IN-SITU MEASUREMENTS OF DELAMINATION CRACK TIP BEHAVIOUR IN COMPOSITE LAMINATES INSIDE A SCANNING ELECTRON MICROSCOPE

by

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Abstract

Delamination is an important failure mode for composite laminates. As it affects the mechanical response of the structure and is difficult to detect from the surface, this type of damage is of great concern, particularly in the aerospace industry. The topic of delamination growth has received much attention, with Linear Elastic Fracture Mechanics (LEFM) being the most common approach to predict the behaviour of a crack from the global applied conditions. However, local perturbations such as resin rich regions, fibre bridging and friction have been noticed by many investigators. Thus global applied loads are often not transposed directly into equivalent local crack tip conditions. Moreover, there is currently considerable controversy about the exact nature of mixed-mode fracture behaviour. Therefore, the objective of this thesis is to measure the load and displacement applied to a specimen and, at the same time, the crack tip behaviour, in order to establish a quantitative relation between them.

An experimental loading jig designed to fit inside a scanning electron microscope (SEM) has been developed. Mode I, mode II and mixed-mode loadings can be applied. The applied loads and displacements are measured and the images obtained from the SEM are stored. After the test, the crack opening and shear displacements are calculated from the applied loads and displacements using LEFM, and are compared with those measured from the images.

Mode I and mode II tests have been conducted that show good agreement between LEFM predictions and measurements. For a brittle material, the behaviour remains linear elastic up to failure. The comparison of the crack faces displacements with the ones obtained from a finite element analysis are also excellent.
The effect of the increase in fracture toughness with mode I crack growth on the local crack tip behaviour has been studied. As the crack grows, the magnitude of the measured crack opening displacement profiles is reduced. The assumption that fibre bridging keeps the crack closed is thus confirmed experimentally and quantitatively.

As reported by other investigators, 45° microcracks are created ahead of the crack tip under mode II loading. When the load is increased, the ligaments created by the microcracks bend and finally the microcracks coalesce, while more microcracks are created ahead. The growth of this damage zone has been measured and modeled using an analogy with the plastic zone in metals. The stress-displacement curve of the damaged material has also been deduced from the experimental results using a Dugdale approach.

One of the most interesting findings of the mode II tests is the presence of significant crack opening displacements even though the loading is supposed to induce pure shear. The amount of opening varies with the surface roughness of the crack. This can explain the large scatter observed by many investigators in $G_{IIc}$ data, as the tests are not really pure mode II tests, but in fact mixed-mode tests with various proportions of mode I. The determination of the widely used mode II material toughness is therefore questioned.
Sommaire

Le délaminage est un des principaux modes de rupture des laminés de composite. Étant donné qu’il affecte le comportement mécanique de la structure et qu’il est difficile à détecter en surface, ce type d’endommagement est préoccupant, particulièrement pour l’industrie aérospatiale. La propagation du délaminage est le sujet de nombreuses études et la mécanique de la rupture linéaire élastique (MRLE) est la théorie la plus couramment utilisée pour prédire le comportement d’une fissure à partir des charges globales appliquées. Cependant, de nombreux chercheurs ont observé des perturbations locales telles que des zones riches en résine, du pontage de fibres et de la friction. En conséquence, les charges globales appliquées ne se transmettent pas directement en charges locales équivalentes en bout de fissure. De plus, la nature du comportement à la rupture en mode mixte est une source de controverse. Ainsi, l’objectif de ce travail consiste à mesurer les charges et les déplacements appliqués à un échantillon et, simultanément, le comportement en bout de fissure afin d’établir une relation quantitative entre les deux.

Un appareil de chargement a été conçu pour être installé à l’intérieur d’un microscope électronique à balayage (MEB). Des charges en mode I, mode II et mode mixte peuvent être appliquées. Les charges et les déplacements appliqués sont mesurés et les images obtenues du MEB sont enregistrées. Après le test, les déplacements d’ouverture et de cisaillement de la fissure sont calculés d’après les charges et les déplacements appliqués en utilisant la MRLE et sont comparés avec ceux mesurés sur les images.

