness of the each specimen arm, $C$ is the compliance of the specimen, $L$ is the free length, and $\Delta_{Clamp}$ is the clamp correction [46, 48, 51]. The clamp correction is calculated following the same procedure as described in Section 4.4, and the result obtained from the calibration tests was a $\Delta_{Clamp}$ value of 15 mm.

The ECM analysis uses the following equations for the calculation of $G_{I/III}$:

$$G_{I/III} = \frac{3P^2a_e^2z}{2b}$$

(3.38)
\[
G_{Ic}^{mixed} = \frac{4G_{IIc}}{7} \quad (3.39)
\]
\[
G_{IIc}^{mixed} = \frac{3G_{IIc}}{7} \quad (3.40)
\]
in which \( z \) is the slope of a plot of \( C \) versus \( a^3 \), and \( a \) is the measured crack length.

### 3.4.3. Single lap joint (SLJ), double scarf joint (DSJ)

The single lap joint, SLJ, specimens were tested using a universal testing machine, an Instron 5584, using the setup shown in Figure 3.27. The test was performed according to the ISO standard [121], with a loading rate of 0.5 mm/min. To provide extra grip between the SLJ specimens and the grips, emery paper was used; however, some SLJ specimens were tested after aluminium alloy endtabs were bonded to them. This was to verify whether the results obtained from this modification were different from those obtained using only emery paper.

![Figure 3.27 – Setup for testing SLJ specimens.](image)

The computer linked to the testing machine recorded the load and displacement during the test. Some of the specimens were also tested using an extensometer, an Instron 2630-113 with
a gauge length of 50 mm, to obtain the real bondline displacement. The extensometer knife-edges were positioned with one on each arm of the SLJ or DSJ specimen and the extensometer was attached to the specimen using spring clips. From this, a correction factor was obtained which was then used to correct the axial displacement given by the machine.

For the testing of the double scarf joint, DSJ, specimens, the procedure used was exactly the same. However, a different universal testing machine was used, an Instron 5585H, as it had hydraulic grips which were set to a pressure of 5 bar. The setup for this test is shown in Figure 3.28. The aluminium endtabs ensured that the specimens did not slip in the grips.

![Figure 3.28 – Setup for testing DSJ specimens.](image)

These samples were also given a visual post failure inspection of the fracture surface, and when deemed necessary, scanning electron microscopy, SEM, was performed using variable pressure, to have a confirmation of the failure locus of the adhesive.

### 3.5. Chapter summary

In this chapter, a brief description of the materials used in this project including their mechanical properties has been given. Also, a detailed procedure for the manufacture of the
CFRP plates and the various types of bonded joints that were tested in this project has been described.

Finally, the testing procedures obtained from ISO/British standards and protocols that were used for the experimental testing in this study have been outlined.
4. Quasi-static experimental results

4.1. Introduction

In order for the FE simulations to be as accurate as possible, experimental tests were performed with the aim of obtaining as much relevant property data for the adhesive as possible.

These data allow the experimental curves to be directly compared to the numerical curves and also make it possible to obtain the value of the fracture energy, $G_C$, of the adhesive under study, Cytec FM-300M – an epoxy film adhesive of nominal thickness 0.13 mm.

To achieve this, mode I and mode II fracture tests were performed to obtain $G_{IC}$ and $G_{IIC}$, respectively. Mixed mode, single lap joints and double scarf joint tests were also carried out, in order to have more reference points for the comparison with the numerical side of the project, as well as to gather more data about the adhesive used.
The mode I fracture mechanics tests were first performed using TDCB specimens. This was due to the lack of composite material at the beginning of the project, as it was still being chosen by the project partner, Airbus.

4.2. Tapered double cantilever beam (TDCB)

4.2.1. One layer of adhesive

The results obtained from testing TDCB specimens with one layer of adhesive are discussed in this section. The thickness of the bondline after curing was verified before testing, as described in Section 3.4.1, and was 0.1 mm for all of the specimens.

After performing the test, following the procedure in Section 3.4.1, the specimens were broken open. All of the specimens showed cohesive failure in the adhesive, as shown in Figure 4.1, where it is possible to see the green layer left by the adhesive evenly spread on both beams. TDCB specimens were tested with the initial crack (PTFE film) on the side of the lower arm as well as the upper arm, and this made no difference to the experimental results.

![Figure 4.1](image_url) – Photograph of a TDCB specimen after testing with one layer of adhesive.

Load versus displacement curves were plotted for these tests, as shown in Figure 4.2, used later for comparing FE modelling results with the experimental results. In the unloading section of the curves, it was verified that the substrates had not suffered plastic deformation, since the load was equal to zero once the displacement was returned to its initial position.
Quasi-static experimental results

The values of $G_{IC}$ were obtained by applying the three analysis methods mentioned in Section 3.4.1: SBT, CBT and ECM. These methods consider initiation values results are presented in Table 4.1. The values taken into account for use in the modelling were those from the CBT method. Therefore, the mean propagation value of $G_{IC}$, taking into account all of the propagation values, for a bondline thickness of 0.1 mm is 721 J/m², in contrast with the approximate value of 920 J/m² shown by [124] for a FM-300 adhesive.

Table 4.1 – Values of $G_{IC}$ for the different analysis methods considering a TDCB specimen with one layer of film adhesive.

<table>
<thead>
<tr>
<th>Test</th>
<th>$G_{IC}$ (SBT) [J/m²]</th>
<th>$G_{IC}$ (CBT) [J/m²]</th>
<th>$G_{IC}$ (ECM) [J/m²]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Test 1</td>
<td>689</td>
<td>752</td>
<td>698</td>
</tr>
<tr>
<td>Test 2</td>
<td>663</td>
<td>724</td>
<td>688</td>
</tr>
<tr>
<td>Test 3</td>
<td>630</td>
<td>688</td>
<td>622</td>
</tr>
<tr>
<td>Test 4</td>
<td>706</td>
<td>770</td>
<td>723</td>
</tr>
<tr>
<td>Test 5</td>
<td>635</td>
<td>697</td>
<td>643</td>
</tr>
<tr>
<td>Mean Value [J/m²]</td>
<td>665</td>
<td>726</td>
<td>674</td>
</tr>
<tr>
<td>Standard Deviation</td>
<td>33</td>
<td>35</td>
<td>41</td>
</tr>
</tbody>
</table>

Also from this analysis, graphs of $G_C$ versus the crack length were plotted, as shown in Figure 4.3. This graph shows only 2 initiation values, since the crack does not always have a smooth propagation at crack initiation, having instead a burst of crack propagation, which

Figure 4.2 – Load versus displacement graph for TDCB tests with one layer of FM-300M.
meant that for the analysis a third data point (VIS) was not available between NL (deviation from linearity) and a 5% reduction of the slope.

Figure 4.3 – R-curve from a tested one layer of adhesive TDCB specimen (Test 2).

Analysing this graph, it is seen that the value of $G_{IC}$ is dependent on the crack length. Its value increases until the crack length reaches 140 mm, after which the value of $G_{IC}$ tends to reach a steady-state value which is independent of the crack length.

The initiation values of $G_{IC}$ obtained for the TDCB tests were always lower than the propagation values. This is due to the growth of a damage zone ahead of the crack tip, which increases the joint toughness as the volume of yielded material expands. The rising portion of the R-curve ends when the damage zone reaches a steady-state size [125]. Also, the $G_{IC}$ values obtained from all three methods provided similar results.

This value of $G_{IC}$ (726 J/m$^2$) is not very high compared to other film adhesives, such as Cytec FM-73M which has a $G_{IC}$ of 2695 J/m$^2$ [126]. However, these values cannot be directly compared, since they are not for the same bondline thickness, even if they only have one layer of adhesive, since research has shown that the value of the fracture energy varies for slight changes of the bondline thicknesses when these are very thin, as shown in Figures 2.9 and 2.10.
### 4.2.2. Two layers of adhesive

In this section, the influence of the thickness of the adhesive is studied. The TDCB specimens were prepared using two layers of film adhesive, and a bondline thickness of 0.2 mm was measured. Failure was cohesive in the adhesive, see Figure 4.4.

![Figure 4.4 - Photograph of a TDCB specimen after testing with two layers of adhesive.](image)

From the analysis, the values of $G_{IC}$ obtained for the tests, by the three different methods: SBT, CBT and ECM, are presented in Table 4.2. Once again, the value which is considered to be of greater accuracy, and used for subsequent study, is the one obtained from the CBT method. Therefore, the mean propagation value obtained for $G_{IC}$ for a two-layer bondline with 0.2 mm thickness, is $937 \text{ J/m}^2$.

<table>
<thead>
<tr>
<th>Test</th>
<th>$G_{IC}$ (SBT) [J/m$^2$]</th>
<th>$G_{IC}$ (CBT) [J/m$^2$]</th>
<th>$G_{IC}$ (ECM) [J/m$^2$]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Test 1</td>
<td>845</td>
<td>924</td>
<td>850</td>
</tr>
<tr>
<td>Test 2</td>
<td>736</td>
<td>805</td>
<td>517</td>
</tr>
<tr>
<td>Test 3</td>
<td>996</td>
<td>1081</td>
<td>984</td>
</tr>
<tr>
<td>Mean Value</td>
<td>859</td>
<td>937</td>
<td>784</td>
</tr>
<tr>
<td>Standard Deviation</td>
<td>130</td>
<td>138</td>
<td>241</td>
</tr>
</tbody>
</table>

Table 4.2 – Values of $G_{IC}$ for the different analysis methods considering a TDCB specimen with two layers of film adhesive.

Here it is possible to observe once more that the value obtained from the three methods is similar. Figure 4.5 shows an example of one of the R-curves, plotting $G_C$ versus the crack length. It is possible to see from this graph that the value of $G_C$ reaches a steady-state value, which is independent of the crack length, above a crack length of 120 mm.
Figure 4.5 – R-curve from a tested two layer of adhesive TDCB specimen (Test 3).

### 4.2.3. Discussion

Comparing the values from Table 4.1 with Table 4.2, it is possible to see the influence of the bondline thickness on the value of $G_{IC}$, since $G_{IC}$ increased by 30% when the bondline was increased from 0.1 to 0.2 mm. This increase suggests that the value of $G_{IC}$ is dependent on the thickness of the bondline, as is well known and referred to in Section 2.3.5. However, these studies are usually for thicker bondlines and not for such unusually thin bondlines.

The plastic zone size was calculated using the equations in section 2.3.4 and the adhesive properties shown in Table 3.1. From this calculation, values for both one layer and two layers of adhesive were obtained, using $G_{IC} = 726$ J/m² and $G_{IC} = 937$ J/m², respectively. For one layer of adhesive, $r_p$ is equal to 0.08 mm, and therefore the diameter of the plastic zone, of 0.16 mm, is greater than the bondline thickness of 0.1 mm. On the other hand, with two layers of adhesive, the value of $r_p$ was equal to 0.11 mm, corresponding to a diameter of 0.22 mm. This plastic zone size was still greater than the bondline thickness – 0.2 mm – but was nearly fully enclosed in the bondline thickness. This meant that the adhesive’s plastic zone has different shapes depending on whether one or two layers are used. Figure 4.6 shows what the two plastic zones may look like, while the crack is propagating through the adhesive.
Figure 4.6 – Illustrations of the plastic zone at crack tip considering:
a) one layer of adhesive \( (r_p=0.08 \text{ mm}) \); b) two layers of adhesive \( (r_p=0.11 \text{ mm}) \).

Since the substrate is aluminium alloy, which will remain elastic due to its high yield stress, the plastic zone spreads along the adhesive when bonded using only one layer of adhesive, as illustrated in Figure 4.6a, and can be compared to the effect of using two layers of adhesive, as exemplified in Figure 4.6b. This effect may reduce the value of \( G_{IC} \), since its value decreases with the constraining of the plastic zone [36, 39]. However, as this is a film adhesive, it is not possible to vary the bondline thickness as for a paste adhesive. Thus it is not possible to ensure that the plastic zone is not squashed.

Images were also taken using a scanning electron microscope, SEM, to verify whether the failure of the adhesive had been even for both sides of the aluminium substrate. If so, it would mean that the crack had propagated through the centre of the bondline, which in turn implies that the correct value for the \( G_{IC} \) of the adhesive had been obtained. Figures 4.7 and 4.8 show images obtained for the TDCB specimens for both one and two layers of adhesive.
Figure 4.7 – SEM images of the fracture surface of the aluminium TDCB substrates for one layer of adhesive.
Figure 4.8: SEM images of the fracture surface of the aluminium TDCB substrates for two layers of adhesive.
In both Figures 4.7 and 4.8, it is possible to see an even layer of adhesive, as well as a similar amount of fibres (from the random carrier mat) on both fracture surfaces. This similarity suggests that the adhesive failed cohesively and through the centre of the bondline of the TDCB specimen in both cases. On the specimen with two layers of adhesive, the fracture surface also looks similar on both sides of the TDCB. As remarked, in both cases the carrier matt fibres can be seen on both substrate beams, suggesting that it may have contributed for the crack to propagate cohesively through the adhesive. However, for this mode of loading its effect should not be high. For the TDCB with two layers, it is also possible to see that even though there was a good bonding between the two layers of adhesive, there are voids in the fractured surface due to air bubbles having been trapped in the adhesive layer when adding a second layer of film adhesive, which can reduce the toughness of the adhesive. However, air entrapment is a typical occurrence in film adhesives.

4.3. Double cantilever beam (DCB)

4.3.1. One layer of adhesive

The results obtained from testing DCB specimens with one layer of adhesive are discussed in this section. In all cases, the procedure described in Section 3.4.1 was followed, and the bondline thickness was verified and found to be 0.1 mm. After each individual test, the specimens were broken open to establish their failure locus. All specimens failed cohesively in the adhesive as shown in Figure 4.9.

Figure 4.9 – Photograph of a tested DCB specimen with one layer of adhesive.
In these samples, the edges have interlaminar failure which was due to the cleaning process. All specimens are made individually, which leads to excess adhesive over the bondline on all sides of the sample. The removal of excess adhesive, can damage some composite fibres, causing the crack to propagate into the composite, and since the DCB specimen has such a thin bondline (0.1 mm), a small amount of excess adhesive may still be present after cleaning, which may pull the fibres at the sides of the specimen. This issue will not have a significant effect on the results, and the only way to prevent this from happening would be to bond the DCB specimens in a plate, rather than individually and after cut the DCBs into the desired dimensions. However, this creates a significant problem, in which air bubbles get easily trapped in the bondline, which will reduce the value of \( G_{IC} \).

Using the data obtained from the testing machine, graphs of experimental load versus displacement were plotted for these tests, as shown in Figure 4.10. From the unloading section of the curves, it was verified that the substrates had not suffered plastic deformation, since there was linear elastic unloading.

![Figure 4.10](image_url) – Load versus displacement graph from DCB tests with one layer of FM-300M.

The results obtained for \( E_f \) via equation 4.9, gave a value of 165 ± 20 GPa. This value was then checked experimentally by measuring the value of \( E_f \) by performing three point bending tests, using the ISO/BSI standard [127]. A setup similar to that illustrated in Figure 4.11 was
used, and the composite was tested at a rate of 0.5 mm/min, whilst recording the flexural displacement and load.

![Diagram](image)

**Figure 4.11** – Illustration of the setup used for the three point bending test for the calculation of the flexural modulus [127].

With the data obtained from the test, a flexural load/displacement curve is plotted, from which the values necessary to use in Equation 4.10 are extracted, for the calculation of the flexural modulus, \( E'_f \).

\[
E'_f = \frac{0.21L_f^3}{Bh^3} \left( \frac{\Delta F}{\Delta s} \right)
\]  

(4.10)

in which \( L_f \) is the span of the specimen, \( B \) the width of the specimen, \( h \) the thickness of the specimen, \( \Delta s \) is the difference in deflection between \( s'' \) and \( s' \), which are the beam mid-point deflections in millimetres, and \( \Delta F \) is the difference in load \( F'' \) and load \( F' \) at \( s'' \) and \( s' \), respectively.

After following this procedure, the value of the flexural modulus was calculated at 155 ± 5 GPa, a similar result to that obtained via Equation 4.8, confirming that the test procedure is accurate.

From these three methods, the CBT method is again considered to be of greater accuracy. The values of \( G_{IC} \) obtained in the tests considering the three analysis methods: SBT, CBT and ECM, are presented in Table 4.3. As a result, the mean propagation value of \( G_{IC} \) for a bondline thickness of 0.1 mm is 910 J/m².
Quasi-static experimental results

Table 4.3 – Values of $G_{IC}$ for the different analysis methods considering a DCB specimen with one layer of film adhesive.

<table>
<thead>
<tr>
<th>Sample</th>
<th>$G_{IC}$ (SBT) [J/m²]</th>
<th>$G_{IC}$ (CBT) [J/m²]</th>
<th>$G_{IC}$ (ECM) [J/m²]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Sample A</td>
<td>784</td>
<td>964</td>
<td>954</td>
</tr>
<tr>
<td>Sample B</td>
<td>733</td>
<td>901</td>
<td>887</td>
</tr>
<tr>
<td>Sample C</td>
<td>693</td>
<td>864</td>
<td>863</td>
</tr>
<tr>
<td>Sample D</td>
<td>791</td>
<td>973</td>
<td>979</td>
</tr>
<tr>
<td>Sample E</td>
<td>672</td>
<td>830</td>
<td>816</td>
</tr>
<tr>
<td>Mean Value</td>
<td>735</td>
<td>906</td>
<td>903</td>
</tr>
<tr>
<td>Standard Deviation</td>
<td>53</td>
<td>62</td>
<td>54</td>
</tr>
</tbody>
</table>

The graphs of $G_{C}$ versus the crack length obtained by this analysis are shown in Figure 4.12. As is seen, the value of $G_{IC}$ remains relatively constant as the crack length progresses throughout the test, and also, as would be expected, the $G_{IC}$ values obtained from the CBT or ECM are very similar. Also, unlike what was observed in Figure 4.3 for the TDCB, a steady state for which the value of $G_{IC}$ is independent of the crack length can be observed throughout the DCBs R-curve. A more detailed discussion on this difference is given in Section 4.3.3.

Figure 4.12 – R-curve from a tested DCB specimen with one layer of adhesive (Sample A).
4.3.2. Two layers of adhesive

In this section, the influence of the thickness of the adhesive is studied. DCB specimens were prepared using two layers of film adhesive, which meant that the bondline thickness increased to 0.2 mm. Figure 4.13 shows the fracture surfaces of the DCB, and as can be seen, the failure was again mainly located in the adhesive, with the edges suffering an interlaminar failure of approximately 1 mm on each side of the 20 mm wide specimen, due to the cleaning process for the removal of excess adhesive.

![Image of a tested DCB specimen using two layers of adhesive](image)

**Figure 4.13** – Photograph of a tested DCB specimen using two layers of adhesive.

The data obtained from the experimental test was analysed by following the same procedure that is described in Section 4.3.1 for the one-layer DCB. This analysis provided values for $G_{IC}$, as well as graphs of $G_C$ versus the crack length, as shown in Figure 4.14.

![Graph showing $G_C$ versus crack length](graph)

**Figure 4.14** – R-curve from a tested DCB specimen with two layers of adhesive (Sample A).
The value of $G_{IC}$ is very stable as the crack length progresses throughout the test. The mean propagation value obtained for $G_{IC}$ for this bondline thickness was 939 J/m$^2$. The values of $G_{IC}$ obtained in the tests considering different analysis methods: SBT, CBT and ECM, are presented in Table 4.4.

Table 4.4 – Values of $G_{IC}$ for the different analysis methods considering a DCB specimen with two layers of film adhesive.

<table>
<thead>
<tr>
<th></th>
<th>$G_{IC}$ (SBT)</th>
<th>$G_{IC}$ (CBT)</th>
<th>$G_{IC}$ (ECM)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Test 1 [J/m$^2$]</td>
<td>799</td>
<td>974</td>
<td>970</td>
</tr>
<tr>
<td>Test 2 [J/m$^2$]</td>
<td>830</td>
<td>926</td>
<td>907</td>
</tr>
<tr>
<td>Test 3 [J/m$^2$]</td>
<td>798</td>
<td>917</td>
<td>937</td>
</tr>
<tr>
<td>Mean Value [J/m$^2$]</td>
<td>809</td>
<td>939</td>
<td>938</td>
</tr>
<tr>
<td>Standard Deviation</td>
<td>18</td>
<td>31</td>
<td>32</td>
</tr>
</tbody>
</table>

Comparing the values from Table 4.3 with Table 4.4, it is possible to see that just as with the TDCB specimens; the bondline thickness influences the value of $G_{IC}$ obtained in the DCB tests. The mean value of $G_{IC}$ increased when the bondline was increased from 0.1 to 0.2 mm, even though there was no statistical difference between them, suggesting that the value of $G_{IC}$ is dependent on the thickness of the bondline.

4.3.3. Discussion

By comparing the values obtained from the DCB tests with one and two layers of adhesive (see Table 4.3 and Table 4.4), it is seen that, as happened with the TDCB specimens (see Section 4.2.3), the bondline thickness influenced the value of $G_{IC}$. Its mean value increased when the bondline was increased from 0.1 to 0.2 mm, even though for the DCB specimens, there was no statistical difference between them.

SEM images were also taken, to verify whether the failure of the adhesive had been even for both sides of the composite specimens, as was previously verified for the TDCB specimens. Figures 4.15 and 4.16 show images obtained for the DCB specimens for one and two layers of adhesive, respectively.
Quasi-static experimental results

Figure 4.15: SEM photos of the fracture surface of the composite substrates for one layer of adhesive.
Figure 4.16 – SEM photos of the fracture surface of the composite substrates for two layers of adhesive.
Figures 4.15 and 4.16 both show an even layer of adhesive on both sides of the specimen for both the one and two layered samples, with a constant amount of fibres left from the carrier. This similarity suggests that the adhesive failed consistently through the centre of the adhesive layer for the DCB specimens.

Comparing the $G_{IC}$ results between the DCB and TDCB specimens, the specimens which had two layers of adhesive gave the same mean value of $G_{IC}$ (939 ± 31 J/m² and 937 ± 138 J/m², respectively), as would be expected since these two tests are pure mode I tests, and should give the same results since $G_{IC}$ is a material property. On the other hand, the values of $G_{IC}$ obtained considering only one layer of adhesive, were different for each case, 721 ± 32 J/m² for the TDCB and 910 ± 51 J/m² for the DCB, even though for all these cases, the failure locus was always the same, as verified in Figures 4.7, 4.8, 4.15 and 4.16.

At first this difference was thought to be due to the difference in the curing cycle, since an increase of 4 h curing time was applied to the curing cycle due to the use of composite substrates. To verify whether the curing cycle had any influence, TDCB specimens were bonded using additional curing cycles:

- Total curing time 5 h, using a torque of 8 N.m

- Total curing time of 6 h, using a mass of 80 kg (same curing setup as the DCB)

Some DCB specimens were also prepared in the Aeronautics Department at Imperial College London, using a hot press. This allowed bonding these specimens with the same curing cycle as had been done for the TDCB, i.e. reaching a temperature of 175°C within one hour, and maintaining that temperature for another hour, applying a constant pressure of 5 kg/cm². In this case, there was no overshooting of the temperature, since the hot press is capable of heating the specimens faster than in an oven. All of these curing cycles are summarized in Figure 4.17.
After testing all of the specimens, consistent results were obtained for both the DCB and TDCB specimens, i.e. around 721 J/m² for the TDCB specimens, and 910 J/m² for the DCB specimens. Therefore, differential scanning calorimetry, DSC, tests on adhesive samples removed from TDCB and DCB curing cycles (from DCB hot press; from DCB curing for 6 h 30 m; from TDCB curing for 5 h; from TDCB curing for 6 h; from TDCB recommended curing within 1 h) were performed using the method described in Section 3.2.1. They revealed that the glass transition temperature, \( T_g \), remained unchanged for all of the tested samples, at 168.0 ± 1.3°C. Table 4.5 gives a summary of all the tested specimens, showing their curing cycle, \( T_g \) and fracture energy.

**Table 4.5** – Fracture energy and \( T_g \) values obtained from different curing cycle procedures for TDCB and DCB tests.

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Cure Cycle</th>
<th>( G_{IC} ) [J/m²]</th>
<th>( T_g ) [°C]</th>
</tr>
</thead>
<tbody>
<tr>
<td>TDCB</td>
<td>Total of 2 h in oven using a torque of 8 N.m (standard TDCB curing cycle)</td>
<td>721 ± 32</td>
<td>167.7</td>
</tr>
<tr>
<td>TDCB</td>
<td>Total of 5 h in oven using a torque of 8 N.m</td>
<td>728 ± 67</td>
<td>167.8</td>
</tr>
<tr>
<td>TDCB</td>
<td>Total of 6 h in oven using a mass of 80kg</td>
<td>724 ± 37</td>
<td>166.8</td>
</tr>
<tr>
<td>DCB</td>
<td>Total of 6 h 30 m in oven using a mass of 80 kg (standard composite DCB curing cycle)</td>
<td>910 ± 51</td>
<td>168.1</td>
</tr>
<tr>
<td>DCB</td>
<td>Total of 2 h in hot press with pressure of 5 kg/cm²</td>
<td>902 ± 20</td>
<td>170.2</td>
</tr>
</tbody>
</table>
Another possible cause – also discussed in Section 4.2.3 – was the effect of the plastic zone size. This was larger than the bondline thickness in both DCB and TDCB specimens. However, the different substrate materials, isotropic and anisotropic, could be causing a different plastic zone response for the two substrates, since previous studies have suggested that the composite reduces the size of the plastic zone ahead of the crack [128-130].

To address this possibility, aluminium DCB specimens were made following the same surface treatment and standard curing cycle that had been used for the TDCB specimens (see Figure 4.17), and using the jig and pressure used for the DCB composite specimens. The data from the tests were analysed following the procedure described in Section 4.3.1, and the $G_{IC}$ value obtained for one and two layers of adhesive was $820 \pm 36 \text{ J/m}^2$ and $951 \pm 91 \text{ J/m}^2$, respectively. The values of the fracture energy obtained from the different tests are summarized in Table 4.6.

<table>
<thead>
<tr>
<th>Type of Joint</th>
<th>Fracture Energy, $G_{IC}$ [J/m$^2$]</th>
<th>1 Layer of adhesive</th>
<th>2 Layers of Adhesive</th>
</tr>
</thead>
<tbody>
<tr>
<td>TDCB Aluminium</td>
<td>$726 \pm 35$</td>
<td>$937 \pm 138$</td>
<td></td>
</tr>
<tr>
<td>DCB Aluminium</td>
<td>$820 \pm 36$</td>
<td>$951 \pm 91$</td>
<td></td>
</tr>
<tr>
<td>DCB CFRP</td>
<td>$906 \pm 62$</td>
<td>$939 \pm 31$</td>
<td></td>
</tr>
</tbody>
</table>

As can be seen, the $G_{IC}$ value for two layers of adhesive is statistically similar for any of the three types of tested joint, with an average value of $942 \text{ J/m}^2$. On the other hand, the same cannot be said for the results when using only one layer of adhesive, since none of the values are statistically similar. However, by examining the graph of $G_C$ versus the crack length of the aluminium DCB, shown in Figure 4.18, and comparing it to the graph obtained for the aluminium TDCB (see Figure 4.3) it is possible to see that for the aluminium DCB, the steady state section for which value of $G_{IC}$ is independent of the crack propagation is reached a lot sooner than in the TDCB, where it only reaches a steady-state after 130 mm crack propagation.
Quasi-static experimental results

**Figure 4.18** – R-curve from a tested aluminium DCB specimen with one layer of adhesive.

This means that the mean value for the TDCB was reduced to $721 \pm 32$ J/m$^2$, whilst, in fact, the steady-state value for the propagation of $G_{IC}$ was around $800$ J/m$^2$, as verified in the aluminium DCB tests, and shown in Figure 4.19. Here the value of $G_{IC}$ is plotted against the relative crack propagation, $\Delta a$ (equal to $a-a_0$), instead of the crack length, $a$, in order to be able to have a direct comparison between the tested specimens, since all of these specimens have different initial crack lengths. These new data support the idea that the different substrate materials, metal and composite, cause a different plastic zone response, thus giving different values for the mode I fracture energy when the bondline thickness is less than $2r_p$.

**Figure 4.19** – Comparison between the mode I fracture energy and the relative crack propagation for one layer of adhesive.
4.3.4. Peel ply

The peel ply used in this project was also a subject of study, since it was necessary to observe whether it actually provided a good bonding surface or not. When DCB specimens were tested using Cytec FM-300M, all specimens failed cohesively in the adhesive, demonstrating that a good bonding surface was obtained from the peel ply. However, the value of $G_{IC}$ of this adhesive is not very high, which raised the question as to whether this was also why the locus of failure was always cohesive in the adhesive. Subsequent DCB testing was performed by another researcher [131] using ready-made composite substrates, with peel ply supplied by this project but using another film adhesive, 3M 163-2, which had a value of $G_{IC}$ of 2828 ± 174 J/m² (for a bondline thickness of 0.4 mm; using two layers of adhesive). The experimental results showed once more that the peel ply provided a good bonding surface, resulting in cohesive failure within the adhesive, making it unnecessary to perform additional surface treatments to the composite surface.

4.4. End loaded split (ELS)

In this section, the results obtained from ELS testing are discussed. Also, from this section onwards, all specimens are bonded using only one layer of adhesive, since that would be the case for construction of the aircraft [132].

As before, all specimens used in this test had a bondline thickness of 0.1 mm, and after testing were broken to reveal the locus of failure. Figure 4.20 shows a photograph of a tested ELS specimen. As can be seen, it failed cohesively in the adhesive.

![Figure 4.20 – Photograph of a tested ELS specimen.](image-url)
The experimental load versus displacement curves for the clamp calibration tests are shown in Figure 4.21, where different free lengths, $L$, were tested, which will allow for the calculation of the flexural modulus of the sample and also the clamp calibration, $\Delta_{\text{Clamp}}$, specific to this sample, dependent on the rotation and deflection of the specimen at the clamp. The load versus displacement graphs for the mode II ELS tests are shown in Figure 4.22, which shows a linear elastic unloading behaviour; therefore, the specimen suffered no plastic deformation.

![Figure 4.21](load_displacement_graph.png)

**Figure 4.21** – Load versus displacement graph for the ELS clamp calibration tests. (where $L$ is the free length used in the test).
Figure 4.22 – Load versus displacement graph for the ELS tests.

From these four methods, the CBTE method is considered to be the one that delivers greatest accuracy. The values of $G_{IIc}$ obtained from the tests by the four analysis methods: SBT, CBT, CBTE and ECM, are presented in Table 4.7. Consequently, the mean propagation value of $G_{IIc}$, assuming a bondline thickness of 0.1 mm, is 3510 J/m$^2$.

Table 4.7 – Values of $G_{IIc}$ for the different analysis methods considering an ELS specimen with one layer of film adhesive.

<table>
<thead>
<tr>
<th></th>
<th>$G_{IIc}$ (SBT) [J/m$^2$]</th>
<th>$G_{IIc}$ (CBT) [J/m$^2$]</th>
<th>$G_{IIc}$ (CBTE) [J/m$^2$]</th>
<th>$G_{IIc}$ (ECM) [J/m$^2$]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Sample A</td>
<td>3259</td>
<td>3503</td>
<td>3507</td>
<td>3514</td>
</tr>
<tr>
<td>Sample B</td>
<td>3571</td>
<td>3843</td>
<td>4085</td>
<td>3687</td>
</tr>
<tr>
<td>Sample C</td>
<td>2901</td>
<td>3130</td>
<td>3023</td>
<td>3158</td>
</tr>
<tr>
<td>Sample D</td>
<td>3293</td>
<td>3552</td>
<td>3843</td>
<td>3550</td>
</tr>
<tr>
<td>Sample E</td>
<td>3450</td>
<td>3724</td>
<td>3091</td>
<td>3629</td>
</tr>
<tr>
<td>Mean Value</td>
<td>3295</td>
<td>3550</td>
<td>3510</td>
<td>3508</td>
</tr>
<tr>
<td>Standard Deviation</td>
<td>253</td>
<td>271</td>
<td>462</td>
<td>207</td>
</tr>
</tbody>
</table>

This value compares well with other epoxy adhesives, where the value of $G_{IIc}$ in relation to $G_{IC}$ is approximately three times its value [123]. This analysis also provided graphs of $G_C$ versus the crack length, shown in Figure 4.23. As can be seen, the value of $G_C$ reaches a steady state, independent of the crack length, and the initiation values are very similar to those obtained for propagation. Previous studies [48, 133] have not demonstrated such a good match between the initiation and the propagation, instead having a steep rising R-curve until
the value of $G_{IIc}$ reaches a plateau, which was considered to be due to the existence of the inclined micro-cracks that formed ahead of the crack tip in the adhesive layer, as shown in Figure 4.24. However, for this adhesive, another reason that could be provoking this behaviour, is the carrier matt, since it can provide a weaker path for the crack to propagate, as such reducing the R-curve effect.

![Graph](image)

**Figure 4.23** – Mode II R-curve from a tested ELS specimen (Sample A).

![Image](image)

**Figure 4.24** – Photograph of micro-cracking in the adhesive layer (the vertical black lines are drawn 1 mm apart) [48].

The ELS specimens tested in this project also had micro-cracks ahead of the crack. However, these were only visible for up to 2 mm, unlike other cases in which they extend to several millimetres [48], hence the small R-curve. This, however, does not mean that this adhesive is better than other epoxy adhesives, since the value mode II fracture energy for this adhesive,
considering a bondline thickness of 0.1 mm, is roughly 3.5 times the value of the mode I fracture energy, which is a typical experimental observation for structural adhesives [119, 134].

SEM images were taken of the ELS specimens, to verify the failure location of the adhesive, as was previously verified for the other specimens, and also the different modes of failure suffered by the adhesive. Similarly to what was verified in both mode I cases, the failure was localised evenly between both substrate beams. Figures 4.26 and 4.27 show images obtained for the ELS specimens showing the different failure modes verified during the test. These samples also had air trapped during bonding, which resulted in the voids that can be seen in the photos. As mentioned for the mode I SEM images, air entrapment is a typical occurrence when using film adhesives.
Crack Propagation

Starter film area

Mode I failure (Pre-crack area)

Figure 4.25 – SEM photos of the fracture surface of the composite substrates showing the starter film and precrack area.
Crack Propagation

Serrated surface, due to shear failure (mode II)

Figure 4.26 – SEM photos of the fracture surface of the composite substrates of the propagation area.
As described in Chapter 3, the ELS test is performed by initially precracking the samples in mode I before performing the actual test in mode II. In Figure 4.26 it is possible to see the different influences that both modes have on the adhesive. Comparing Figure 4.26 with Figure 4.27, it is possible to see a clear difference between them. Figure 4.26 has the same fracture surface that was observed on the TDCB/DCB specimens, with cohesive failure through the centre of the bondline, and the adhesive shows a tension type of failure. In Figure 4.27, it is possible to see a serrated type of surface, due to the shear failure to which mode II is characteristic [135-137].

### 4.5. Fixed ratio mixed mode (FRMM)

In this section, the results obtained from FRMM testing with one layer of adhesive are discussed. These tests are performed for a ratio of $G_I/G_{II}=4/3$, and in all cases, the specimens failed cohesively in the adhesive as is shown in Figure 4.27. The thickness of the bondline after curing for all of the samples was 0.1 mm.

![Tested Region](image)

**Figure 4.27** – Photograph of a tested FRMM specimen.

The data obtained were plotted in graphs of experimental load versus displacement for both the clamp calibration, which allows the calculation of the clamp calibration, $\Delta_{\text{Clamp}}$, and the FRMM test, where it is possible to see that a linear elastic unloading behaviour occurred. These graphs are shown in Figure 4.28 and 4.29, respectively.
From these four methods, the CBTE method is considered to be the one that delivers greatest accuracy [119]. The values of $G_{IIC}$ obtained from the tests considering the three analysis methods: SBT, CBT, CBTE and ECM, are presented in Table 4.8. As a result, the mean propagation value of $G_{IIC}$ for a bondline thickness of 0.1 mm is taken as 1356 J/m$^2$. 
Table 4.8 – Values of $G_{I/IC}$ for the different analysis methods considering a FRMM specimen with one layer of film adhesive.

<table>
<thead>
<tr>
<th>Sample</th>
<th>$G_{I/IC}$ (SBT) [J/m²]</th>
<th>$G_{I/IC}$ (CBT) [J/m²]</th>
<th>$G_{I/IC}$ (CBTE) [J/m²]</th>
<th>$G_{I/IC}$ (ECM) [J/m²]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Sample_BJ1</td>
<td>1318</td>
<td>1396</td>
<td>1372</td>
<td>1268</td>
</tr>
<tr>
<td>Sample_BJ2</td>
<td>1343</td>
<td>1420</td>
<td>1450</td>
<td>1327</td>
</tr>
<tr>
<td>Sample_BJ3</td>
<td>1080</td>
<td>1149</td>
<td>1245</td>
<td>1146</td>
</tr>
<tr>
<td>Sample_BJ4</td>
<td>1371</td>
<td>1410</td>
<td>1431</td>
<td>1398</td>
</tr>
<tr>
<td>Sample_BJ5</td>
<td>1103</td>
<td>1216</td>
<td>1286</td>
<td>1130</td>
</tr>
<tr>
<td>Mean Value</td>
<td>1243</td>
<td>1318</td>
<td>1357</td>
<td>1254</td>
</tr>
<tr>
<td>Standard Deviation</td>
<td>140</td>
<td>126</td>
<td>89</td>
<td>115</td>
</tr>
</tbody>
</table>

This information was further analysed by plotting graphs of $G_{I/IC}$ versus the crack length, Figure 4.30. As can be seen, the value of $G_{I/IC}$ is initially dependant of the crack length until it reaches a steady state, independent of the crack length, at about 92 mm. Statistically all methods show good agreement between themselves; however, as shown in Figure 4.30, the CBT can overestimate the value of the fracture energy, since it is very sensitive to the clamped free-length correction.

![Figure 4.30 – R-curve from a tested FRMM specimen.](image-url)
4.5.1. Discussion

This test was performed in order to be able to plot a graph of $G_{IC}$ versus $G_{IIIC}$. In this case, the ratio was equivalent to $G_{IC}/G_{IIIC}=4/3$. Therefore, taking into account the values of pure mode I, pure mode II and mixed mode from the quasi-static tests performed, it was now possible to plot this graph, since from the experiments it is known that $G_{IC}$ is 906 J/m$^2$, $G_{IIIC}$ is 3510 J/m$^2$ and $G_{II/IIIC}$ for this ratio is 1356 J/m$^2$. This graph is shown in Figure 4.31.

![Graph showing the relationship between $G_I$ and $G_{II}$](image)

*Figure 4.31 – Failure envelope for the Cytec FM-300M (1 layer of adhesive).*

As can be seen, this relationship is linear. This type of relationship is not always achieved, since it is possible to obtain concave or convex responses [52, 138-140]. In these cases, other criteria are used to follow the experimental data points, using the power law or the BK law, among others. This response changes from one adhesive to another, since the failure envelopes are dependent on the relative fracture energy values. Figure 4.31 also shows the possible positions where $G_{II/IIIC}$ could decrease had the relationship between $G_I$ and $G_{II}$ not been linear, considering the mixed mode ratio taken into account in the FRMM test.
4.6. Single lap joint (SLJ)

The results obtained from testing SLJ specimens are presented in this section. In all cases, the specimens failed cohesively in the adhesive, as shown in Figure 4.32. Before performing the test procedure described in Section 3.4.3, the bondline thickness after curing was verified to be 0.1 mm.

![Photograph of a tested SLJ specimen.](image)

**Figure 4.32** – Photograph of a tested SLJ specimen.

The information obtained from the testing machine allowed the creation of the graph shown in Figure 4.33 of the load versus the machine displacement. This shows that the mean fracture load for a SLJ is 12.2 kN, meaning it can sustain a stress of 39.0 MPa, before failure.

![Load versus displacement for the SLJ tests.](image)

**Figure 4.33** – Load versus displacement for the SLJ tests.
As is shown, the stiffness of the curves varies between each sample by a maximum of 20%. After careful inspection of the SLJ specimens, it was discovered that these had slight changes amongst themselves in the width and thickness, which suggests being the reason for the different stiffnesses, since, for example, sample B had the smallest cross-sectional area and sample C had the largest area (i.e. 25.4 x 3 mm²). These differences aren’t of very high magnitude, but they are sufficient to give considerable differences in the measured stiffness of a SLJ. Nevertheless, their far-field stresses are similar, as shown in Table 4.9.

<table>
<thead>
<tr>
<th>Sample</th>
<th>Area [mm²]</th>
<th>Load [N]</th>
<th>Far-field Stress [MPa]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Sample A</td>
<td>70.22</td>
<td>12278.71</td>
<td>174.87</td>
</tr>
<tr>
<td>Sample B</td>
<td>69.69</td>
<td>11417.65</td>
<td>169.12</td>
</tr>
<tr>
<td>Sample C</td>
<td>75.6</td>
<td>12954.16</td>
<td>171.35</td>
</tr>
<tr>
<td>Sample D</td>
<td>73.69</td>
<td>12743.27</td>
<td>172.93</td>
</tr>
</tbody>
</table>

However, this graph does not show the correct displacement that was measured in the bonded joint, due to slippage between the machine grips and the endtabs plus emery paper system, which resulted in a much larger displacement than that observed from the extensometer. Therefore, load-displacement data was linked to the data from the extensometer from each test, which allowed plotting corrected graphs of the experimental load versus extensometer displacement curves of these tests, see Figure 4.34.

![Figure 4.34 – Load versus extensometer displacement graph for SLJ tests.](image-url)
Using the data in Table 4.9 and using the real displacement for the SLJ tests, as used in Figure 4.34, a graph plotting the far-field stress versus extensometer displacement was created; see Figure 4.35, to confirm the similarities between the tested specimens.

![Figure 4.35 – Far-field stress versus extensometer displacement graph for the SLJ tests.](image)

To obtain the real displacement value of the SLJ, the data from the test using SLJ – recorded in mm/mm – were converted to a displacement dimension in mm. This displacement was the actual movement suffered by the bonded joint. Therefore, the maximum displacement value obtained in the machine was substituted by the value obtained with the extensometer.

The use of the extensometer and subsequent modification of the load-displacement curve could influence the modelling of the SLJ, since it suffers bending moments at the overlap ends. However, since the extensometer that was used had a gauge length of 50 mm, the grips were located far from where the composite has a greater bending moment, and owing to the composite’s high stiffness, it is unnecessary to calibrate the displacement for the FE model, since the bending moment applied in the joint will be reduced.
4.7. **Double scarf joint (DSJ)**

The DSJ specimens were manufactured using three different scarf angles: 7, 30 and 45°. The purpose was to investigate whether this made any difference to the maximum load that the specimens could support, since the scarf angle reduces the stress concentration in the joints, and also to understand whether the failure locus would be constant. The bondline thickness of all tested samples was 0.1 mm and all had a natural fillet. The specimens were tested following the procedure in Section 3.4.3.

After testing all specimens, the pieces were examined on both sides to identify the failure type. In all cases, the specimens failed cohesively in the adhesive, as seen in Figure 4.36.