Des essais en mode I et mode II ont été effectués et correspondent aux prédictions de la MRLE. Pour un matériau fragile, le comportement demeure linéaire et élastique jusqu’à la rupture. Les
déplacements des faces de la fissure sont similaires à ceux obtenus d’après une analyse par éléments finis.

L’augmentation de la ténacité lors de la propagation en mode I a été reliée au comportement local en bout de fissure. L’amplitude des déplacements d’ouverture de la fissure mesurés diminue lorsque la fissure se propage. Ceci confirme expérimentalement et quantitativement l’hypothèse que le pontage de fibres maintient la fissure fermée.

Comme souligné par d’autres chercheurs, des microfissures à 45° sont créées en bout de fissure sous un chargement en mode II. Lorsque la charge augmente, les ligaments créés par les microfissures plient et les microfissures se rejoignent alors que d’autres microfissures se forment à l’avant. La propagation de cette zone d’endommagement a été mesurée et modélisée en utilisant une analogie avec la zone plastique dans les métaux. L’approche de Dugdale a été utilisée pour déduire la courbe contrainte-déformation du matériau endommagé à partir des résultats expérimentaux.

Un des points les plus intéressants des tests en mode II est la présence d’importants déplacements d’ouverture de la fissure alors que ce mode de chargement devrait induire du cisaillement pur. La quantité d’ouverture varie selon la rugosité de la fissure. Ceci peut expliquer la grande variation de \( G_{IIc} \) rapportée par de nombreux chercheurs puisque les tests ne sont pas réellement du mode II pur, mais un mode mixte avec diverses proportions de mode I. La façon de déterminer la tenacité en mode II des matériaux, une propriété largement utilisée, est donc remise en question.
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\( G_{ICG} \)  Mode I global critical strain energy release rate  
\( G_{IB} \)  Mode I bridging strain energy release rate  
\( G_{ICL} \)  Mode I local critical strain energy release rate  
\( G_{friction} \)  Mode II friction strain energy release rate  
\( G_{ss} \)  Steady state bridging zone resistance  
\( G_{12} \)  Laminate in-plane elastic shear modulus  
\( h \)  Half beam thickness  
\( h_d \)  Damage height at the tail of damage zone  
\( h \)  Specimen depth (Appendix D)  
\( k \)  Load-displacement slope  
\( k_{corr} \)  Corrected load-displacement slope  
\( k_{indent} \)  Load-displacement slope from indentation test  
\( k_{measured} \)  Load-displacement slope from three point bending test  
\( K_I \)  Mode I stress intensity factor  
\( K_{II} \)  Mode II stress intensity factor  
\( L \)  Specimen length  
\( L \)  Support span (Appendix D)  
\( L_d \)  Length of the zone containing microcracks that have not coalesced  
\( L_{ss} \)  Maximum bridging zone length  
\( P \)  Applied load  
\( P_I \)  Applied load in Mode I  
\( P_{II} \)  Applied load in Mode II  
\( P_L \)  Left arm load  
\( P_R \)  Right arm load  
\( r \)  Distance from crack tip  
\( r_c \)  Length of zone grown by coalescence  
\( r_d \)  Damage shift  
\( r_I \)  Distance from the COD profile origin  
\( r_{II} \)  Distance from the CSD profile origin  
\( u \)  Longitudinal displacement  
\( U \)  Elastic energy
\( v \) Transverse displacement
\( V_f \) Fibre volume fraction
\( w \) Specimen width
\( W \) Energy required for crack growth
\( \delta \) Displacement
\( \delta_0 \) Ligament maximum opening displacement
\( \delta_I \) Displacement in mode I (opening)
\( \delta_{II} \) Displacement in Mode II (shear)
\( \bar{\delta} \) Damage zone end opening
\( \delta_{s0} \) Ligament maximum shear displacement
\( \bar{\delta}_s \) Shear displacement at initial crack tip
\( \delta_L \) Left arm displacement
\( \delta_R \) Right arm displacement
\( \delta_s \) Shear displacement
\( \varepsilon_{\text{compression}} \) Axial compression strain
\( \varepsilon_{\text{tension}} \) Axial tension strain
\( \Gamma_B \) Energy consumed by fibre bridging pull-out
\( v_{12} \) Laminate Poisson's ration
\( \theta \) Surface waviness angle
\( \sigma \) Normal stress
\( \sigma_0 \) Ligament initial stress
\( \tau \) Shear stress
\( \tau_d \) Maximum stress in damage zone
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À mon père, André D. Paris, M. Ing.,
qui m'a communiqué sa passion pour le métier d'ingénieur
et son incessant désir d'apprendre.
Chapter 1 Introduction