![Figure 4.36 - Photographs of tested DSJ specimens: (a) 7 degrees; (b) 30 degrees; (c) 45 degrees.](image-url)
With the information obtained, i.e. the data from the testing machine together with the data from the extensometer, experimental load versus extensometer displacement graphs were plotted, as shown in Figure 4.37. This shows that the mean fracture load for a DSJ with a 7 degree scarf angle is 60.0 kN, meaning it can sustain a stress of 30.0 MPa, before failure.

![Figure 4.37](image)

**Figure 4.37** – Load versus displacement graph for DSJ 7° tests.

As previously stated, all specimens failed cohesively. However, to understand whether the scarf angle made a difference to the maximum load that this joint can withstand, a comparison between all of the results obtained for all angles of the DSJ was made. Figure 4.38 compares the entire experimental load versus displacement curves, where it is possible to see that similar loads correspond to similar displacements for specimens with different scarf angles.
Figure 4.38 – Load versus displacement graph for DSJ tests with angles of $7^\circ$, $30^\circ$ and $45^\circ$.

Before joint failure, at approximately 0.6 mm, it is possible to see that some of the curves show small sudden drops: this was deduced to be due to the fracture of the natural fillet that was present on the specimens, since the fillet varied from specimen to specimen, some with more overflow than others, as has been observed in previous works [120]. In some of the DSJ tests, it is possible to see large variation in the stiffness, especially for the DSJ with 30 degrees. In this case, it was not possible to calculate the far-field stress, since the DSJ have arms of different thicknesses. Therefore, it was considered that the reason that was given for the SLJ is also applicable to these samples, i.e. the cross-sectional area of the beams varied, influencing the stiffness of the overall specimens. Nevertheless, for the three angles tested, the failure mode observed was always cohesive failure in the adhesive. Table 4.10 shows the maximum loads obtained for the various tests.

<table>
<thead>
<tr>
<th>Sample</th>
<th>Maximum Load [kN]</th>
<th>7 Degrees</th>
<th>30 Degrees</th>
<th>45 Degrees</th>
</tr>
</thead>
<tbody>
<tr>
<td>Sample A</td>
<td>59.8</td>
<td>56.7</td>
<td>54.9</td>
<td></td>
</tr>
<tr>
<td>Sample B</td>
<td>61.5</td>
<td>58.4</td>
<td>57.8</td>
<td></td>
</tr>
<tr>
<td>Sample C</td>
<td>58.9</td>
<td>52.6</td>
<td>53.8</td>
<td></td>
</tr>
<tr>
<td>Sample D</td>
<td>-</td>
<td>57.7</td>
<td>-</td>
<td></td>
</tr>
<tr>
<td>Sample E</td>
<td>-</td>
<td>54.8</td>
<td>-</td>
<td></td>
</tr>
<tr>
<td>Mean</td>
<td>60.0</td>
<td>56.0</td>
<td>55.5</td>
<td></td>
</tr>
<tr>
<td>Standard Deviation</td>
<td>1.3</td>
<td>2.3</td>
<td>2.1</td>
<td></td>
</tr>
</tbody>
</table>
Quasi-static experimental results

Although five specimens of the 7 and 45 degree DSJ were made, two of the samples failed in the endtab section. However, since consistent results (standard deviations of 1.3 and 2.1, respectively) were obtained from the remaining three samples, they were not repeated. The work described in [105-107] showed that scarf angles between 90 and 45 degrees do not affect the value of the maximum load; however, between 6 and 45 degrees, the maximum load increases by up to 30%. In this project, even though all values tested show similar maximum load values and load-displacement curves, only the 30 and 45 degree values are statistically similar, with an increase of 10% of the maximum load for the 7 degree angle. This confirms the improvement of the maximum load for lower scarf angles.

4.8. Chapter summary

In this chapter, the experimental results obtained for the different quasi-static tests performed from which the values of the critical strain energy release rate were obtained for pure mode I and mode II for use in the numerical simulations were presented.

Initially, tapered double cantilever beam, TDCB, specimens were made and tested, due to the composite material not being available. All of the tested specimens failed cohesively in the adhesive, and from the analysis, the value of the mode I fracture energy, $G_{IC}$, was found to be 726 J/m$^2$.

In the manufacture of the bonded joints which used composite substrates, it was not possible to perform the recommended curing cycle, since the curing temperature of the composite was close to that of the film adhesive. Therefore, the curing time was increased from 2 h to 6 h 30 min. To investigate whether the extra time changed the properties of the adhesive, differential scanning calorimetry, DSC, tests were performed on cured adhesives from TDCB and DCB curing cycles. The value obtained for the glass transition temperature, $T_g$, from both samples was $168.0 \pm 1.3^\circ C$.

DCB quasi-static tests gave a value of $G_{IC}$ of 906 J/m$^2$. These specimens were broken open to reveal that all of them had failed cohesively in the adhesive.

Although all the modelling performed using one layer of film adhesive, which provided a bondline thickness of 0.1 mm, DCB and TDCB specimens were also manufactured using two
Quasi-static experimental results

layers of adhesive. This increased the bondline thickness to 0.2 mm. Although for the DCB specimens this did not significantly change the value of $G_{IC}$ (from $906 \pm 62 \text{ J/m}^2$ to $939 \pm 31 \text{ J/m}^2$). However, it did for the TDCB specimens, for which the value of $G_{IC}$ was the same as that obtained by the DCB test for a 0.2 mm bondline, but greater than the value from the 0.1 mm bondline (an increase from $726 \pm 35 \text{ J/m}^2$ to $937 \pm 138 \text{ J/m}^2$). The plastic zone size was then calculated for all cases, which revealed that for the specimens with one layer of adhesive, the plastic zone was very restricted, since the radius, $r_p$, was similar to the bondline thickness. This difference was deduced to be due to the use of different substrates, since the plastic zone size had a radius of 0.1 mm, thus bigger than the bondline thickness. To confirm this, aluminium alloy DCBs were bonded and tested with one and two layers of adhesive, confirming that the value of $G_{IC}$ was equal using either aluminium or composite substrates when using two layers of adhesive. However, it also confirmed that for a single layer of adhesive, the value of the fracture energy is influenced by the substrate when the plastic zone size is much larger than the bondline thickness, that is, when the plastic zone size increases, the fracture energy decreases. From this test, and by comparing the R-curves obtained from the TDCB and aluminium DCB tests, it was concluded that the steady state value of the mode I fracture energy for an aluminium alloy substrate was in the region of 800 J/m$^2$.

ELS tests revealed that the value of the mode II fracture energy, $G_{IIC}$, was 3510 J/m$^2$. These specimens were subsequently opened to shown that the adhesive had failed cohesively.

For the mixed mode test, FRMM specimens were tested with a mode mixity of $G_I/G_{II} = 4/3$. The specimens were broken open, which revealed that they had failed cohesively in the adhesive. The value of the mixed mode fracture energy, $G_{IIC}$, for this ratio, was found to be 1357 J/m$^2$. The failure envelope for this adhesive was created, and shown to have a linear relationship between $G_{IC}$ and $G_{IIC}$.

SLJ and DSJ tests were performed in which all specimens failed cohesively in the adhesive, with the SLJ achieving a mean maximum load of 12.2 kN. The DSJ were manufactured using three different scarf angles, 7º, 30º and 45º. All tested specimens showed similar maximum load values and load-displacement curves, but only DSJ with 30 and 45 degrees were statistically similar (56.0 ± 2.3 kN and 55.5 ± 2 kN, respectively), with the maximum load, for the 7 degrees DSJ, being 60.0 ± 1.3 kN.
5. FE modelling

5.1. Introduction

In this chapter, a detailed description of the numerical simulations performed for the various quasi-static tests described in Chapter 4 is given, as well as a comparison between the experimental and numerical results. The finite element simulations were performed by using the numerical program Abaqus. Two different numerical methods were used in this project: the virtual crack closure technique, VCCT, and cohesive zone modelling, CZM. The exceptions were for the SLJ and DSJ specimens where only the CZM method was used, as these simulations were performed without the use of an initial crack, hence the VCCT cannot be used. These models did not consider residual stress effects, since [141] showed that these had negligible effect on the bonded joint.

For all simulations, the boundary conditions were imposed to mimic as closely as possible the actual boundary conditions that are present in the experimental tests without adding load or stiffness to the final results. Also, all simulations were performed in 2D generalised plane strain, since the width of the specimen is well above the minimum width necessary for having effect from plane strain. This simplifies the models and also reduces computational times.
Figure 5.1 shows a flow chart where it is possible to see the link between Chapter 4 with the experimental tests and the current chapter, as well as the steps taken in this chapter, to be able to predict the failure of a bonded joint.

**Figure 5.1** – Flow chart showing the stages of the quasi-static modelling approach.
The material properties used in this chapter are detailed in Table 5.1, and partial input files for some of the simulations described in this chapter are shown in Appendix A. These material properties are considered to be reliable, since the FE models are not sensitive to property changes, over reasonable values.

**Table 5.1 – Material properties for the adhesive, the aluminium alloy substrates/endblocks, and the composite [109, 110, 112, 113, 118].**

<table>
<thead>
<tr>
<th>Material</th>
<th>Cytec FM-300M</th>
<th>Aluminium Alloy</th>
<th>Material</th>
<th>T800S/M21</th>
</tr>
</thead>
<tbody>
<tr>
<td>E [GPa]</td>
<td>3.12</td>
<td>72.4</td>
<td>E11[GPa]</td>
<td>170</td>
</tr>
<tr>
<td>G [GPa]</td>
<td>1.13</td>
<td>28</td>
<td>E22, E33[GPa]</td>
<td>8.1</td>
</tr>
<tr>
<td>σy [MPa]</td>
<td>41</td>
<td></td>
<td>G12, G13 [GPa]</td>
<td>4.8</td>
</tr>
<tr>
<td>ν</td>
<td>0.33</td>
<td>0.33</td>
<td>G23 [GPa]</td>
<td>4.01</td>
</tr>
<tr>
<td>GIc [J/m²]</td>
<td>800 (TDCB)</td>
<td>910 (DCB)</td>
<td>υ12, υ13</td>
<td>0.33</td>
</tr>
<tr>
<td>GIIC [J/m²]</td>
<td>3510</td>
<td></td>
<td>υ23</td>
<td>0.01</td>
</tr>
</tbody>
</table>

**5.2. Tapered double cantilever beam (TDCB) simulations**

Simulations were performed for a TDCB test by applying the virtual crack closure technique, VCCT, and cohesive zone modelling, CZM. The type of element used in all these simulations was generalised plane strain with reduced integration. For meshing purposes, each element had 4 nodes, element type CPEG4R, and a dimension of 1 mm by 1 mm. In the case of the adhesive, the elements have a dimension of 0.025 mm by 1 mm (height and length, respectively) for the VCCT simulation only – this was the largest recommended aspect ratio and also the coarsest mesh that could be used without affecting the numerical results. Therefore, for the TDCB model tested, the number of elements was 16740, for which 35 minutes was the necessary average computational time.

These simulations were performed considering all the restrictions that the TDCB is subjected to while the actual test is carried out. Figure 5.2 shows the location of these restrictions; in position B, a “pinned” boundary condition was inserted, blocking any movement on the X, Y
and Z axes, but allowing the rotation of the substrate from this point, as would happen in the experimental test. In position A, a boundary condition to prevent any movement in the X-axis as placed, as well as a displacement parameter in the Y-axis. No boundary condition was inserted on the opposite side of A and B. Gravity was not included in the simulations.

![Diagram](image)

**Figure 5.2** – Illustration of the location of the boundary conditions and displacements on the TDCB specimens.

The mechanical properties of the materials used in the simulations for the adhesive, the Cytec FM-300M, and the aluminium alloy adherends are shown in Table 5.1. These were considered to have isotropic behaviour.

### 5.2.1. Virtual crack closure technique

In this section, the results from using VCCT to run the numerical simulations of the TDCB test are shown. These were performed taking into account the boundary conditions, material and element properties given in Section 5.2. As the method used is the VCCT, this model was performed using two parts – top half and bottom half – and assembling them together afterwards. This meant that the adhesive layer was divided into two pieces, and that a set of nodes had to be selected which corresponded to the bonded area of the TDCB. Figure 5.3 shows the mesh used for the TDCB test and also highlights the location of the BNODES set, which is the node set to the slave surface (as mentioned in Section 2.5.1).
Before being able to run these simulations, it was necessary to make some modifications to the input file, since Abaqus does not give all the required instructions directly from the Abaqus/CAE window. As a result, the input file was altered, with the introduction of the following extra command lines:

```
**INITIAL CONDITIONS, TYPE=CONTACT
  Sl a, Mas, BNODES”
```

where Sl a and Mas are the slave and master surfaces, respectively, which are be bonded together, connected via the nodes in the BNODES set. As well as:

```
**CONTACT PRINT
  *DEBOND, SLAVE=Sl a, MASTER=Mas
  *FRACTURE CRITERION, TYPE= VCCT, MIXED MODE BEHAVIOR=POWER
  <GIC>, <GIIC>, <GIIIC>, 2.0, 2.0, 2.0”
```

where $GIC$, $GIIC$, $GIIIC$, are the values for the fracture energy for mode I, mode II and mode III, respectively. The value “2.0” is related to the mixed mode behaviour law selected, in this case the power law. However, since this is a pure mode test, its value is not relevant. There is a more detailed discussion of this point in Section 5.5.2, when the modelling of mixed mode is discussed. This second addition to the input file specifies the energy necessary for the crack to propagate, and therefore debond the node connecting the slave surface to the master surface. Since the value of $G_{IIIIC}$ is not known and not necessary for the tests simulated, it is considered to be equal to zero, as is suggested in the Abaqus manual.
With these modifications, the TDCB simulations were run using a value of $G_{IC}$ of 800 J/m$^2$, since that was the steady state value obtained experimentally. The result is shown in Figure 5.4 which illustrates the outcome of the specimen at the end of the simulation, and Figure 5.5 which shows a comparison between the experimental data – see Section 4.2.1 – and the numerical simulation.

![Image of a numerically tested TDCB specimen](image)

**Figure 5.4** – Image of a numerically tested TDCB specimen (units in MPa), in steady state using $G_{IC}$ equal to 800 J/m$^2$.

![Load versus displacement graph](graph)

**Figure 5.5** – Load versus displacement graph comparing experimental data with data obtained from TDCB simulations using 16740 elements.

From the three experimental curves, Test 1 is the test which had the closest experimental match to the mean $G_{IC}$ of 800 J/m$^2$ used for the simulation. However, comparing the experimental curve with the numerical curve, there is a clear difference between the initial
part of the curves, in which the numerical simulation is stiffer than the experimental load-displacement curve, even after giving an offset to the numerical curve to compensate for the initial experimental adjustment verified in the curves. The numerical curve also shows a higher maximum load than that obtained in the experimental tests, followed by a gradual decrease of the load during crack propagation. The propagation section shows good agreement between the curves towards the end, only showing a difference between the numerical and experimental curves at the beginning of the propagation phase. This initial difference is due to the higher value of the maximum load, which then gradually decreases until it reaches the same plateau that is observed with the experimental curves. This difference is quite noticeable, since the FE model assumes no R-curve behaviour.

The oscillations in the simulation curve that can be observed in Figure 5.5, are due to the VCCT considering that the bonded nodes are rigidly bonded until they are separated when the energy reaches $G_{IC}$, causing the oscillations as the crack propagates.

In order to find out if it was possible to obtain better agreement for the initial stiffness of the curve and also observe the influence of certain parameters on the this extra stiffness, simulations were performed with the following modifications:

5.2.1.1. **Element type**

Here, only the element type was changed. The following types were considered in the different simulations: plane strain with reduced integration (CPE4R), generalised plane strain with reduced integration (CPEG4R), and generalised plane strain with reduced integration and hybrid formulation (CPEG4RH). The generalised plane strain adds a degree of freedom – Z plane – to the simulation when compared to the plane strain. The hybrid formulation is recommended for both incompressible and almost incompressible cases. Hybrid elements include an additional degree of freedom that determines the pressure stress in the element directly. The results are shown in Figure 5.6.
Figure 5.6 – Load versus displacement graph comparing experimental data with data obtained from simulations considering different element types.

As can be seen, the element type did not influence the extra stiffness observed in the initiation section of the curve. All three curves have the same initial stiffness, making them indistinguishable in the graph. On the propagation side, the curve resulting from the plane strain element is slightly different from the other two curves. Studies of models with element types of plane stress, plane strain and generalised plane strain, have shown that from these three simulations, the results from the generalised plane strain models fit between those obtained from the plane stress and plane strain models, since the plane strain assumes an infinite deformation in the Z direction, and the generalised plane strain assumes a finite deformation [142]. Nonetheless, good agreement is obtained in Figure 5.6, better result than that obtained using the plane strain model.

5.2.1.2. Number of nodes

In this part, only the number of nodes was changed. Instead of having 4 nodes per element (CPEG4R), 8 nodes per element (CPEG8R) were used, as shown in Figure 5.7.
This has the effect of increasing the number of nodes selected for bonding, for the same size mesh. It means that the load-displacement curve should become smoother and more precise, since the debonding of the nodes takes into account the middle of the element whereas before, only the outer nodes were considered. The results are shown in Figure 5.8, demonstrating that there is no alteration to the stiffness of the curve.

The modification from a 4 noded element to an 8 noded element did not alter the stiffness of the curve. Both curves are exactly one on top of the other; therefore, an increase of the number of nodes per element was unnecessary, as a result not increasing the computational time for this model.
5.2.1.3. Convergence input file lines

To prevent any contact problems occurring during the simulation, a few extra lines were inserted (shown in bold) into the input file:

```
*Step, name=Step-1, nlgeom=YES, inc=5000, convert sdi=no
*Static
0.001, 1. 1e-05, 0.015
*CONTROLS, PARAMETERS=TIME INCREMENTATION
    ,,,50```

where sdi stands for severe discontinuity iterations. By inserting these lines, a new iteration is forced to start, if severe discontinuities occur during an iteration, ensuring that the simulation would be able to continue. However, it was necessary to know if this affected the results obtained in the simulations. By removing these lines from the input file, it was possible to see that it did not affect the end result, as seen in Figure 5.9.

![Figure 5.9](image) – Load versus displacement graph comparing experimental data with data obtained from simulations considering different convergency specifications.
5.2.1.4. **Mesh refinement**

In this modification, the mesh was refined in order to increase the number of elements in the TDCB. All elements, including the adhesive elements, were reduced to a quarter of their original size, increasing the number of elements from 16740 to 66960. This modification follows the idea that with smaller elements the results obtained are more accurate, since the only parts that are taken into account in the VCCT modelling are the bonded nodes and not the area between nodes; however, this also increases the computational time – to 180 minutes. The results are shown in Figure 5.10

![Graph showing load versus displacement](image)

**Figure 5.10** – Load versus displacement graph comparing experimental data with data obtained from simulations considering course and fine meshing.

As can be seen, this modification made no significant change to the load-displacement curve, which has no visible difference, demonstrating that the original number of elements was sufficient for a good result, so that there was no need to increase the mesh size and therefore the computational time for this model.

5.2.1.5. **Discussion**

The increased stiffness observed in these models can also be seen in literature supplied by Abaqus, other research projects [143], and also in research papers [144-146]. However, this
extra stiffness is never explained; and after testing the numerical simulations using different parameters, which always led to the same result, it was deduced that the extra initial stiffness was due to the method used – VCCT – since it considers that the bonded nodes are rigidly bonded before they are separated, meaning that they have an infinite stiffness up to the point when they debond, consequently increasing the stiffness of the model.

5.2.2. Cohesive zone modelling

Cohesive zone modelling, CZM, was applied. For this model, two different paths were followed: the use of cohesive elements and the use of cohesive contact. For the model using cohesive contact, the mesh, the element type and the model design were the same as had been used for the VCCT model. The only difference was the set of command lines added to the input file:

```
"*COHESIVE BEHAVIOR, ELIGIBILITY=SPECIFIED CONTACTS, TYPE=UNCOUPL ED
<KI>, <KII>, <KIII>
*DAMAGE INITIATION, CRITERION=QUADU
<δn0>, <δt0>, <δIII>
*DAMAGE EVOLUTION, TYPE=ENERGY, MIXED MODE BEHAVIOR=POWER LAW,
MODE MIX RATIO=ENERGY, POWER=2, SOFTENING=EXPONENTIAL
<GI>, <GII>, <GIII>"
```

where $K_I$, $K_{II}$, $K_{III}$, $\delta_{n0}$, $\delta_{t0}$, and $\delta_{III}$ are fitting parameters from the traction-separation curve for the cohesive modelling for mode I, mode II, and mode III, respectively. The lines:

```
"*INITIAL CONDITIONS, TYPE=CONTACT
Sla, Mas, BNODES"
```

were also added.

For the cohesive elements model, the mesh density and the element type used for the aluminium beams were identical to the original VCCT model, described in Section 5.2. The main difference was to the adhesive, which was substituted by one layer of cohesive elements with thickness equal to that of the bondine (element type COH2D4).
For the layer of cohesive elements, as well as the nodes of the cohesive contact, it was necessary to insert the cohesive traction-separation parameters for which a good prediction would be obtained. In order to do this, an initial set of parameters of the traction-separation curve was determined using the mechanical properties of the adhesive and then adjusting these parameters so that the curve obtained from the simulation was in good agreement with the experimental curve.

The parameters for defining the cohesive law for pure mode I and mode II are: the stiffness, $K_I, K_{II}$; the separations at damage initiation, $\delta_{n0}, \delta_{t0}$; and the critical energy release rates, $G_{IC}, \bar{G}_{IC}$ (area under the traction-separation curve), as shown for mode I in Figure 5.11.

Using equations 5.1 to 5.2 obtained from [147, 148] and the mechanical property values of the adhesive shown in Table 5.1, it is possible to obtain initial values of the cohesive contact parameters:

\[
K_I = \frac{E}{t_a} = 3.12 \times 10^{13} \ [Pa/m] \quad (5.1)
\]

\[
\delta_{n0} = \frac{\sigma_y}{E/t_a} = 1.60 \times 10^{-6} \ [m] \quad (5.2)
\]
\[ \sigma_{nc} = K_I \times \delta_{n0} = 50.0 \times 10^6 \text{ [Pa]} \]  \hspace{1cm} (5.3)

with an adhesive thickness, \( t_a \), of 0.1 mm. The cohesive element properties are obtained by simply not including the thickness, \( t_a \), from equations 5.1 and 5.2. These values were then used to create traction-separation curves with a linear or an exponential damage evolution, since these are the only shapes allowed by Abaqus when using an energy type of damage evolution, i.e. the area under the curve is equal to the value of the fracture energy.

By using these initial values, with the value for the mode I fracture energy, \( G_{IC} \), of 800 J/m\(^2\), and considering an energy type of damage evolution with both a linear (as shown in Figure 5.11) or an exponential softening (as shown in Figure 2.21), the initial stiffness as well as the maximum load were overestimated. Therefore, an iterative process was performed to find which values best suited these parameters, using these initial values as a starting point.

The results of the cohesive element and cohesive contact models were always indistinguishable, as shown in Figure 5.12. Therefore, all results shown from here onwards were obtained using the cohesive contact model, since these models are easier to apply and it can use the same model that was previously created for applying the VCCT.

![Figure 5.12](image_url) – Graph comparing experimental data with data obtained from TDCB simulations, using cohesive elements and cohesive contact.
At first the value of $K_I$ was modified, which allowed to match the stiffness of the simulation with the experimental curve, and afterwards the value of $\delta_{n0}$, to match the maximum load. The curves obtained from the iterative process are shown in Figure 5.13.

**Figure 5.13** – Graph comparing experimental data with iterations 1-3 data from cohesive contact TDCB simulations.

As can be seen, the last iteration, iteration 3, shows good agreement between the curves obtained in the simulation and the experimental curve. The parameters for this iteration are:

\[
K_I = 5.12 \times 10^{11} \text{ [Pa/m]} \quad \delta_{n0} = 1.60 \times 10^{-6} \text{ [m]} \quad \sigma_{nc} = 8.19 \times 10^5 \text{ [Pa]}
\]

However, since it is essential that these cohesive parameters work for different types of tests, in particular those which have mixed mode, instead of just a pure mode, it was necessary to further modify these mode I parameters in order to obtain a widespread good result, as is further explained in Section 5.5.3. These parameters also needed to be such that the area under the first triangle of the traction-separation curve was equal to the value of the threshold fracture energy. Therefore, new parameters were used:

\[
K_I = 5.12 \times 10^{10} \text{ [Pa/m]} \quad \delta_{n0} = 6.2 \times 10^{-5} \text{ [m]} \quad \sigma_{nc} = 3.42 \times 10^6 \text{ [Pa]}
\]

and the result is shown in Figure 5.14, which compares the experimental curves and the final previous iteration with the new numerical curve, curve it4.
Figure 5.14 – Graph comparing experimental data with data from the final iterations obtained from cohesive contact TDCB simulations.

As noticed, the numerical curve, it4, no longer has a near perfect agreement with the experimental curves, and is visibly less stiff, due to the lower value for $K_I$, with a slightly lower maximum load than the experimentally obtained one. However, agreement is still within the error interval.

Although the cohesive traction-separation parameters are obtained from the mechanical properties of the material in question, the values obtained after the iterations don’t seem to have any direct physical meaning or significance towards the adhesive other than being a set of fitting parameters for the mode I failure of the FM-300M, using cohesive elements/contact. A comparison between these last parameters and the original parameters can be seen in Figure 5.15.
The traction-separation curves are clearly different and no longer have a relation with the properties of the adhesive. However, these are fitting parameters that allow the numerical simulation of mode I models, which is shown in the next section, when the same parameters were used for the double cantilever beam models.

### 5.2.2.1. Mesh dependency

As described in Chapter 2, numerical instabilities can occur if the mesh is not fine enough. With this in mind, simulations using a finer mesh were performed – all elements, including the adhesive elements, were reduced to a quarter of their original size, as previously done in Section 5.2.1.4. The result is shown in Figure 5.16.
Figure 5.16 – Load versus displacement graph comparing experimental data with data obtained from simulations considering coarse and fine mesh.

As can be seen, the same result was obtained using a coarse and fine mesh, proving that this cohesive parameter provides stable results. The curves are a perfect match, making it only possible to see the black curve when the visible yellow curve that describes the simulation obtained from the fine mesh simulation is plotted with a dotted line. This not only means that good parameters for the CZM have been obtained, but also that the lowest recommended mesh size provided a good result, and therefore there was no need to increase the mesh size.

5.2.3. Summary

Simulations were performed for TDCB models using VCCT modelling. Comparing the numerically-obtained load versus displacement curve with the experimental ones, there is good agreement for the propagation section of the curve, but the model is stiffer in the initiation section of the curve. Several other simulations were performed, changing various parameters such as the element type, the mesh density, the number of nodes and the use of a convergence code written into the input file. However, these modifications made no difference to the prediction, and it was proposed that the extra initial stiffness was due to the VCCT considering that the bonded nodes have an infinite stiffness up to the point when they debond, consequently increasing the stiffness of the model.
For the CZM simulation of the TDCB, an iterative process was used to obtain the mode I CZM parameters for which the load versus displacement curve had good agreement with the experimental curves.

5.3. Double cantilever beam (DCB) simulations

Simulations were performed for a DCB test by once again applying the virtual crack closure technique, VCCT, and cohesive zone modelling, CZM, using the material properties detailed in Table 5.1, and the CZM mode I parameters obtained from the last iteration (it4 – see Section 5.2.2). The load versus displacement curves numerically obtained were then compared with those obtained experimentally, see Section 4.3.1.

Following the study undertaken in Section 5.2.1 concerning the influence of various parameters on the outcome of the simulation results, the element parameters used initially for the TDCB specimens were maintained and used on the DCB specimens. The mesh used on the substrate and endblocks employed elements of 0.5 x 0.25 mm (length and height, respectively); for the adhesive, the elements were 0.5 x 0.05 mm (length and height, respectively). Also, an out-of-plane surface thickness due to the use of 2D models was added as a geometric property, equal to the width of the specimen, i.e. 20 mm.

Simulations were again performed by mimicking the actual experimental test. Since this is another pure mode I test, the boundary conditions are equal to the ones applied to the TDCB. Figure 5.17 shows the location of these restrictions; in B, a pinned boundary condition was inserted, blocking any movement on the X and Y axes, and in A, a boundary condition to prevent any movement in the X-axis. This point is also where a vertical displacement was inserted. The endblock adhesive is always ignored in the tests that use endblocks. To attach the endblock to the composite substrate in the simulation, the composite and endblock is designed as one part, and after they are partitioned into the relevant materials, thus removing the need for tie constraints.
Figure 5.17 – Illustration of the locations of the boundary conditions and displacements in the DCB.

### 5.3.1. Virtual crack closure technique

Taking into consideration the boundary conditions, material and element properties considered in Section 5.3, VCCT simulations were performed, using a value of mode I fracture energy of 906 J/m² instead of 800 J/m², since 906 J/m² was the value obtained in the experimental composite DCB test. A comparison between the numerical and the experimental curves can be seen in Figure 5.18.

![Graph comparing experimental data with data obtained from a VCCT double cantilever beam simulation.](image)

Figure 5.18 – Graph comparing experimental data with data obtained from a VCCT double cantilever beam simulation.

As seen, there is again a difference between the curves in the initiation of crack growth, in which the numerical simulation is stiffer than the experimental load-displacement curve, and again maximum load overshoots, as had been seen in Section 5.2.1. Nonetheless, in the
propagation, there is quite good agreement between the curves, as had already been observed with the TDCB simulations. This extra stiffness is due to the bonded nodes having an infinite stiffness up to the point when they are debonded. However, the predicted curve obtained for this test had a better agreement than that obtained for the TDCB test. This is due to both these models assuming that there is no R-curve behaviour, which was verified experimentally for the DCB, but not the TDCB specimens.

### 5.3.2. Cohesive zone modelling

A simulation of the DCB test was performed using cohesive zone modelling. In this case, since TDCB simulations had already been carried out, the final values of $K_I$ and $\delta_{n0}$ (from iteration 4) which resulted in the best agreement between numerical and experimental curves for later use in mixed-mode testing, were used for this simulation, i.e. $K_I = 5.12 \times 10^{10} \ [Pa/m]$; $\delta_{n0} = 6.2 \times 10^{-5} \ [m]$. The difference in relation to the simulation performed in Section 5.2.2 is the use of the value of $G_{IC}$ of 906 J/m$^2$ obtained from the experimental composite DCB test, rather than the 800 J/m$^2$ determined from the TDCB test. Therefore the area under the traction-separation curve increases, increasing the softening area of the damage evolution. The result is shown in Figure 5.19.

![Figure 5.19](image-url)  

**Figure 5.19** – Graph comparing experimental data with data obtained from a cohesive contact DCB simulation using the CZM parameters obtained from the TDCB simulations.
As seen, there was immediate good agreement between the curve obtained in the simulation and the experimental curves, suggesting that these cohesive element values are good mode I parameters.

5.4. End loaded split (ELS) simulations

Simulations were performed for the ELS test by applying the virtual crack closure technique (VCCT) and cohesive zone modelling, using the material properties detailed in Table 5.1. The element type used for the parts in this simulation was 4 noded generalised plane strain elements, with a mesh density of 0.5x0.25 mm, as previously used in the DCB model, and for the adhesive section, a mesh density of 0.025x0.25 mm was used. However, unlike the TDCB or DCB tests, in this test it was necessary to investigate the influence of the clamp on the boundary conditions that affect the sample.

5.4.1. Clamp calibration

When performing the experimental ELS test, it was necessary to carry out calibration runs with a sample that has no crack. By using this data and knowing the geometry of the clamp, see Figure 5.20, a good set of boundary conditions for the ELS test simulation was obtained.

![Clamp Area and Linear Bearing](image)

**Figure 5.20** – Photograph of the clamping fixture used in the ELS and FRMM quasi-static tests.
The calibration tests, as described in Section 3.4.2, consisted in running the test with an uncracked ELS specimen until a load of 250 N was reached for various different free lengths, \( L \), see Figure 5.21. As shown in the illustration, the calibration simulations were first run mimicking the actual test, putting the whole length of the specimen into the model – with no movement in the X and Y-axis at surfaces A and B relative to the clamp or each other. The clamped system was allowed to have movement along the X-axis relative to the endblock, and finally point C was given a movement restriction on the X-axis, where it was also given a displacement along the Y-axis.

![Illustration of the boundary conditions of the ELS.](image)

**Figure 5.21** – Illustration of the boundary conditions of the ELS.

By running the simulations at free lengths of 50, 60, 70, 80 and 90 mm, it was possible to see the difference between the experimental and the numerical curves, see Figure 5.22 (experimental curves shown with a maximum load of 250 N), and then trying different boundary conditions at the location of surfaces A and B, that improved the agreement between the curves. Since neither VCCT nor CZM are applied in these simulations – model with no crack – there is no concern as to whether the boundary conditions increase or decrease the stiffness of the ELS predictions, later described in Sections 5.4.2 and 5.4.3.
As can be seen, the numerical simulation curves are always stiffer than the experimental curves. Different designs for the mode II clamp calibration test were tried, which included removing altogether the section under the clamp, replacing it with an encastre boundary condition at the end of the ELS calibration specimen; another design was to stop any movement under the clamped area, with only the endblock having vertical movement; and the final model considered the creation of a new part in the model, namely a clamp. However, these changes led to only a slight or no improvement in the results, never being able to match the experimental calibration results with those obtained in the simulations. They were also dependent on the free length used, i.e. a good agreement would be obtained when using one of the free lengths, but no agreement could be obtained for any of the other free lengths. The third design also had another weakness which was the increase of the computational time due to the addition of the clamp part. After attempting these different designs, using different variations of movement restrictions on the clamp area and the endblock, the model which led to the best result without the necessity of making modifications for each free length is shown in Figure 5.23.
**Figure 5.23** – Illustration of the boundary conditions of the ELS simulation:
- - original free length; – new free length considering also the $\Delta_{\text{Clamp}}$. 

The boundary conditions used for the original model (Figure 5.21) were also used in the modified model. The only difference between this model and the previous model, is that the free length of the specimen was increased by 16 mm – this value is equivalent to the clamp correction, $\Delta_{\text{Clamp}}$, which was calculated in Section 4.4 for the purpose of obtaining the value of $G_{\text{HIC}}$. This value is characteristic of the clamp for the specific specimen dimensions and materials, and reflects the rotation and deflection of the specimen at the clamp. The results achieved for this model can be seen in Figure 5.24.

**Figure 5.24** – Graph comparing experimental data with data obtained from ELS calibration simulations using a clamp model with the free length increased by $\Delta_{\text{Clamp}}$. 
Since the results achieved with this model had a much better agreement with the experimentally obtained results, it was used for all the subsequent ELS models.

### 5.4.2. Virtual crack closure technique

With this new model, VCCT simulations were run using a value of $G_{IC}$ equal to 3510 J/m$^2$. Figure 5.25 shows both a comparison between experimental and numerical curves and also a comparison with the numerical curve obtained from the original model for the clamp.

![Graph comparing experimental data with data obtained from ELS simulations using VCCT.](image)

**Figure 5.25** – Graph comparing experimental data with data obtained from ELS simulations using VCCT.

It is possible to see that the curve obtained with this model gives much better agreement than the previous one. Even though it still has a greater maximum load, the initial stiffness and propagation sections of the curve are in good agreement with the experimental curve. This change of behaviour of the initial stiffness between the two models may be because the ELS specimens are pre-cracked before testing, and this should be performed until the crack propagates between 2.5 and 5 mm. This experimental variation is easily verified when examining the experimental curves in Figure 5.25, in which all the experimental curves have different stiffnesses. On the other hand, a specific initial crack is given in the simulation, equivalent to a 5mm pre-crack, for which Sample D was the specimen which had a pre-crack length closest to this value.


5.4.3. Cohesive zone modelling

In order to perform cohesive zone modelling, CZM, an initial set of parameters of the traction-separation curve, shown in Figure 5.26, was determined, to start an iterative process in order to obtain the best agreement between numerical and experimental curves. For these ELS models, the only method used was cohesive contact, since it provides the same results as the cohesive elements, and it is easier to implement.

![Figure 5.26 – Illustration of a traction-separation curve for mode II.](image)

Using Equations 5.4 to 5.5 obtained from [147, 148] and the mechanical property values of the adhesive shown in Table 5.1, initial values of the cohesive contact parameters are obtained:

\[ K_{II} = \frac{G}{t_a} = 1.13 \times 10^{13} \ [Pa/m] \]  
\[ \delta_{t0} = \frac{\sigma_y}{G/t_a} = 4.42 \times 10^{-6} \ [m] \]  
\[ \sigma_{tc} = K_{II} \times \delta_{t0} = 50.0 \times 10^{6} [Pa] \]  

with an adhesive thickness, \( t_a \), of 0.1 mm.

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By using these values for an initial iteration, the stiffness and the maximum load were overestimated. Therefore, an iterative process was performed to find which values best suited these parameters, using these initial values as starting point. As was done for the mode I CZM parameters, the value of $K_{II}$ was the first to be modified, until good agreement between numerical and experimental curves was obtained, i.e. both curves have a similar initial stiffness, followed by modification of the value of $\delta_{t0}$ so that the maximum load in the simulation had good agreement with the experimental results. The curves obtained from this iterative process are shown in Figure 5.27, and the final mode II CZM parameters were:

$$K_{II} = 1.52 \times 10^{12} \text{ [Pa/m]} \quad \delta_{t0} = 2.10 \times 10^{-5} \text{ [m]} \quad \sigma_{tc} = K_{II} \times \delta_{t0} = 3.19 \times 10^{7} \text{ [Pa]}$$

![Figure 5.27 – Graph comparing experimental data with data obtained from CZM simulations.](image)

As can be seen, there was good agreement between the curves obtained in the simulation (it2) and experimentally, suggesting that these cohesive contact values have good mode II parameters. It is also possible to see that the second iteration has a lower stiffness than what was observed in both experimental curves and the previous iteration. This was done for the same reason that the mode I CZM parameters were modified, to be able to predict joints with mixed mode failure. A more detailed explanation of this mixed mode issue is given in Section 5.5.3.

It is also possible to see that the stiffness obtained from the models it1 and it2, is not very sensitive to the change of the value of $K_{II}$, which allows fitting the CZM parameters such that...
not only the mixed mode simulations work, but also so that the area of the first triangle from the traction-separation curve is equal to the value of the threshold fracture energy.

5.5. Fixed ratio mixed mode (FRMM) simulations

Simulations were performed for the FRMM test by applying the virtual crack closure technique (VCCT) and cohesive zone modelling. As used in the previous section, the element type used for the simulation was a 4 noded generalised plane strain element, with an element size of 0.5x0.25 mm for the endblock and substrates, and for the adhesive section, a mesh density of 0.025x0.25 mm. Additionally, tests of the clamp boundary conditions were applied using the modified model obtained in Section 5.4.1, to verify whether it works not only as a clamp calibration for the ELS test, but also for the FRMM test.

5.5.1. Clamp calibration

The FRMM test is very similar to the ELS test in terms of boundary conditions. Therefore, the model obtained for the ELS simulation which had the best accuracy with respect to the boundary conditions (see Section 5.4.1) was applied for the FRMM calibration tests, as shown in Figure 5.28. Figure 5.28a, shows an illustration of a calibration simulation run mimicking the actual test, putting the whole length of the specimen into the model – with no movement in the X and Y-axis at surfaces A and B relative to the clamp or each other. The clamped system was allowed to have movement along the X-axis relative to the endblock, and finally point C was given a movement restriction on the X-axis, where it was also given a displacement along the Y-axis. Figure 5.28b shows the FRMM model, considering the boundary conditions that were validated for the ELS test.
Figure 5.28 – Illustration of the boundary conditions and displacement of the FRMM test: 
a) Physically accurate model; b) Modified model with the free length increased by $\Delta_{\text{Clamp}}$. 
(original free length in dashed line).

Again, the calibration tests were run considering both the normal clamp and the modified 
clamp. The value of the clamp calibration, $\Delta_{\text{Clamp}}$, was again experimentally obtained for the 
FRMM test, and since its value was statistically similar to the obtained in the mode II test, the 
same clamp model was used for the FRMM modelling. The results for the two models 
illustrated in Figure 5.28 can be seen in Figure 5.29 and 5.30, respectively.
Figure 5.29 – Graph comparing experimental data with data obtained from calibration simulations (where L is the free length used in the test).

Figure 5.30 – Graph comparing experimental data with data obtained from FRMM calibration simulations using a model with the free length increased by $\Delta_{\text{Clamp}}$. 
5.5.2. Virtual crack closure technique

Having obtained good agreement between the numerical and experimental curves in the clamp calibration, simulations were performed using that model. However, before doing so, the mixed mode behaviour had to be selected and defined to obtain the correct relationship between the mode I and mode II fracture energies, as seen in Figure 4.32, for which a complete decohesion is obtained. This relationship has a linear shape; however, Abaqus does not have a direct linear option. Therefore, considering that the cohesive elements/contact were given an energy type of damage evolution, i.e. it defines damage in terms of the energy required for failure [149-151], two different laws were taken into account in the simulations, the power law and the BK law. The power law gives:

\[
\left( \frac{G_{II}^{mixed}}{G_{IC}^{mixed}} \right)^{\alpha} + \left( \frac{G_{IIC}^{mixed}}{G_{IIIC}^{mixed}} \right)^{\alpha} = 1
\]  \hspace{1cm} (5.5)

where \( G_{I}^{mixed} \) is the mixed mode fracture energy under mode I; \( G_{II}^{mixed} \) is the mixed mode fracture energy under mode II; and \( \alpha \) is the power law coefficient. For the relationship observed for this adhesive, the coefficient is equal to 1.03. For the BK law, the equation used for the mode mixity relationship is:

\[
G_{C}^{mixed} = G_{IC} + (G_{IIIC} - G_{IC}) \left( \frac{G_{II}^{mixed}}{G_{I}^{mixed} + G_{II}^{mixed}} \right)^{\eta}
\]  \hspace{1cm} (5.6)

where \( \eta \) is the BK law coefficient, equal to 2.06 [149]. A comparison between the relationships obtained using these mixed mode behaviour laws and the experimentally obtained linear relationship is shown in Figure 5.31.
Figure 5.31 – Comparison between the power law and BK law mixed mode behaviour law on the relationship between $G_{IC}$ and $G_{IIC}$.

As can be seen, both laws show good agreement with the linear relationship obtained experimentally. Simulations were then carried out using VCCT on the modified clamp model considering a power law and a BK law mixed mode behaviour, and the result can be seen in Figure 5.32.

Figure 5.32 – Graph comparing experimental data with data obtained from VCCT simulations using two different mixed mode behaviour laws.
Again, there is a difference between the curves in the initial section, in which the numerical simulation is stiffer than the experimental load-displacement curve. Nevertheless, in the propagation, there is quite good agreement between the curves, as had already been observed with the TDCB and DCB simulations. Also, the use of the power law or the BK law made no difference to the end result, as expected.

5.5.3. Cohesive zone modelling

Simulation of the FRMM test was performed using cohesive zone modelling. In the previous sections, DCB and ELS simulations were carried out, thus obtaining values of $K_I$, $K_{II}$, $\delta_{n0}$ and $\delta_{t0}$ for which good agreement between numerical and experimental curves was achieved.

When using the first values for which a good agreement was obtained for both the mode I (it3) and mode II models, $K_I = 5.12 \times 10^{11} \,[Pa/m]$; $\delta_{n0} = 1.6 \times 10^{-6} \,[m]$ and $K_{II} = 1.52 \times 10^{12} \,[Pa/m]$; $\delta_{t0} = 2.10 \times 10^{-5} \,[m]$, respectively, the mixed mode model, due to numerical reasons, was not able to converge once it reached the crack tip, as shown in Figure 5.33, where the simulation aborted as soon as it reached the crack tip.

Figure 5.33 – Comparison between the load-displacement curves obtained numerically and experimentally for the FRMM test.
This non-convergence behaviour in mixed mode has been seen in other research studies [152]. To avoid this, some researchers [148] created a user subroutine and generally they consider that the values of $K_I$ and $K_{II}$ are equal. In this case, the values of $K_I$ and $K_{II}$ are different, and it was not possible to know how Abaqus calculates the $K$ value for mixed mode loading, since they do not release this information to normal users [153]. The information that is given in the Abaqus help files refers to two articles which consider only one value for the stiffness, as previously mentioned. With this in mind, Robinson, Iannucci and Ehrich [154-156] suggested that the stiffness of the mode I models, $K_I$, was too high. Therefore new models were run considering a lower stiffness value. The new mode I cohesive parameters, $K_I = 5.12 \times 10^{10} \, [Pa/m]$; $\delta_{n0} = 6.2 \times 10^{-5} \, [m]$, and the mode II cohesive parameters, $K_{II} = 1.52 \times 10^{12} \, [Pa/m]$; $\delta_{t0} = 2.10 \times 10^{-5} \, [m]$, were then used in the FRMM simulation. These values were picked as they had a lower stiffness, which would allow for mixed mode models to be run, but also so that the area of the first triangle of the traction-separation curves relative to each pure mode was equal to its threshold fracture energy value. The result can be seen in Figure 5.34.

![Graph comparing experimental data with data obtained from FRMM cohesive contact simulations.](image)

**Figure 5.34** – Graph comparing experimental data with data obtained from FRMM cohesive contact simulations.

In terms of initial stiffness, the result obtained is very good compared to the experimental curve, as is the propagation curve, which also shows similar curvature to the experimental
curve. However, the maximum load is a bit higher than that experimentally obtained. Nonetheless, the numerical curve is within good agreement.

5.6. Single lap joint (SLJ) simulations

5.6.1. FE model

In this section, a description of the how single lap joint models were created and their results is given. The SLJ models were developed considering that the bondline has no initial crack. In previous models, a defect (initial crack) was always present, which meant that both VCCT and CZM methods could be used. However, unlike the CZM, VCCT needs an initial crack for it to work. Therefore, the VCCT could no longer be used as a comparison method, and only CZM was used for the SLJ models. For the design of the adhesive, two models were tested: without fillet and with a small fillet with a radius equivalent to the thickness of the bondline, as shown in Figure 5.35, since these had been the fillet types that had been verified under the microscope for the tested SLJ specimens.

![Diagram of fillets and crack propagation path](image)

**Figure 5.35** – Illustration of the fillets and the crack propagation path used in the CZM single lap joint and double scarf joint simulations using: a) no fillet; b) small fillet.

This was to see whether this modification made a big difference to the end result, and whether any difference was due to a greater maximum load or numerical problems in the simulation owing to there not being any fillet. All elements used in this simulation were generalised plane strain (CPE4G1), with an element size of 0.21x0.15 mm in the bonded area section of the arms, and a coarser mesh for the rest of the arms, and 0.21x0.0125 mm in the adhesive part, as shown in Figure 5.36. This allowed a reduction of the processing time.
The SLJ models suffered some modifications, when compared to the experimental specimens, see Figure 5.37a. The endtab section of the SLJ was removed and the boundary constraints were positioned at the ends of the arms: one side had an encastre type boundary, which does not allow movement in any direction. On the opposite side a displacement constraint blocks any movement in the Y-axis and allows a chosen displacement in the X-axis, as illustrated in Figure 5.37b. This reduced the simulation calculation times, without affecting the SLJ boundary conditions in the experimental test, or the numerical results. A simulation of this model was then executed, and the results are discussed in Section 5.6.3.

Figures 5.36 – Image showing the mesh density of the bonded section.

Figures 5.37 – Illustration of the location of the boundary and displacement conditions of the SLJ specimens: a) Full model, b) Reduced model.

5.6.2. Analytical model

To have other data for comparison, a prediction of the maximum load was also obtained analytically, using the Kinloch-Osiyemi model mentioned in Section 2.6.1, since it only
FE modelling considers the mode I fracture energy, $G_{IC}$, contribution to the global fracture energy [57, 98]. This analytical model considers that:

$$G_{\text{max}} = \frac{12}{E_{11}h^3} M_0^2$$  \hspace{1cm} (5.7)

where $M_0$ is the bending moment per unit width, $h$ is the thickness of the adherend, and $E_{11}$ is the Young’s modulus of the adherend. The value of $M_0$ can be calculated using:

$$M_0 = \frac{1}{2} k T_{\text{max}} (h + t_a)$$  \hspace{1cm} (5.8)

where $k$ is the bending moment factor, $T_{\text{max}}$ is the maximum applied load per width, and $t_a$ is the bondline thickness. The value of $k$ is given by:

$$k = \frac{1}{1 + \lambda (c - a)}$$  \hspace{1cm} (5.9)

in which $c$ is equal to half of the overlap length of the joint ($c = 6.25$ mm), $a$ is the crack length, and

$$\lambda = \sqrt{\frac{T_{\text{max}}}{D_f}}$$  \hspace{1cm} (5.10)

with $D_f$ is the bending stiffness, which can be found from:

$$D_f = \frac{E_{11}h^3}{12(1 - \nu^2)}$$  \hspace{1cm} (5.11)

where $\nu$ is the Poisson’s ratio of the adherend [98, 120, 157], and $a$ can be calculated from:

$$a = c - \frac{1}{\lambda} \left[ \left( \frac{3(T_{\text{max}}[h+t_a])^2}{E_{11}h^3G_{\text{max}}} \right)^{1/2} - 1 \right]$$  \hspace{1cm} (5.12)

Finally, the value of $T_{\text{max}}$ was calculated using the properties of the composite and the adhesive, and also assuming that the value of $G_{\text{max}}$ was equal to the value of $G_{IC}$. The result obtained from this analysis is discussed in the next section.
5.6.3. Results

The results that were obtained from both the FE model and the analytical model can be seen in Figure 5.38, where they are compared with the experimental curves.

![Figure 5.38](image)

**Figure 5.38** – Far-field stress versus displacement graph comparing experimental and numerical data for SLJ testing, and the predicted maximum load from the Kinloch-Osiyemi (KO) model.

As can be seen, good agreement was obtained between the curves, both for the maximum load and the displacement. The predicted maximum load obtained from the KO model also provided a very good expected maximum load for the SLJ test, since at initiation, mode I is the dominant failure mode, with mode II increasing its dominance the more the crack propagates [10, 158-160]. The curves obtained when using a fillet in the simulation, were equivalent to those shown in Figure 5.38, since the fillet used in the simulations – and also observed in the experimental tests – was not of sufficient size to make a significant contribution to the increase of the maximum load.

5.7. Double scarf joint (DSJ) simulations

Simulations were performed for the DSJ test by using only cohesive zone modelling for the same reasons described in the previous section. For each part of the model the type of element used was generalised plane strain, and for meshing purposes it was considered that
each element had 4 nodes with a dimension of 0.25 mm by 0.25 mm in the bondline section of the substrate adherends, and a coarser mesh for the rest. In the adhesive section, since cohesive contact was used, two layers of elements were created with a length of 0.25 mm. These models were also performed with and without fillet as was performed for the single lap joints, using the same fillet model as shown in Figure 5.35

As for the SLJ, the DSJ model was simplified in order for the processing time of the simulation to be reduced. Figure 5.39 shows an illustration of the steps taken from the initial full model (Figure 5.39a), where the model looks like the actual tested specimen to the final reduced model that was used (Figure 5.39c), where the model had the endtabs removed, and due to the joints symmetry, only half of the joint was modelled.

![Figure 5.39](image_url)

**Figure 5.39** – Illustrations of the location of the boundary and displacement conditions of the DSJ specimens: a) Full model, b) Modified model, c) Model used in the simulations.

Simulations were then carried out for all tested scarf angles – 7, 30, 45°. The 7 degree DSJ was given an offset at the tip, equivalent to the thickness of one ply (0.125 mm), as experimentally verified in the tested DSJ since it was not possible to cut closer to the edge due to the required angle. This joint suffers peel stresses on the scarfed side of the joint, whereas the opposite side has a more constant dominance of mode II failure, and as had happened experimentally, it was given a crack path through the middle of the adhesive. This joint is predicted to fail with the crack propagating from both ends of the overlap, as was seen for the SLJ.
Since the experimental curves were corrected using the displacement obtained from the extensometer, the load reading in the simulation is performed on the nodes located in the equivalent area in which the extensometer was positioned. The results obtained can be seen in Figure 5.40, 5.41 and 5.42, in which the numerical curve is compared with the experimental curves.

**Figure 5.40** – Load versus displacement graph comparing experimental and numerical data for DSJ 45° tests.

**Figure 5.41** – Load versus displacement graph comparing experimental and numerical data for DSJ 30° tests.
As can be seen, good agreement was obtained between the curves, for both the maximum load and the displacement. For the 45° and 30° DSJ simulations, the predicted loads are similar to those obtained experimentally, as well as being very similar between them both, as was found to be the case in the experimental tests. On the other hand, the 7° DSJ simulation prediction does not agree so well, with an error of approximately 10 kN (see Figure 5.42). However, the actual experimental specimen had a natural fillet as did the 30° and 45° specimens but unlike these two, for the 7° sample the fillet was not as constant throughout the width of the specimen and had quite a bit of overflow, making it harder to give a more precise prediction of the load-displacement curve. Furthermore, it had an offset which may have been influenced by the natural fillet formed around and over it.

5.8. Chapter summary

In this chapter, the numerical simulations of all of the quasi-static experimental tests described in Chapter 4 are presented. A description of the parameters used for each test is given and the simulations were performed using two different methods: VCCT and CZM.
Simulations were performed for TDCB models using VCCT modelling. Comparing the numerically-obtained load versus displacement curve with the experimental ones, there is good agreement for the propagation section of the curve, but the model is stiffer in the initiation section of the curve. Several other simulations were performed, changing various parameters such as the element type, the mesh density, the number of nodes and the use of a convergence code written into the input file. However, these modifications made no difference to the prediction, and it was deduced that the extra initial stiffness was due to the VCCT considering that the bonded nodes have an infinite stiffness up to the point when they debond, consequently increasing the stiffness of the model.

For the CZM simulation of the TDCB, an iterative process was used to obtain the mode I CZM parameters for which the load versus displacement curve had good agreement with the experimental curves.

DCB simulations were performed, and once more the VCCT simulation was stiffer in the initiation part of the curve and had a higher maximum load. However, it showed good agreement with the rest of the curve. For the CZM simulation, the final iteration values obtained for the TDCB were used directly in the first simulation, since TDCB and DCB are both pure mode I tests. This resulted in good agreement between the numerical and experimental data throughout the curve.

In the mode II ELS testing, a clamp was used. A good FE model was obtained by extending the free length, L (distance between the loading pin and the clamp), in relation to the actual experimental test, by adding a Δ_{Clamp}, which was calculated from the calibration tests for this particular mode II procedure. With the new model, the agreement between the numerical and the experimental curves improved greatly, and in the VCCT models, only the maximum load overshot; the initial stiffness and propagation giving good agreement. For the CZM simulation of the ELS tests, an iterative process was used to obtain the values of the mode II CZM parameters for which the load versus displacement curve gave good agreement in relation to the experimental curves.

With the cohesive contact parameters obtained, from the mode I and mode II modelling, these values were used directly in the FRMM simulation. The result obtained was good and within a reasonable error interval. For the SLJ and DSJ models, only CZM was used since these
were considered not to have any initial crack in them. Once again, the cohesive parameters used in the FRMM were used directly in this simulation, which resulted in good agreement between the numerical and experimental data – 12.2 kN and 12.2 ± 1 kN, respectively, for the SLJ; 49.2 kN and 60.0 ± 1.3 kN, respectively, for the 7 Degree DSJ; 53.2 kN and 56.0 ± 2.3 kN, respectively, for the 30 Degree DSJ; 53.2 kN and 55.5 ± 2.1 kN, respectively, for the 45 Degree DSJ.
6. Fatigue experimental and numerical results

6.1. Introduction

In this chapter, the results obtained from the fatigue tests performed throughout the project are shown and analysed. The data gathered in this chapter will allow prediction of the failure of a joint, by inserting the threshold values obtained experimentally into subsequent analyses of other types of joints.

To obtain the threshold values of the adhesive fracture energy for both mode I, $G_{Ith}$, and mode II, $G_{IIth}$, fatigue tests were performed. The same tests that had been carried out for the quasi-static tests were executed in the fatigue tests, i.e. DCB tests were performed for mode I, and ELS tests for mode II.

Fatigue tests were also performed on SLJ for various different percentages of the maximum load obtained from the quasi-static tests described in Chapter 4. This allowed the creation of an S-N curve to be employed later for comparison with the numerical simulations.
Using the values obtained of $G_{Ith}$ and $G_{IIth}$, simulations were performed for the DCB and ELS tests. However, more importantly, simulations were performed for the SLJ tests in order to obtain the threshold maximum load for that type of joint, which was compared with the experimentally obtained S-N curve.

Simulations were also performed for the DSJ models, even though it was not possible to compare the predicted result with the experimentally obtained S-N curve. This was due to the fact that the maximum load achieved by this joint is ~60 kN, and the grips available for fatigue testing are manually tightened, not being able to produce enough pressure to stop the specimen from slipping from its position in the grips. Figure 6.1 shows a flow chart where it is possible to see the link between Chapter 5 with the quasi-static modelling and the current chapter, as well as the steps taken in this chapter, to be able to predict the maximum threshold load of a bonded joint.

---

**Figure 6.1** – Flow chart showing the stages of the fatigue modelling approach.
6.2. Double cantilever beam (DCB)

Fatigue tests were performed using DCB specimens, since they allow the threshold strain energy release rate, \( G_{th} \), to be obtained for this type of failure mode, mode I. Samples with the design shown in Figure 3.10a, section 3.3.2.2, were made. However, unlike the quasi-static samples, a grid scale was not glued to its side for following the evolution of the crack. In this case, a thin layer of correction fluid was also applied to the side of the DCB; once dry, a basic measuring scale was marked with a permanent pen using increments of 5 mm, for reference. A finer scale was unnecessary; crack propagation was read using the scale on the travelling microscope that was used to follow the crack. The setup used in this test was the same as that used in the quasi-static test, shown in Figure 3.20. An Instron 1342 with a 100 kN loadcell was used.

These tests were all performed at room temperature, considering a sinusoidal waveform with a frequency of 5 Hz and an R-ratio of \( \delta_{min}/\delta_{max} = 0.1 \). The displacements used were obtained from Equations 4.4 and 4.5 in section 4.3.1. Modifying Equation 4.5, and considering \( N \) equal to 1, we obtain:

\[
G_{IC} = \frac{3P\delta}{2B(a+|A|)} F \iff P = \frac{2BaG_{IC}}{3\delta} \tag{6.1}
\]

Substituting this into Equation 4.4 we get:

\[
G_{IC} = \frac{16G_{IC}a^2}{9E_s\delta^2} \left( \frac{3a^2}{h^3} + \frac{1}{h} \right) \tag{6.2}
\]

And, consequently:

\[
\delta = \sqrt{\frac{16G_{IC}a^2}{9E_s} \left( \frac{3a^2}{h^3} + \frac{1}{h} \right)} \tag{6.3}
\]

Inserting the data obtained from the experimental quasi-static tests in Equation 6.3, the value of maximum displacement was calculated. A displacement of 75% of this value was then used in the fatigue test. The values applied in the DCB fatigue test can be seen in Table 6.1.
Table 6.1 – Values used for the DCB fatigue tests.

<p>| | |</p>
<table>
<thead>
<tr>
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</tr>
</thead>
<tbody>
<tr>
<td>$\delta_{\text{mean}}$</td>
<td>1.35 mm</td>
</tr>
<tr>
<td>$\delta_{\text{max}}$</td>
<td>2.45 mm</td>
</tr>
<tr>
<td>$\delta_{\text{min}}$</td>
<td>0.25 mm</td>
</tr>
<tr>
<td>Frequency</td>
<td>5 Hz</td>
</tr>
<tr>
<td>Amplitude</td>
<td>1.10 mm</td>
</tr>
</tbody>
</table>

All the specimens failed cohesively in the adhesive, as shown in Figure 6.2. In this figure it is very clear that there are two separate areas: area 1 is failure in mode I under fatigue, and area 2 is failure in mode I when the sample was opened (in order to observe where the adhesive had failed). Area 2 suffered mode I failure at a much higher rate.

Figure 6.2 – Photograph of a tested fatigued DCB specimen with one layer of adhesive: 1) Fracture in fatigue; 2) Fracture from opening specimen.

In these samples, the edges show interlaminar failure, as had occurred in the quasi-static tests. This was due to the cleaning process – the removal of excess adhesive can damage some composite fibres; and because, with such a thin bondline (0.1 mm), a small amount of excess adhesive may still be present, which can pull the fibres at the sides of the specimen. The fracture surface was also analysed using SEM, to determine whether it is different from that observed in the quasi-static test, see Figure 6.2.
Crack Propagation

Figure 6.3 – SEM image of the fracture surface of the mode I fatigue sample.
Fatigue experimental and numerical results

This fracture surface was found to be similar to that obtained from the quasi-static test, the main difference being the mat fibres. In the quasi-static test, the fibres were ripped from their bonded position, due to the relatively quick fracture of the specimen. By contrast, the fibres are subject to a different type of loading in the fatigued DCB, which results in a greater presence of mat fibres evenly spread on both sides of the DCB specimen: due to the cyclic loading, the mat fibres fail at the crack tip, thus not debonding all of the fibre from the adhesive layer, unlike what was seen for the quasi-static fracture surface. As described in Chapter 4 on the quasi-static mode I failure micrographs, although the presence of a carrier matt suggests that it might create a path for a crack to grow, its effect in mode I loading should not be of great consequence to the data obtained for this adhesive.

To analyse the data obtained from these tests, the most common methods used are the secant and the incremental polynomial methods [161-163]. In this project, only the incremental polynomial method was used, since it fits a second-order polynomial expression to seven adjacent data points, and more importantly this reduces the scatter in growth rate which is characteristic of fatigue testing [163]. The results obtained from these tests were analysed to plot a $da/dN$ versus $G_{\text{max}}$ graph by applying an incremental polynomial method [164]:

$$
\hat{a}_i = b_0 + b_1 \left( \frac{N_i - C_1}{C_2} \right) + b_2 \left( \frac{N_i - C_1}{C_2} \right)^2
$$

(6.4)

where,

$$
-1 \leq \left( \frac{N_i - C_1}{C_2} \right) \leq 1
$$

(6.5)

in which $b_0$, $b_1$, and $b_2$ are the regression parameters that are determined by the least squares method between $a_{i-n} \leq a \leq a_{i+n}$; $\hat{a}_i$ is the fitted value of the crack size at $N_i$. The parameters $C_1$ and $C_2$ are used to scale the input data, thus avoiding numerical difficulties in determining the regression parameters, and they are given by:

$$
C_1 = \frac{1}{2} \left( N_{i-n} + N_{i+n} \right)
$$

(6.6)

$$
C_2 = \frac{1}{2} \left( N_{i+n} - N_{i-n} \right)
$$

(6.7)
Fatigue experimental and numerical results

The rate of crack growth at $N_i$ is obtained from:

$$\left(\frac{da}{dN}\right)_{N_i} = \frac{b_1}{c_2} + 2b_2 \frac{(N_i-C_1)}{C_2^2}$$ (6.8)

The value of $G_{\text{max}}$ associated with this $da/dN$ value is computed using the fitted crack size, $\hat{a}_i$, corresponding to $N_i$, in Equation 4.5. The results obtained from this method are shown in Figure 6.4.

![Figure 6.4](image)

**Figure 6.4** – Crack growth rate, $da/dN$, versus $G_{\text{max}}$ obtained from DCB testing using FM-300M adhesive.

As can be seen, only the mode I threshold strain energy release rate, $G_{\text{th}}$, and $G_{\text{IC}}$ are marked on the graph. The Paris law gradient was not obtained, since the approach that is being followed in this project only requires the threshold value, as described in Section 2.6.2, which is later used to predict the threshold load below which the joint (SLJ or DSJ) will not fail. Although this region is not considered for the FE modelling, it has a lot more scatter than would be expected. However, the bondline thickness of these DCB specimens was 0.1 mm, and this is likely to be the reason for the large scatter. Figure 6.5 shows three crack growth rates for mode-I DCB specimens considering three different bondline thicknesses [37].
Figure 6.5 – Fatigue crack growth rates for mode I DCB specimens with different bondline thicknesses using a toughened epoxy adhesive: a) 0.13 mm; b) 0.38 mm; c) 0.79 mm [37].

A greater scatter can be seen for the DCB specimens with a smaller bondline thickness, see Figure 6.5a. For this adhesive, and considering a bondline thickness of 0.1 mm, the value of $G_{\text{th}}$ is approximately 10% of $G_{\text{IC}}$, which, compared to other adhesives, is a common property [106], as illustrated in Figure 6.6.

Figure 6.6 – Relation between total energy release rate and $da/dN$ for bonded composite joints using EC3445 adhesive [165].
To enable comparison of the actual fatigue data with the mode I fatigue model, thereby validating the simulation, a DCB mode I simulation was performed, using the input file that had been generated for the quasi-static test (described in section 5.3.2), replacing the $G_{IC}$ value with the value of $G_{ith} = 115 \text{ J/m}^2$ (see Appendix B1) and using a displacement of 2.45 mm (see Table 6.1). The result of the simulation can be seen in Figure 6.7.

![Figure 6.7 – Load versus displacement graph of the numerical data from the DCB threshold value of $G_{ith}$.](image)

This was then compared with the experimental results, in which the crack length propagated until it reached a length of approximately 70 mm (using an insert of 50 mm), after which it no longer propagated. The FE model showed approximately the same crack length (72 mm), and load (38.7 N) that had been verified experimentally (~70 mm and 42 ±2 N, respectively). This agreement between the experimental and numerical results meant that good CZM fitting parameters had been obtained.

With this in mind, i.e. the analysis from the da/dN graph (Figure 6.4) and the fatigue experimental data, it was possible to plot graphs for other fracture energies, which indicate the number of cycles the DCB can withstand at maximum load before it breaks. Figure 6.8 shows the load-displacement curves for $G_{IC}$ (906 J/m$^2$), $G_{ith}$ (115 J/m$^2$), and $G_l$ values of 200 and 300 J/m$^2$, with their respective lifetime obtained from the experimental fatigue tests and matched to the da/dN versus $G_{max}$ curve. This gives a good illustration as to how rapidly the...
value of the strain energy release rate decreases during cyclic loading, as well as the maximum load that the joint can withstand.

Figure 6.8 – Load versus displacement graph of the numerical data from the DCB tests.

Using this data, a maximum load, $P_{\text{max}}$, versus $\log N$ curve was plotted, as seen in Figure 6.9, which shows the predicted variation of maximum load with fatigue cycles for a composite DCB, using the data available from the tests.

Figure 6.9 – $P_{\text{max}}$-logN curve for a composite DCB bonded using FM-300M.
Analysing Figure 6.8 and 6.9, it is possible to see that even a 65% reduction in the $G_I$ value (from 906 J/m$^2$ to 300 J/m$^2$) gives a life of only around 5000 cycles. A reduction of 88% is needed to reach the threshold value.

### 6.3. End loaded split (ELS)

Fatigue tests were performed for mode II, using ELS specimens, since they allowed the threshold $G$ for this type of failure mode to be obtained. Samples with the design shown in Figure 3.10b were made.

For these fatigue tests, a smaller, lighter clamp was applied, since the one used in the quasi-static tests was unsuitable for a fatigue test due to its size and weight (Figure 5.14). The original clamp did not affect the outcome of the quasi-static tests due to the slow speed at which these were performed. However, in the fatigue tests, where the clamp oscillates at a frequency of 5 Hz, the size and weight of the clamp would have a significant impact on the experimental outcome. Therefore, a new smaller clamp with a linear bearing was used, see Figure 6.10.

![Figure 6.10 – Clamp used in the ELS fatigue tests.](image)

Mode II calibration tests were performed using this new clamp, as described in Section 3.4.2, to obtain the value for the clamp calibration, $\Delta_{\text{Clamp}}$, of 31.2 mm. This value is larger than that obtained in the quasi-static tests, see Section 4.4, due to the use of a light clamp and will be used in the calculation of the mode II threshold strain energy release rate, $G_{\text{IIIth}}$. 

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These tests were all performed at room temperature, using a sinusoidal waveform with a frequency of 5 Hz and an R-ratio of $\delta_{\text{min}}/\delta_{\text{max}} = 0.1$, with the setup shown in Figure 6.11.

![Setup for the ELS fatigue test.](image)

**Figure 6.11** – Setup for the ELS fatigue test.

The displacements used for this test were obtained using equations 4.11 in section 4.4 and equation 6.9:

$$\frac{\delta}{P} = \frac{3a^3 + L^3}{2Bh^3E_1} \Rightarrow P = \frac{2Bh^3E_1\delta}{3a^3 + L^3} \tag{6.9}$$

Substituting this into equation 4.11, we obtain,

$$G_{\text{IIIC}} = \frac{9h^3E_1\delta^2a^2}{(3a^3 + L^3)^{1.1}} \tag{6.10}$$

And, therefore:

$$\delta = \sqrt[3]{\frac{G_{\text{IIIC}}(3a^3 + L^3)^2}{9h^3E_1a^2}} \tag{6.11}$$

Using Equation 6.11 and inserting the data obtained from the experimental quasi-static tests in Section 4.4, the maximum displacement value was calculated. A displacement of 75% of
this value was then calculated as a reference value for fatigue testing, in line with previous experiments. However, due to limitations from the fatigue machine, this value could not be attained and the greatest displacement achieved was 47% of the calculated maximum displacement. This means that the linear region of the da/dN curve is reduced. However, as explained in the previous section, only the value of \( G_{\text{th}} \) is of interest in this project. The values applied in the ELS fatigue test are shown in Table 6.2.

Table 6.2 – Values used for the ELS fatigue tests.

<table>
<thead>
<tr>
<th>75% of maximum displacement</th>
<th>47% of maximum displacement</th>
</tr>
</thead>
<tbody>
<tr>
<td>( \delta_{\text{mean}} )</td>
<td>5.84 mm</td>
</tr>
<tr>
<td>( \delta_{\text{max}} )</td>
<td>10.61 mm</td>
</tr>
<tr>
<td>( \delta_{\text{min}} )</td>
<td>1.06 mm</td>
</tr>
<tr>
<td>Frequency</td>
<td>5 Hz</td>
</tr>
<tr>
<td>Amplitude</td>
<td>4.78 mm</td>
</tr>
</tbody>
</table>

All tests showed interfacial or interlaminar failure, as seen in Figure 6.12. It is very clear that there are three separate areas: area 1 is the pre-crack zone, which failed cohesively, and was subject to mode I quasi-static testing, area 2 is the interfacial/interlaminar failure that occurred during mode II fatigue testing and area 3 is the zone under the clamp, which failed cohesively in the adhesive when the sample was opened under mode I quasi-static testing. SEM images were recorded to establish where area 2 of the sample had actually failed – these images are shown in Figures 6.12 and 6.13.

![Figure 6.12](image)

**Figure 6.12** – Photograph of a ELS fatigue specimen:
1) Pre-crack fracture; 2) Fracture under fatigue; 3) Fracture from opening specimen.
Figure 6.13 – SEM images of the adhesive side of the fracture surface of the area subject to mode II fatigue testing.
Figure 6.14 – SEM images of the composite side of the fracture surface of the area subject to mode II fatigue testing.
As can be seen from the SEM photographs, the failure area on both specimen beams shows unidirectional fibres, which means that the ELS specimens had an interlaminar failure within the composite substrate – this was verified for all tested ELS specimens. The data obtained from these tests were analysed using an incremental polynomial method, as described in Section 6.2. The results obtained are shown in Figure 6.15.

![Figure 6.15](image-url)  
**Figure 6.15** – Crack growth rate, $\frac{da}{dN}$, versus $G_{\text{max}}$ obtained from ELS testing using FM-300M adhesive.

As can be seen, there is a lot of scatter in the results as had also been observed in the mode I fatigue results. However, unlike the DCB fatigue tests, these mode II results did not reach the value of $G_{\text{IIth}}$ since, for all tests, the crack only stopped propagating when it reached the clamped area. As such, only the linear section of the $\frac{da}{dN}$ curve is obtained. Additionally, from Figures 6.12 and 6.13, it was verified that the specimens had interlaminar failure within the composite structure. Therefore this curve is related to the composite rather than to the adhesive, since the adhesive did not fail in all of the fatigue tests. Consequently, it is expected that the $G_{\text{IIth}}$ of the adhesive is greater than the $G_{\text{IIth}}$ of the composite, which had the lowest reading of approximately 180 J/m$^2$. 
Since it was not possible to obtain the value of $G_{Ith}$ for the Cytec FM-300M, it was necessary to estimate it for use in the FE modelling. Therefore, the $G_C/G_{th}$ relation obtained in the mode I experimental tests, as well as research reports in the literature, were taken into account to try to accurately estimate $G_{Ith}$. The experimentally-obtained value of $G_{Ith}$ for this adhesive is approximately 10% of the value of $G_{IC}$; the literature also states that the threshold fracture energy is approximately 10% of the static fracture energy, as in [166]. With this in mind, the value of $G_{Ith}$ used for the FE models was considered to be 10% of the value of $G_{IIC}$, i.e. $G_{Ith}$ equal to 350 J/m².

6.4. Single lap joint (SLJ)

These tests were performed to obtain the S-N curve, which would then be used to compare the threshold maximum load value obtained experimentally with that obtained by FE modelling, employing the values of $G_{Ith}$ and $G_{Ith}$ obtained in Sections 6.2 and 6.3.

6.4.1. Experimental results

Fatigue tests were performed for standard size SLJ specimens, with the design shown in Figure 3.12, and the setup shown in Figure 3.24. Before starting any test, the mean of the maximum load (i.e. failure load) was calculated from all of the quasi-static tests which resulted in a maximum load of 12.2 kN (a maximum stress of 38.4 MPa) for SLJ specimens. Tests were then performed at different percentages of this maximum load, at room temperature, employing a sinusoidal waveform with a frequency of 5 Hz and an R-ratio of 0.1. All tests were carried out using load control, with the aim of measuring the number of cycles that the SLJ specimen can withstand before it fails, for a specific maximum load. Due to issues with machine availability, two different machines were used for this test: a Mayes fatigue machine with a 250 kN loadcell and Phoenix Instruments control, and an Instron 1342 with a 100 kN loadcell and Phoenix Instruments control.

As had happened with the quasi-static tests, all the specimens failed cohesively in the adhesive. Figure 6.16 shows five samples of SLJ tested with different percentages of the maximum load.
Figure 6.16 – Photograph of tested SLJ specimens at various percentages of the maximum load.

All the data were collected to plot an S-N_f curve, shown in Figure 6.17, as well as an S-logN_f curve, Figure 6.18, where the threshold maximum load is approximately 30 % of the quasi-static load, 3.66 kN (a threshold stress of 11.71 MPa).

Figure 6.17 – S-N_f curve for the composite T800S/M21-FM-300M single lap joints.
**Figure 6.18** – S-log\(N_f\) curve for the composite T800S/M21 single lap joints bonded using FM-300M.

From Figures 6.17 and 6.18, it can be seen that the load or stress for which these joints gain a reasonably good joint lifetime is obtained only when the threshold for the SLJ is reached. This value is shown by an arrow, which means that the fatigue test of the SLJ, for that particular load, would have continued running had it not been stopped after 10 million cycles. Figure 6.17 follows the same type of curve that is seen in other epoxy film adhesives, such as Cytec FM-73M, shown in Figure 6.19, for which there is little resistance from the joint under fatigue for up to 35% of the maximum load, below which the single lap joint corresponds to a good joint lifetime.

**Figure 6.19** – Load versus number of cycles to failure for composite 913C XAS-5-47%-FM73M single lap joints [157].
6.4.2. **FE modelling**

Taking into account the data obtained from the DCB and ELS fatigue tests, a SLJ simulation was performed, using the abaqus input file that had been generated for the quasi-static SLJ test, described in section 5.6. The values of $G_{IC}$ and $G_{IIC}$ were then replaced with the values of $G_{Ith}$ and $G_{IIth}$, respectively, as can be seen in the input files shown in Appendix A7 and B2. Figure 6.20 shows the traction separation law for both mode I and mode II, for both the threshold and the critical strain energy release rate values, for which:

\[
\begin{align*}
K_I &= 5.12 \times 10^{10} \text{ [Pa/m]} \quad \delta_{n0} = 6.68 \times 10^{-5} \text{ [m]} \quad \sigma_{nc} = 3.42 \times 10^6 \text{ [Pa]} \\
K_{II} &= 1.52 \times 10^{12} \text{ [Pa/m]} \quad \delta_{t0} = 2.10 \times 10^{-5} \text{ [m]} \quad \sigma_{tc} = 3.19 \times 10^7 \text{ [Pa]}
\end{align*}
\]

Mixed mode behaviour: Power law: $\alpha = 1.03$  
BK law: $\eta = 2.06$

in which all that has changed is the area under the curve. These areas are equivalent to the value of $G_{IC}/G_{IIC}$ or $G_{Ith}/G_{IIth}$, for the static and fatigue curves, respectively. The designated area of the threshold fracture energies has to be situated within the first triangle of the quasi-static traction-separation curve, as this area corresponds to no crack propagation. As explained in Section 5.5.3, this was also taken into consideration when the new CZM fitting parameters were obtained.

![Figure 6.20](image-url) – Traction-separation curves for mode I and mode II.
The same mixed mode behaviour considered in the quasi-static models was used in the fatigue models (i.e. linear), since the value of $G_{th}$ for both mode I and mode II are assumed to have undergone a similar reduction – 10% of the static fracture energy. Therefore, a similar decrease of the value of $G_{IIC}$ was assumed, meaning that the mixed mode behaviour verified in the quasi-static tests, would also be verified in the fatigue tests. The result of the simulation can be seen in Figure 6.21, which predicts a maximum load of 3.86 kN (a maximum stress of 12.35 MPa) which a single lap joint can withstand at the threshold.

![Figure 6.21](image) – Load versus displacement graph of the numerical data for the SLJ threshold test.

This threshold maximum load was obtained with the assumption that the value of $G_{IIth}$ is equal to 10% of the value of $G_{IC}$, since that was a typical epoxy film adhesive value. However, for clarification purposes, a simulation considering a threshold equal to 5% of the quasi-static fracture energy (175 J/m$^2$) was run; the prediction decreased to 3 kN (stress of 9.6 MPa), giving a more conservative but still acceptable result. Therefore, since the use of a much lower value of $G_{IIth}$ still resulted in a reasonable prediction, the estimated value of 350 J/m$^2$ was used for the rest of the FE models.

The numerical result for the threshold maximum stress was then plotted against the experimental results, as shown in Figure 6.22. The KO model was used to predict the threshold maximum load, since it only considers the use of $G_{th}$ and therefore it does not rely
on the use of the estimated $G_{\text{th}}$ in its calculations. Good agreement between the analytical model and the experimental results was obtained.

![S-Nf curve](image)

**Figure 6.22** – S-N$_f$ curve for the composite T800S/M21-FM-300M single lap joints comparing experimental results with threshold analytical and numerical results.

Comparison between the results shows that an excellent prediction for the load threshold has been achieved, with it being situated between 35% of the maximum load where the specimen failed and 30% where it did not fail. However, as previously stated, the real value of $G_{\text{th}}$ is unknown and had to be estimated and, therefore, the real $G_{\text{th}}$ might thus be lower than expected. Additionally, the actual experimental threshold load may also be slightly higher than that obtained experimentally.

**6.5. Double scarf joint (DSJ)**

Fatigue tests were not performed for this type of specimen, since there were no grips that could be used to properly hold the specimens throughout the test. The available grips could only hold the DSJ specimens in place up to a load of 10 kN, after which the specimen would start slipping. However, considering that a good prediction was obtained for the SLJ, a DSJ simulation with a scarf of 45° was performed, using the input file generated for the quasi-static test, described in section 5.7, but replacing the values of $G_{\text{IC}}$ and $G_{\text{IIc}}$ with the values of
Fatigue experimental and numerical results

$G_{Ih}$ and $G_{IIh}$, as had been done for the SLJ simulations. This simulation predicts the value of the maximum load for which no crack propagation occurs. The result from this simulation can be seen in Figure 6.23, showing a maximum threshold load of 17.6 kN.

![Figure 6.23 – Load versus displacement graph of the numerical data for the DSJ threshold test.](image)

This predicted maximum threshold load of 17.6 kN is equivalent to approximately 30% of the value of that obtained in the quasi-static tests. Although this threshold maximum load prediction could not be verified – the grips only work for a load of up to 10 kN –, it is expected to be a good prediction, since it used the same cohesive contact parameters as the SLJ, which provided an excellent prediction of the maximum threshold load.

### 6.6. Chapter summary

In this chapter, the threshold values of the adhesive strain energy release rate, $G_{Ih}$, were obtained. DCB fatigue tests were performed to find the value of $G_{Ih}$. A value of 115 J/m$^2$ was measured which was approximately 10% of the $G_{IC}$. ELS fatigue tests were performed to obtain the value of $G_{IIh}$. However, the ELS samples consistently suffered from interlaminar failure and therefore the values of $G_{II}$ being measured were related to the composite rather than the adhesive; consequently the value of $G_{IIh}$ for use in FE modelling was considered to
be 10% of $G_{IIc}$, i.e. 350 J/m². Fatigue tests of SLJ samples were carried out, in order to obtain an S-N curve and thus a threshold maximum load where failure does not occur.

With all of these data, simulations were run using the Abaqus input files from the simulations described in Chapter 5, replacing only the values of $G_{Ic}$ and $G_{IIc}$ with $G_{Ith}$ and $G_{IIth}$, respectively, to obtain the threshold maximum load values for the SLJ and DSJ from the FE modelling.

Comparing the results obtained experimentally for the SLJ, with an analytical model (KO model) and the numerical simulation, good agreement was achieved between them, with both analytical and numerical models predicting a threshold value of roughly 30% of the quasi-static maximum load, which is validated by the experimental tests. The prediction obtained for the DSJ – also roughly 30% of the quasi-static maximum load – was considered to be good, in the absence of any experimental data.
7. Discussion

7.1. Experimental data

In this project, experimental tests were performed to obtain the critical strain energy release rate for mode I and mode II, $G_{IC}$ and $G_{IIC}$, respectively, as well as their fatigue threshold values, $G_{Ith}$ and $G_{IIth}$, respectively, for a structural epoxy adhesive (Cytec FM-300M). These tests were performed using aluminium alloy and composite substrates (T800S/M21). For both substrates, all of the samples failed cohesively in the adhesive, except for the mode II fatigue in which the composite suffered interlaminar failure.

When comparing the values obtained with those available in the literature, patterns of similarity arose. The value of $G_{IC}$ obtained using composite substrates (906 J/m$^2$) is in good agreement with the value obtained for an adhesive of the same family, the FM-300 [124], which gives a value of approximately 920 J/m$^2$. In the present work, this value was obtained using composite beams, which have a $G_{IC}$ value of 465 J/m$^2$. Although this is clearly a lower value than that of the adhesive, this did not affect the behaviour of the specimen, having failed cohesively in the adhesive. No damage was observed in the substrates, which behaved in the required linear elastic manner. This is due to not only the crack in the adhesive being loaded in mode I while any crack in the composite beams would be loaded under mixed mode as it is not on the axis of the specimen, but also because the composite has no starter crack and hence it is more difficult for an interlaminar crack to initiate.
Fatigue tests of the FM-300M adhesive gave a threshold value of 115 J/m$^2$. This value is approximately 10% of the measured $G_{IC}$. This behaviour, of $G_{th}/G_{IC} \approx 0.1$, is frequently observed with other epoxy film adhesives [106], particularly for adhesives with a relatively low value of $G_{IC}$ as is the case of the FM-300M, as shown in Figure 7.1, even though the $G_{th}$ of the FM-300M is relatively high compared to the other adhesives. The data shown in this figure are for epoxy adhesives which were all tested at similar frequencies (5 Hz) and equal R-ratio of 0.1. Although adhesives tested at other frequencies or R-ratios are not shown here, the same 10% relationship between $G_{th}$ and $G_{IC}$ is observed, when the tests are performed at a frequency and R-ratio of up to 20 Hz and 0.5, respectively.

![Figure 7.1](image)

**Figure 7.1** – Threshold versus critical strain energy release rate for some commercially available epoxies [167-170].

As can be seen in Figure 7.1, adhesives that have a high value of $G_{IC}$, don’t necessarily have an equivalent increase in the value of $G_{th}$. In the cases shown in the figure, it is possible to see that the threshold reduced to approximately 5% of $G_{IC}$, instead of 10%, for the toughest adhesives.

The value of $G_{IIc}$ tends to be higher than $G_{IC}$, due to shear loading present in the mode II test, where the crack surfaces slide over each other, unlike the mode I test, which has tension loading, reducing the energy necessary for a crack to propagate. For this adhesive, its value was approximately 3.5 times the value of $G_{IC}$ (3510 J/m$^2$) than $G_{IC}$. This adhesive property
can be verified in the literature, where the value of $G_{IIc}$ is typically 3 times that of $G_{IC}$ for epoxy adhesives, as shown in Figure 7.2, by the trendline fitted by linear regression to the data.

![Figure 7.2](image_url)

**Figure 7.2** – Critical strain energy release rate for mode I and mode II fracture of commercially available epoxy adhesives [123, 171, 172].

Comparing the value of $G_{IIc}$ obtained for the adhesive against the $G_{IIc}$ of the composite (3510 J/m² and 651 J/m², respectively), it is possible to see that as in mode I, this large difference did not affect the outcome of the test. However, under fatigue loading, this did have an effect on the results, which led to all samples having interlaminar failure. If the threshold value of the composite were 10% of $G_{IIc}$, it would be equivalent to 65 J/m². This very low value would have severe effects on the ability of the crack to propagate through the bondline, as was experienced experimentally with the delamination of all the mode II specimens. Therefore, the value of $G_{IIth}$ for the adhesive was not possible to verify experimentally. Nevertheless, similarly to what was observed for $G_{Ith}$, in the literature the value of $G_{IIth}$ tends to be equivalent to 10% the value of $G_{IIc}$ [166]. Considering that in the previous observations concerning the values of $G_{IC}$, $G_{IIc}$ and $G_{Ith}$, these followed the same pattern as for similar types of adhesive, in the present work it was assumed that the value of $G_{IIth}$ was equivalent to 10% of the value of $G_{IIc}$, i.e. 350 J/m². To confirm this value it would be necessary to test the FM-300M adhesive with a tougher composite, reducing the risk of delamination, or a metal substrate.
The fact that this adhesive has a carrier mat with random fibres embedded in it raised the question of the effect of the carrier on the values of $G$. References [173-175] report that the carrier mat tends to guide the propagation of the crack, since it is the weakest plane in the bondline, because of the reduced surface area caused by its presence [176]. This could explain the good results with the cohesive failure observed all tests, except mode II fatigue, and also the lack of a significant R-curve effect for the mode II quasi-static test, since the carrier mat creates a path of less resistance. There are two main types of carriers, woven and non-woven. The adhesive in this project used a non-woven random fibre carrier mat. The adhesives with random fibre carrier mat are supposed to have better handling properties, which primarily control the bondline thickness by preventing excessive run-out during curing, and also provide a constant bondline thickness when pressures are variable due to slight mismatches in the adherend contours. On the other hand, woven carriers provide an optimum mechanical performance for both shear and peel properties due to the relatively rigid structure of the woven mat, compared to a non-woven mat [177]. A tight knit woven carrier is also capable of having some degree of electrical isolation [175]. An issue that can occur when using carrier mats is bridging. This tends to happen with woven carrier mats, and has the consequence of creating a very large R-curve effect [178]. However, in the present work, no bridging was observed.

All these data were used to create a failure envelope, which relates the ratio of mode I and mode II critical strain energy release rates. This relationship was linear for the FM-300M, a typical phenomenon that can be seen in various other epoxy film adhesives [124], even though other shapes (concave and convex) may also occur. These different shapes occur due to the different toughening agents used in the various adhesives, which may provide increased mode I or mode II characteristics to the adhesive.

An issue that occurred during the project was the different values of $G_{IC}$ obtained when testing aluminium alloy tapered double cantilever beams, TDCB, and composite double cantilever beams, DCB, specimens that have only one layer of film adhesive. Values of 726 J/m² and 906 J/m², respectively, were measured. The plastic zone sizes were calculated. This showed that the radius of the plastic zone was nearly the same size as the bondline thickness. Hence, the height of the plastic zone is restricted, resulting in a relatively low critical strain energy release rate being measured [179, 180].
This explains why a relatively low $G_{IC}$ may be measured for such a thin bondline, but raises the question of the effect of the two different substrates. Bell et al. [181] showed that the value of $G_{IC}$ can be dependent on the substrate used in the TDCB/DCB specimens, due to the influence that the transverse modulus, $E_{22}$, of the substrate has on the form of the stress field ahead of the crack, and consequently on the extent of the plastic deformation ahead of the crack tip. Daghyani et al. [182] showed that a higher level of constraint gave a lower value of $G_{IC}$. This is due to a reduction in the plastic dissipation in the adhesive [183].

The influence of the substrate on the plastic zone was confirmed by testing DCB specimens made using aluminium alloy beams. A mean $G_{IC}$ value of 820 J/m$^2$ was measured for the aluminium DCBs. Bell et al. [181] showed that the measured $G_{IC}$ can be reduced when the substrate thickness is increased, for both steel and CFRP substrates. The data from the aluminium alloy substrates in the present work also follow this trend. However, comparing the R-curves from the aluminium TDCB and DCB specimens shows that only considering the mean values can be rather simplistic. The aluminium alloy DCBs gave a smaller R-curve than the TDCB tests. Overlaying the R-curves shows that a steady state value of $G_{IC}$ is achieved at longer crack lengths in both cases, i.e. where the value of $G_{IC}$ no longer increases but is independent of the crack length. This value of $G_{IC}$ is approximately 800 J/m$^2$ in both cases, indicating that the value of $G_{IC}$ is not dependent on whether DCB or TDCB specimens are used.

This steady state value for the aluminium alloy substrates (of 800 J/m$^2$) can be compared with that for the composite substrates (of 906 J/m$^2$). This shows that the difference in constraint between the two substrate materials results in a difference of approximately 10% between the values of $G_{IC}$ for this bondline thickness.

For the TDCBs and DCBs tested using two layers of adhesive, the values obtained for $G_{IC}$ were $937 \pm 138$ J/m$^2$ and $939 \pm 31$ J/m$^2$, respectively. For these samples the bondline thickness of 0.2 mm is large enough that the plastic zone size is not restricted, and any difference in constraint does not have a significant effect. Indeed, Daghyani et al. [182] showed that as the bondline thickness of an adhesive joint is increased, the constraint is relieved. Comparison of these data with those from the 0.1 mm bondline specimens shows that there is an influence of the bondline thickness on the value of $G_{IC}$. This is well known to happen, and it is commonly reported that the measured $G_{IC}$ will increase to a maximum and
then decrease to a constant value as the bondline thickness is increased [39]. The critical bondline thickness for this maximum value of $G_{IC}$ depends on the toughness of the adhesive [34].

For the validation of predicted maximum loads, two types of overlap joints were tested: a single lap joint, SLJ, and a double scarf joint, DSJ, (using angles of 7, 30 and 45 degrees). The dimensions and testing of the SLJ followed an ISO standard, so it should be possible to compare the results to those obtained with similar adhesives. However, the failure load is dependent on the substrate thickness and properties, so the results are only directly comparable when similar substrate properties and dimensions are used. There are no directly comparable data in the literature, but work using similar systems and dimensions by Tong [184] shows that the measured maximum load is reasonable. This is supported by the modelling work. Analytical models such as the Kinloch-Osiyemi (KO) model were applied, and a maximum load of 11.2 kN was calculated. The cohesive zone modelling approach predicted a maximum value of 12.1 kN. Hence, it is possible to see that good agreement is obtained in both cases in the present work.

The double scarf joints, DSJ, tested in this project were prepared with three different taper angles with the aim of observing which angle provided the highest maximum load. References [105-107] report that between the angles of 90 and 45 degrees there is little or no change to the value of the maximum load. However, the effect of the reduction of stress concentration at the crack tip becomes more noticeable when the taper is less than 45 degrees, with an increase of 30% in the maximum load when the scarf angle is decreased from 45 to 6 degrees, see Figure 7.3. This effect was not as drastic for the FM-300M, which showed a negligible maximum load difference between 30 and 45 degrees. This is also verified in the literature, see Figure 7.3, which shows little change in the value of the maximum load between these angles. However, for a scarf angle of 7 degrees, there was a 10% increase of the maximum load compared to that obtained with the 45 degree DSJ. Even so, these values were within reasonable error, as is shown in Figure 7.3, where the DSJ experimental results are compared with results available in the literature and their error bars.
As can be seen in Figure 7.3, the maximum load values obtained for the DSJs follow the same pattern as those reported in the literature. Although the values obtained are within reasonable error, the lower values obtained for the maximum load at 7 degrees, compared to the literature, may be due to the step at the end of the taper, which was left at 1 ply thickness (0.125 mm). This would increase the stress concentration, thus reducing the maximum load at failure. The size of the fillet will also have an effect. The DSJs in the present work had a very small natural fillet, which again will decrease the failure load due to the higher stress concentration generated, compared to a larger fillet.

### 7.2. FE modelling

Simulations were performed using two methods: the virtual crack closure technique (VCCT) and cohesive zone modelling (CZM). The simulations performed using the VCCT method, although always having good propagation results, always showed high initial stiffness. This has been reported in many previous studies [143-145, 186], although without any explanation. After different modelling approaches to this problem were tried, it was proposed here that this was due to VCCT being rigidly bonded before the nodes debond. Meaning that,
although the bonded elements deform during the test, the nodes that hold the crack tip together are rigidly bonded up to the point that the simulation reaches the fracture energy given for the cohesive failure of the adhesive. This rigidity increases the overall stiffness.

With the CZM modelling, the fitting parameters were calibrated with the modelling of the mode I, mode II and mixed mode tests. These are not a directly known property, i.e. by dividing the modulus of the adhesive by its thickness, and each loading mode uses different values for the stiffnesses, $K_I$ and $K_{II}$, respectively. These values were obtained through an iterative process. Its starting point was obtained by dividing the Young’s modulus and the shear modulus by the adhesive thickness to obtain the initial values for $K_I$ and $K_{II}$, respectively. The need to iterate these values was due to the predictions not matching the experimental results. After a few iterations, a good agreement would be obtained from the calibrated CZM parameters. However, once calibrated these parameters could be used for other simulations with no further modification of the values.

To predict the response in mixed mode, the stiffnesses $K_I$ and $K_{II}$ must be combined. It was not possible to find out what method Abaqus uses to combine the values of the stiffnesses, since this information is not divulged to users [153]. However, some researchers, as in [148], create their own user subroutine which allows the use of equal parameters for mode I and mode II but, similarly to the method used in this study, this subroutine still needs calibration of the value of $K$. Therefore, such user subroutines are not necessarily better, since they increase the effort when building a model and the final result is the same.

With the calibration of the CZM parameters done, SLJ and DSJ predictions were performed. These gave predictions that agreed well with experimental results, having obtained results of 12.1 kN and 12.2 kN, respectively, for the SLJ specimens. The predicted maximum load for the DSJ specimens agreed well with that obtained experimentally, for example, 53 kN and 55.5 kN, respectively for the DSJ with a 45 degree scarf.

In fatigue, the prediction obtained for the single lap joints showed that the threshold maximum load for this adhesive was equivalent to 30% of the quasi-static maximum load, a common characteristic found for other epoxy adhesives tested using SLJ specimens [95]. This threshold was successfully confirmed with experimental data obtained during the project. The maximum threshold load of the DSJ was also predicted, and again an
approximate value of 30% of the quasi-static maximum load was obtained. However, it was not possible to validate the numerical prediction due to lack of experimental data. Nevertheless, Ashcroft et al. [187] report a reduction of the maximum load down to approximately 30% of the quasi-static maximum load in fatigue, suggesting that a good prediction would be obtained for the DSJ.

Thus, the numerical predictions obtained using this cohesive zone modelling approach compared well with the experimental results. The aim of this modelling approach was to obtain in a quick and effective way, the quasi-static and threshold maximum loads for a bonded joint, using calibrated CZM adhesive parameters. Compared to other modelling approaches available in the literature, which calculate the whole S-N curve, this approach is much easier to apply, since it is only necessary to calibrate the CZM parameters and know the values of $G_{IC}$, $G_{IIc}$, $G_{Ith}$, $G_{IIth}$ and the type of mixed mode behaviour that the adhesive has. Once obtained, these parameters can be used directly in any model that has a joint with an adhesive with these specifications (in this case 0.1 mm thick Cytec FM-300M). In this way, the number of simulations required for any joint geometry can be reduced down to two, in order to obtain the quasi-static maximum load and the threshold maximum load for that joint, which are the main values of interest for industry.

### 7.3. Peel Ply

The secondary objective of this project was to find out whether the supplied peel ply provided a good bonding surface or not. This was due to there being lots of issues reported about the effective use of peel ply, especially if it has nylon fibres. Nylon peel plies are difficult to remove from the laminate and can, in some cases, damage the composite [188]. There are two types of peel ply: a conventional (dry) peel ply and a wet peel ply (as used in this project). As the name suggests, the wet peel ply already comes impregnated with the resin ready for curing. For the dry peel ply on the other hand, the resin is bled from the composite laminate. This resin is relatively tough, compared to that impregnated in the wet peel ply, and hence the peel ply can be difficult to remove completely. Hence, current dry peel plies leave contaminated fibres on the composite surface, which can be weak spots in the bondline, as they cause poor adhesion to the substrates [189]. In [116, 188-192] the disadvantages of using peel ply are reported, owing to the lack of proper debonding of the
fibres when removing them from the composite surface, making it necessary to use an additional surface treatment on the composite before bonding.

Nevertheless, the wet peel ply provided a good bonding surface for all specimens with all quasi-static and fatigue tests having failed cohesively in the adhesive or, for mode II fatigue testing, having had interlaminar failure. This peel ply was put further to the test when it was used for DCBs using a different film adhesive, 3M 163-2, which had a value of $G_{IC}$ of $2828 \pm 174 \text{ J/m}^2$ [143]. The experimental results showed once more that the peel ply provided a good bonding surface, making it unnecessary to perform additional surface treatments to the composite surface [131].
8. Conclusions and recommendations for future work

8.1. Conclusions

8.1.1. Introduction

The work described in this thesis was carried out to model and predict the failure of adhesively-bonded composite joint. Quasi-static experimental tests were performed to measure load versus displacement curves as well as other adhesive material properties. Fatigue tests were also performed to obtain the rate of crack growth versus the applied strain energy release rate in mode I and mode II. The stress versus number of cycles (S-N) curve for single lap joints was also obtained. Numerical models were developed using these data, by applying the virtual crack closure technique and the cohesive zone modelling, CZM, method. For the CZM models, traction-separation parameters were obtained for pure mode I and pure mode II, and subsequently used in all quasi-static and fatigue CZM models. The load versus displacement curves were predicted and compared to the experimental curves. The fatigue threshold for the single lap joint was also predicted.

In this chapter, a summary of the project main conclusions is described.
8.1.2. Experimental data

An epoxy film adhesive (Cytec FM-300M) was used to bond unidirectional carbon-fibre composite substrates (Hexply T800/M21). The wet peel ply used provided a good bonding surface, as all specimens tested quasi-statically and in fatigue failed cohesively in the adhesive, except for the mode II fatigue tests. The bondline thickness obtained was 0.1 mm.

The mode I critical strain energy release rate of the adhesive was measured using double cantilever beam, DCB, tests and a value of $G_{IC} = 906 \, J/m^2$ was obtained. A mode II critical strain energy release rate, $G_{IIC}$, of $3510 \, J/m^2$ was obtained from end-loaded split tests. In mixed mode, the critical strain energy release rate, $G_{I/IIc}$, was found to be $1357 \, J/m^2$ for a mixed mode ratio of $G_I/G_{II} = 4/3$. These data gave a failure envelope with a linear relationship, and were used for the numerical modelling work.

The effect of changing the curing time was investigated, but there was no effect on the glass transition temperature, $T_g$, measured using 10 °C/min rate for heating and cooling, a value of $168°C \pm 1.3°C$ being measured in all cases.

Increasing the bondline thickness to 0.2 mm, using two layers of the adhesive, did not significantly change the value of $G_{IC}$. The fracture energy was measured using aluminium alloy DCB tapered double cantilever beam, TDCB, specimens as well as the composite DCB specimens. There was no effect of the substrate used, a mean $G_{IC}$ of $942 \, J/m^2$ being measured.

The choice of substrates did have an effect for the 0.1 mm bondline thickness. A $G_{IC}$ of $726 \pm 35 \, J/m^2$ was measured using aluminium alloy TDCB specimens, and of $820 \pm 36 \, J/m^2$ for aluminium alloy DCB specimens. These were significantly lower than for the composite substrates. The plastic zone size was calculated, and shown to be equal to the bondline thickness, hence squashing the plastic zone and reducing the value of $G_{IC}$. The difference was deduced to be due to the use of different substrates affecting the constraint at the crack tip.

Double scarf joint, DSJ, tests were performed using three different scarf angles, of 7°, 30° and 45°. Only the maximum load achieved varied, and the DSJs with 30° and 45° scarf angles...
Conclusions and recommendations for future work

were statistically similar (56.0 ± 2.3 kN and 55.5 ± 2 kN, respectively). The maximum load for the 7º DSJ was slightly higher at 60.0 ± 1.3 kN.

Fatigue tests were carried out to obtain the mode I and mode II threshold strain energy release rate, $G_{\text{Ith}}$ and $G_{\text{IIth}}$, respectively. All of the DCB fatigue tests failed cohesively in the adhesive and a $G_{\text{Ith}}$ of 115 J/m$^2$ was obtained, which is approximately 10% of the value of $G_{\text{IC}}$.

In mode II, interlaminar failure in the composite occurred, and hence $G_{\text{IIth}}$ could not be measured. As the mode I the threshold fracture energy was 10% of $G_{\text{IC}}$ and other film adhesives show $G_{\text{IIth}}$ to be approximately 10% of $G_{\text{IIIC}}$, the value considered for the FE modelling was assumed to be 350 J/m$^2$, i.e. 10% of the measured $G_{\text{IIIC}}$ for the adhesive.

Quasi-static single lap joint, SLJ, tests were performed a mean maximum load of 12.2 kN was measured. Fatigue tests were performed to obtain the S-N curve, which reached the threshold load at 30% of the quasi-static failure load.

8.1.3. FE modelling

In the modelling part of this project, two modelling techniques were used: the virtual crack closure technique, VCCT, and cohesive zone modelling, CZM.

Simulations were performed for TDCB models using VCCT modelling. There was good agreement between the numerically-obtained load versus displacement curve and the experimental ones, for the propagation section of the curve. However, the numerical curve is stiffer in the initiation section of the curve. Several other simulations were performed, changing various parameters, but these made no difference. It was proposed that the extra initial stiffness was due to the VCCT considering that the bonded nodes have an infinite stiffness up to the point when they debond. Similarly, for the DCB simulations, The VCCT prediction was stiffer in the initiation part of the curve and had a higher maximum load. However, it showed good agreement with the rest of the curve.

For the CZM simulation of the TDCB, a first set of parameters was obtained using the measured mechanical properties and thickness of the adhesive. The result obtained from these
values was unsatisfactory, since there was poor agreement between the numerical and the experimental curves. Therefore, an iterative process was used to adjust these values to obtain load versus displacement curve which gave good agreement with the experimental curves. The final iteration gave values for the mode I stiffness fitting parameter, $K_I$, the separation at damage initiation, $\delta_{n0}$, and stress, $\sigma_{nc}$, of:

$$K_I = 5.12 \times 10^{10} \text{ [Pa/m]} \quad \delta_{n0} = 6.2 \times 10^{-5} \text{ [m]} \quad \sigma_{nc} = 3.42 \times 10^6 \text{ [Pa]}$$

For the CZM simulation of the DCB, the final iteration values obtained for the TDCB were used. This gave good agreement between the numerical and experimental data throughout the curve.

In mode II, an FE model of the ELS test was created which was independent of the free length, using the experimentally measured clamp correction. Good agreement between the numerical and experimental curves was obtained with this model. For the CZM simulation, the same procedure was followed as for mode I, but using the value of the shear modulus, $G$, instead of the Young’s modulus, $E$, for its first set of parameters. An iterative process was used to obtain the load versus displacement curve which gave good agreement with the experimental curves. The final iteration gave values for the mode II stiffness fitting parameter, $K_{II}$, the separation at damage initiation, $\delta_{t0}$, and stress, $\sigma_{tc}$, of:

$$K_{II} = 1.52 \times 10^{12} \text{ [Pa/m]} \quad \delta_{t0} = 2.1 \times 10^{-5} \text{ [m]} \quad \sigma_{tc} = 3.19 \times 10^7 \text{ [Pa]}$$

The cohesive element parameters obtained from the mode I and mode II modelling were used directly in the mixed mode simulation. The agreement between the prediction and the experimental force versus displacement data was good and within a reasonable error interval.

The same cohesive parameters were used in the SLJ and DSJ simulations, which led to good agreement between the numerical and experimental data from both the SLJ and DSJ tests.

To model fatigue, the values of $G_{IC}$ and $G_{IIC}$ were simply replaced by $G_{Ith}$ and $G_{IIth}$, respectively. The threshold load for SLJ tests was predicted to be 3.85 kN, which agrees well with the experimentally obtained S-N curve threshold of 3.6 kN (30% of the static maximum load). A threshold load of approximately 18 kN was predicted for the DSJ, which would also be about 30% of the failure load measured in the quasi-static tests.
8.2. Future work

Throughout the development of this project, some questions in relation to the study of these systems arose. Here some suggestions are given for future work.

First, the experimental validation of the double lap joint, DSJ, simulation prediction of the fatigue threshold load would be of great interest, since it would support the validity of this modelling approach.

Secondly, as this project was related to the use of adhesively-bonded composite structures in the aeronautical industry, experimental tests should be performed at different temperatures, since the aircraft structure has to withstand temperatures that can range from -50°C to 50°C. As well as temperature, the effect of moisture on this adhesive would be an important issue to investigate, since research has shown that they reduce the lifetime of a bonded joint [8, 9]. This would allow a better insight into the limitations or not of the peel ply used in this project, as no additional surface treatment is used.

Another interesting line of thought, again for the validation of the efficiency of the peel ply as a substitute to any surface preparation, would be to do the same experimental tests as have been performed for Cytec FM-300M, but instead using Cytec FM-300U, which is exactly equal to FM-300M, except for the carrier mat, which it does not have. The carrier enables the film adhesive to be easily handled, and also controls the bond line thickness of the adhesive. However, crack propagation usually tends to follow the plane of the carrier [173-175], thus helping the peel ply provide a good surface. By performing tests using the FM-300U, a confirmation on the effectiveness on the use of peel ply would be obtained.

On the modelling side of the project, an investigation into ways of improving the cohesive element definitions as well as understanding exactly what each of the traction-separation curve parameters actually means in terms of the adhesives’ properties would be valuable.
References


46. ESIS TC4 Protocol - Fibre-composites- The determination of the mode II fracture resistance, G_{II,C} of unidirectional fibre-composites using the calibrated end loaded split (C-ELS) test and an effective crack length approach. 2009.
References


89. Feng, *Cohesive Element or Surface-Based Cohesive Behaviour?*, in Analysis of Impact on Pre-loaded Composite Panels. 2009.


120. Taylor, A.C., The Impact and Durability Performance of Adhesively-Bonded Metal Joints, in Department of Mechanical Engineering. 1997, Imperial College of Science, Technology & Medicine.


126. Taylor, A.C., Personal Communication. 2011, Imperial College London.


Appendix

This appendix shows some of the input files of the models used in this thesis. Only the first model (Section A1) displays the whole input file with the exclusion of the majority of the nodes, in order to reduce the size of the file. The rest of the quasi-static models show only the modifications performed at the end of the input file, since the initial, suppressed, section is equal to that shown in Section A1. For the fatigue models input file, only the section that was modified is shown.

A. Quasi-static models

A1. Input file for a model of a TDCB specimen using VCCT

*Heading
** Job name: TDCB Model name: Model-1
** Generated by: Abaqus/CAE Version 6.8-1
*Preprint, echo=NO, model=NO, history=NO, contact=YES
**
** PARTS
**
*Part, name=Bottom
*Node
1, 0.209999993, -4.31082253e-13
2, 0.209999993, 5.99999985e-05
⁞
8707, 0.307999998, 2.99999992e-05
8708, 0.308999985, 2.99999992e-05
*Element, type=CPEG4R
1, 1, 36, 1034, 455
2, 36, 2, 37, 1034
⁞
8369, 975, 8708, 974, 35
8370, 8708, 832, 33, 974
*Node
8709, 0.310000002, 5.99999985e-05, 0.
*Nset, nset=Bottom-RefPt_, internal
8709,
*Nset, nset=_PickedSet152, internal
1, 4, 5, 7, 8, 9, 10, 11, 12, 13, 14, 15, 16, 17, 18, 19
20, 21, 22, 23, 24, 25, 26, 27, 28, 29, 30, 31, 32, 33, 34, 247
*Elset, elset= _PickedSet152, internal, generate
501, 8250, 1

*Nset, nset= _PickedSet153, internal
1, 2, 3, 4, 5, 6, 33, 35, 36, 37, 38, 39, 40, 41, 42, 43
44, 45, 46, 47, 48, 49, 50, 51, 52, 53, 54, 55, 56, 57, 58, 59

8688, 8689, 8690, 8691, 8692, 8693, 8694, 8695, 8696, 8697, 8698, 8699, 8700, 8701, 8702, 8703
8704, 8705, 8706, 8707, 8708

*Elset, elset= _PickedSet153, internal
1, 2, 3, 4, 5, 6, 7, 8, 9, 10, 11, 12, 13, 14, 15, 16
17, 18, 19, 20, 21, 22, 23, 24, 25, 26, 27, 28, 29, 30, 31, 32

8343, 8344, 8345, 8346, 8347, 8348, 8349, 8350, 8351, 8352, 8353, 8354, 8355, 8356, 8357, 8358
8359, 8360, 8361, 8362, 8363, 8364, 8365, 8366, 8367, 8368, 8369, 8370

** Section: Adhesive
*Solid Section, elset= _PickedSet153, material= FM-300M, ref node= Bottom-RefPt_0.01,

** Section: Section-1
*Solid Section, elset= _PickedSet152, material= ALUMINIUM, ref node= Bottom-RefPt_0.01,

*End Part
**

*Part, name= Top
*Node
1, 0.209999993, 0.0232200008
2, 0.209999993, 5.99999985e-05

8707, 0.307999998, 0.0151319997
8708, 0.308999985, 0.0151319997

*Element, type= CPEG4R
1, 1, 36, 1034, 131
2, 36, 37, 1035, 1034

8369, 8707, 8708, 975, 976
8370, 8708, 974, 35, 975

*Node
8709, 2.05872849e-13, 0.0515299998, -1.59473608e-18

*Nset, nset= Top-RefPt_, internal
8709,

*Nset, nset= _PickedSet154, internal
1, 2, 3, 4, 5, 6, 7, 9, 11, 14, 15, 16, 17, 18, 19, 20
21, 22, 23, 24, 25, 26, 27, 28, 29, 30, 31, 32, 33, 34, 35, 36

8687, 8688, 8689, 8690, 8691, 8692, 8693, 8694, 8695, 8696, 8697, 8698, 8699, 8700, 8701, 8702
8703, 8704, 8705, 8706, 8707, 8708
*Elset, elset=_PickedSet154, internal
   1, 2, 3, 4, 5, 6, 7, 8, 9, 10, 11, 12, 13, 14, 15, 16
   17, 18, 19, 20, 21, 22, 23, 24, 25, 26, 27, 28, 29, 30, 31, 32
::
8349, 8350, 8351, 8352, 8353, 8354, 8355, 8356, 8357, 8358, 8359, 8360, 8361, 8362, 8363, 8364
8365, 8366, 8367, 8368, 8369, 8370
*Nset, nset=_PickedSet155, internal
   2, 3, 8, 9, 10, 11, 12, 13, 60, 61, 62, 63, 64, 65, 66, 67
   68, 69, 70, 71, 72, 73, 74, 75, 76, 77, 78, 79, 80, 81, 82, 83
::
2256, 2257, 2258, 2259, 2260, 2261, 2262, 2263, 2264, 2265, 2266, 2267, 2268, 2269, 2270, 2271
2272, 2273, 2274, 2275, 2276
*Elset, elset=_PickedSet155, internal, generate
1001, 1620, 1
** Section: Section-1
*Solid Section, elset=_PickedSet154, material=ALUMINIUM, ref node=Top-RefPt_0.01,
** Section: Adhesive
*Solid Section, elset=_PickedSet155, material=FM-300M, ref node=Top-RefPt_0.01,
*End Part
**
**
** ASSEMBLY
**
*Assembly, name=Assembly
**
*Instance, name=Top-1, part=Top
*End Instance
**
*Instance, name=Bottom-1, part=Bottom
2.62238597503739e-09, -6e-05, 0.
*End Instance
**
*Nset, nset=Pin, instance=Bottom-1
7833,
*Nset, nset=Load, instance=Top-1
7932,
*Nset, nset=BNODES, instance=Top-1
384, 385, 386, 387, 388, 389, 390, 391, 392, 393, 394, 395, 396, 397, 398, 399
::
560, 561, 562, 563, 564, 565, 566, 567, 568, 569, 570, 571, 572, 573, 574, 575
576, 577, 578
*Nset, nset=BNODES, instance=Bottom-1
2, 3, 37, 38, 39, 40, 41, 42, 43, 44, 45, 46, 47, 48, 49, 50
51, 52, 53, 54, 55, 56, 57, 58, 59, 60, 61, 62, 63, 64, 65, 66
Appendix

*Elset, elset=_Mas_S2, internal, instance=Bottom-1, generate
  2, 420, 2
*Elset, elset=_Mas_S4, internal, instance=Bottom-1
  453, 455, 457, 459, 461, 463, 465, 467, 469, 471, 473, 475, 477, 479, 481, 483

*Surface, type=ELEMENT, name=Mas
  _Mas_S2, S2
  _Mas_S4, S4
*Elset, elset=_Sla_S4, internal, instance=Top-1, generate
  1001, 1419, 2
*Elset, elset=_Sla_S3, internal, instance=Top-1, generate
  1481, 1540, 1
*Elset, elset=_Sla_S2, internal, instance=Top-1, generate
  1542, 1620, 2
*Surface, type=ELEMENT, name=Sla
  _Sla_S4, S4
  _Sla_S3, S3
  _Sla_S2, S2
*End Assembly

** MATERIALS

*Material, name=ALUMINIUM
  *Elastic
    7.24e+10, 0.33
  *Plastic
    2.9e+08, 0.
    4.27e+08, 0.196
*Material, name=FM-300M
  *Elastic
    3.121e+09, 0.38

** INTERACTION PROPERTIES

*Surface Interaction, name=FRACT
  0.01,
  *Friction, slip tolerance=0.005
  0.,

** INTERACTIONS

** Interaction: Int-1
  *Contact Pair, interaction=FRACT
Sla, Mas
**  *******************************************************
**
** *INITIAL CONDITIONS, TYPE=CONTACT
Sla, Mas, BNODES
**
** ** STEP: Step-1
**
** *Step, name=Step-1, nlgeom=YES, inc=5000, convert sdi=no
*Static
0.01, 1., 1e-10, 0.015
*CONTROLS, PARAMETERS=TIME INCREMENTATION
, , , , , 50
**
** ** BOUNDARY CONDITIONS
**
** ** Name: Encastre Type: Symmetry/Antisymmetry/Encastre
*Boundary
Pin, PINNED
** ** Name: Displacement Type: Displacement/Rotation
*Boundary
Load, 1, 1
Load, 2, 2, 0.0035
**
** *CONTACT PRINT
*DEBOND, SLAVE=Sla, MASTER=Mas
*FRACTURE CRITERION, TYPE=VCCT, MIXED MODE BEHAVIOR=POWER
800., 800., 0., 2.0, 2.0, 2.0
**
** ** OUTPUT REQUESTS
**
** *Restart, write, frequency=0
**
** ** FIELD OUTPUT: F-Output-1
**
** *Output, field, variable=PRESELECT
**
** ** HISTORY OUTPUT: H-Output-1
**
** *Output, history
*Node Output, nset=Load
CF2, RF2, TF2
*End Step

End Step
A2. Input file for a model of a TDCB specimen using CZM

! ** MATERIALS **
*Material, name=ALUMINIUM
  *Elastic
    7.24e+10, 0.33
  *Plastic
    2.9e+08, 0.
    4.27e+08, 0.196
*Material, name=FM-300M
  *Elastic
    3.121e+09, 0.38
** ** INTERACTION PROPERTIES **
*Surface Interaction, name=FRACT
  0.01,
  *Friction, slip tolerance=0.005
  0.,
**
*COHESIVE BEHAVIOR, ELIGIBILITY=SPECIFIED CONTACTS,
  TYPE=UNCOPLED
  5.12e+10, 5.12e+10, 5.12e+10
*DAMAGE INITIATION, CRITERION=QUADU
  6.68E-05, 6.68E-05, 6.68E-05
*DAMAGE EVOLUTION, TYPE=ENERGY, MIXED MODE BEHAVIOR=POWER
  LAW, MODE MIX RATIO=ENERGY, POWER=2.0, SOFTENING=EXPONENTIAL
  800.0, 800.0, 0.0
** ** INTERACTIONS **
**
** Interaction: Int-1
*Contact Pair, interaction=FRACT
  Sla, Mas
** ----------------------------------------------------------------
** INITIAL CONDITIONS, TYPE=CONTACT
  Sla, Mas, BNODES
**
** STEP: Step-1 **
**
*Step, name=Step-1, nlgeom=YES, inc=5000, convert sdi=no
*Static
  0.01, 1., 1e-25, 0.015
*CONTROLS, PARAMETERS=TIME INCREMENTATION
  , , , , , 50
**
** BOUNDARY CONDITIONS **
** Name: Encastre Type: Symmetry/Antisymmetry/Encastre
*Boundary
Pin, PINNED
** Name: Displacement Type: Displacement/Rotation
*Boundary
Load, 1, 1
Load, 2, 2, 0.0035
**
** OUTPUT REQUESTS
**
*Restart, write, frequency=0
**
** FIELD OUTPUT: F-Output-1
**
*Output, field, variable=PRESELECT
**
** HISTORY OUTPUT: H-Output-1
**
*Output, history
*Node Output, nset=Load
CF2, RF2, TF2
*End Step

A3. Input file for a model of a ELS specimen using VCCT

!** MATERIALS
**
*Material, name=ALUMINIUM
*Elastic
7.3e+10, 0.33
*Plastic
2.9e+08, 0.
4.27e+08, 0.196
*Material, name=FM-300M
*Elastic
3.121e+09, 0.38
*Material, name=M21/T800S
*Elastic, type=ENGINEERING CONSTANTS
1.7e+11, 8.1e+09, 8.1e+09, 0.33, 0.33, 0.01, 4.8e+09, 4.8e+09
4.01e+09,
**
** INTERACTION PROPERTIES
**
*Surface Interaction, name=FRACT
0.02,
*Friction, slip tolerance=0.005
0.,
*Surface Behavior, pressure-overclosure=HARD
** ** INTERACTIONS
** **
** Interaction: Int-1
*Contact Pair, interaction=FRACT
Sla, Mas
** ****************************
**
*INITIAL CONDITIONS, TYPE=CONTACT
Sla, Mas, BNODES
**
** STEP: Step-1
**
*Step, name=Step-1, nlgeom=YES, inc=8000, convert sdi=no
*Static
0.0001, 1., 1e-25, 0.0015
*CONTROLS, PARAMETERS=TIME INCREMENTATION , , , , 250
**
** BOUNDARY CONDITIONS
**
** Name: BC-5 Type: Symmetry/Antisymmetry/Encastre
*Boundary
FIX, YSYMM
** Name: Displacement Type: Displacement/Rotation
*Boundary
Load, 1, 1
Load, 2, 2, 0.02
**
*CONTACT PRINT
*DEBOND, SLAVE=SLA, MASTER=MAS
*FRACTURE CRITERION, TYPE=VCCT, MIXED MODE BEHAVIOR=POWER
906., 3510., 0., 2.0, 2.0, 2.0
**
** OUTPUT REQUESTS
**
*Restart, write, frequency=0
**
** FIELD OUTPUT: F-Output-1
**
*Output, field, variable=PRESELECT
**
** HISTORY OUTPUT: H-Output-1
**
*Output, history
*Node Output, nset=Load
CF2, RF2, TF2
*End Step
A4. Input file for a model of a ELS specimen using CZM

! ** INTERACTION PROPERTIES **
*Surface Interaction, name=FRACT 0.02,
*Friction, slip tolerance=0.005 0.,
*Surface Behavior, pressure-overclosure=HARD **

*COHESIVE BEHAVIOR, ELIGIBILITY=SPECIFIED CONTACTS,
TYPE=UNCOUPL ED 5.12e+10, 1.52e+12, 5.12e+10
*DAMAGE INITIATION, CRITERION=QUADU
6.68E-05, 2.1E-05, 6.68E-05
*DAMAGE EVOLUTION, TYPE=ENERGY, MIXED MODE BEHAVIOR=POWER LAW, MODE MIX RATIO=ENERGY, POWER=2.0, SOFTENING=EXPONENTIAL
906.0, 3510.0, 0.0 **

** INTERACTIONS **

** Interaction: Int-1
*Contact Pair, interaction=FRACT Sla, Mas **

** INITIAL CONDITIONS, TYPE=CONTACT Sla, Mas, BNODES **

** STEP: Step-1 **

*Step, name=Step-1, nlgeom=YES, inc=8000, convert sdi=no
*Static 0.01, 1., 1e-25, 0.015
*CONTROLS, PARAMETERS=TIME INCREMENTATION , , , , , 250 **

** BOUNDARY CONDITIONS **

** Name: BC-5 Type: Symmetry/Antisymmetry/Encastre
*Boundary Clamp, YSYM M **

** Name: Displacement Type: Displacement/Rotation
*Boundary Load, 1, 1
Load, 2, 2, 0.025 **

** OUTPUT REQUESTS **
A5. Input file for a model of a FRMM specimen using VCCT

/* Restart, write, frequency=0 */
/* FIELD OUTPUT: F-Output-1 */
/* Output, field, variable=PRESELECT */
/* HISTORY OUTPUT: H-Output-1 */
/* Output, history */
/* Node Output, nset=Load */
CF2, RF2, TF2
*End Step

! ** INTERACTION PROPERTIES */
** Surface Interaction, name=FRACT 0.02, *Friction, slip tolerance=0.005 0., **
** INTERACTIONS **
** Interaction: Int-1 *Contact Pair, interaction=FRACT Sla, Mas **
** INITIAL CONDITIONS, TYPE=CONTACT Sla, Mas, BNODES **
** STEP: Step-1 **
*Step, name=Step-1, nlgeom=YES, inc=8000, convert sdi=no *Static 0.0001, 1., 1e-25, 0.0015 *
*CONTROLS, PARAMETERS=TIME INCREMENTATION **
** BOUNDARY CONDITIONS **
** Name: BC-5 Type: Symmetry/Antisymmetry/Encastre *Boundary Clamp, YSYM **
** Name: Displacement Type: Displacement/Rotation *Boundary Load, 1, 1
Load, 2, 2, 0.016
**
*CONTACT PRINT
*DEBOND, SLAVE=SLA, MASTER=MAS
*FRACTURE CRITERION, TYPE=VCCT, MIXED MODE BEHAVIOR=POWER
906., 3510., 0., 1.03, 1.03, 1.03
**
** OUTPUT REQUESTS
**
*Restart, write, frequency=0
**
** FIELD OUTPUT: F-Output-1
**
*Output, field, variable=PRESELECT
**
** HISTORY OUTPUT: H-Output-1
**
*Output, history
*Node Output, nset=Load
CF2, RF2, TF2
*End Step

A6. Input file for a model of a FRMM specimen using CZM

** INTERACTION PROPERTIES
**
*Surface Interaction, name=FRACT
  0.02,
*Friction, slip tolerance=0.005
  0.,
*Surface Behavior, pressure-overclosure=HARD
**
*COHESIVE BEHAVIOR, ELIGIBILITY=SPECIFIED CONTACTS,
  TYPE=UNCOPLED
  5.12e+10, 1.52e+12, 5.12e+10
*DAMAGE INITIATION, CRITERION=QUADU
  6.68E-05, 2.10E-05, 6.68E-05
*DAMAGE EVOLUTION, TYPE=ENERGY, MIXED MODE BEHAVIOR=POWER
  LAW, MODE MIX RATIO=ENERGY, POWER=1.03, SOFTENING=EXPONENTIAL
  906.0, 3510.0, 0.0
**
** INTERACTIONS
**
** Interaction: Int-1
*Contact Pair, interaction=FRACT
  Sla, Mas
**-------------------------------------------------------------**
*INITIAL CONDITIONS, TYPE=CONTACT
Sla, Mas, BNODES
**
** STEP: Step-1
**
*Step, name=Step-1, nlgeom=YES, inc=8000, convert sdi=no
*Static
0.01, 1, 1e-25, 0.015
*CONTROLS, PARAMETERS=TIME INCREMENTATION
, , , , , , 250
**
** BOUNDARY CONDITIONS
**
** Name: BC-5 Type: Symmetry/Antisymmetry/Encastre
*Boundary
Clamp, YSYM
** Name: Displacement Type: Displacement/Rotation
*Boundary
Load, 1, 1
Load, 2, 2, 0.016
**
** OUTPUT REQUESTS
**
*Restart, write, frequency=0
**
** FIELD OUTPUT: F-Output-1
**
*Output, field, variable=PRESELECT
**
** HISTORY OUTPUT: H-Output-1
**
*Output, history
*Node Output, nset=Load
CF2, RF2, TF2
*End Step

A7. Input file for a model of a SLJ specimen using CZM

! ** INTERACTION PROPERTIES
**
*Surface Interaction, name=FRAC
0.025,
*Friction, slip tolerance=0.005
0.,
*Surface Behavior, pressure-overclosure=HARD
**
*COHESIVE BEHAVIOR, ELIGIBILITY=SPECIFIED CONTACTS,
TYPE=UNCOUPLLED
5.12e+10, 1.52e+12, 5.12e+10
*DAMAGE INITIATION, CRITERION=QUADU
6.68E-05, 2.10E-06, 6.68E-05
*DAMAGE EVOLUTION, TYPE=ENERGY, MIXED MODE BEHAVIOR=POWER
LAW, MODE MIX RATIO=ENERGY, POWER=1.03, SOFTENING=EXPONENTIAL
906.0, 3510.0, 0.0
**
** INTERACTIONS
**
** Interaction: Int-1
*Contact Pair, interaction=FRACT
Sla, Mas
** -------------------------------------------------------------
**
*INITIAL CONDITIONS, TYPE=CONTACT
Sla, Mas, BNODES
**
** STEP: Step-1
**
*Step, name=Step-1, nlgeom=YES, inc=15000, convert sdi=no
*Static
0.01, 1., 1e-25, 0.015
*CONTROLS, PARAMETERS=TIME INCREMENTATION
, , , , , 250
**
** BOUNDARY CONDITIONS
**
** Name: Encastre Type: Symmetry/Antisymmetry/Encastre
*Boundary
Encastre, ENCASTRE
** Name: Force Type: Displacement/Rotation
*Boundary
Force, 1, 1, 0.00025
Force, 2, 2
**
** OUTPUT REQUESTS
**
*Restart, write, frequency=0
**
** FIELD OUTPUT: F-Output-1
**
*Output, field, variable=PRESELECT
**
** HISTORY OUTPUT: H-Output-1
**
*Output, history
*Node Output, nset=Reading
RF1,
*End Step
B. Fatigue models

B1. Input file modification for a DCB specimen using CZM

!
** INTERACTION PROPERTIES
**
*Surface Interaction, name=FRACT 0.02,
*Friction, slip tolerance=0.005 0.,
*Surface Behavior, pressure-overclosure=HARD
**
*COHESIVE BEHAVIOR, ELIGIBILITY=SPECIFIED CONTACTS, TYPE=UNCOPLED
5.12e+10, 5.12e+10, 5.12e+10
*DAMAGE INITIATION, CRITERION=QUADU
6.68E-05, 6.68E-05, 6.68E-05
*DAMAGE EVOLUTION, TYPE=ENERGY, MIXED MODE BEHAVIOR=POWER LAW, MODE MIX RATIO=ENERGY, POWER=2.0, SOFTENING=EXPONENTIAL
115.0, 115.0, 0.0
**
!
!
!

B2. Input file modification for the SLJ and DSJ model using CZM

!
** INTERACTION PROPERTIES
**
*Surface Interaction, name=FRACT 0.02,
*Friction, slip tolerance=0.005 0.,
*Surface Behavior, pressure-overclosure=HARD
**
*COHESIVE BEHAVIOR, ELIGIBILITY=SPECIFIED CONTACTS, TYPE=UNCOPLED
5.12e+10, 1.52e+12, 5.12e+10
*DAMAGE INITIATION, CRITERION=QUADU
6.68E-05, 2.1E-05, 6.68E-05
*DAMAGE EVOLUTION, TYPE=ENERGY, MIXED MODE BEHAVIOR=POWER LAW, MODE MIX RATIO=ENERGY, POWER=1.03, SOFTENING=EXPONENTIAL
115.0, 350.0, 0.0
**
!