As a result of their high specific properties\(^1\), composite materials are of great interest in applications such as aerospace and aeronautics, where component weight is a critical factor. In these applications, significant savings in fuel costs are possible through the use of composite structures.

In order to take full advantage of their potential, the performance and safe operating lifetime of composite structures must be estimated with precision. At present, accurate predictive methods are lacking, resulting in a great deal of uncertainty and thus overdesign. The application of fracture mechanics to these materials is especially important as a result of their sensitivity to damage and their tendency to degrade their properties when damaged.

As heterogeneous materials, composites are characterized by the presence of several types of inherent flaws. In laminated composites, damage mechanisms are classified as follows (Figure 1.1): fibre breakage, interlaminar matrix cracking and interlaminar matrix delamination. Matrix cracking is the cracking of the resin within a layer, parallel to the fibre direction. Delamination consists of the separation of adjacent layers. It is often difficult to detect as it is not always visible from the surface. The major consequence of delamination is a loss of bending stiffness and compressive properties. In practice, different types of damage can be present simultaneously. However, each damage mechanism must first be modeled in isolation before it can be studied in conjunction with others.

\(^1\) modulus- and strength-to-weight ratios
Chapter 1 Introduction

The prevalent failure mode in composite structures used in the aerospace industry is delamination. This complex failure mechanism is one of the principal factors limiting the use of composite structures in this industry. Thus, delamination failure must be better understood and this is the aim of this project.

1.1 Delaminations in composite laminates

Delaminations can be created during manufacturing of the part or during service. The manufacturing of composite materials introduces delamination by (Garg, 1988):

- contamination of the lay-up by foreign materials preventing adhesion between plies
- improper curing of the matrix
- resin rich region between layers which provide paths of least resistance
- residual thermal stresses due to the cooldown after curing
- machining
- impact by tools and improper handling

Delamination may also be introduced in service by mechanical loads. Local out-of-plane loads create interlaminar stresses which may lead to delamination. They are caused by (Garg, 1988):

- impacts (for example, in aircrafts: runway debris, hailstones, bird strikes, ground service vehicle and ballistics)
- eccentricities in the load path
- discontinuities in the structure
• mismatch of properties between layers near the free edge of a laminated composite

• environmental conditions (moisture gradient through the thickness, thermal stresses due to in-service conditions)

Typical composite structures have complex geometries and carry complex loadings (see Figure 1.2). As seen in Figure 1.2, the range of scales of interest is very large. Even with current computational ability, it would be prohibitive to have one finite element method (FEM) model of the whole structure with the mesh density necessary to provide the detailed local stress fields needed for fracture mechanics calculations. Instead, the overall problem can be split into a number of sub-problems. Using a building block approach (Martin, 1995), the stress distribution obtained from a global model (see Figure 1.2) is used to determine the areas where failure is likely to occur. The global model also provides the boundary conditions to apply to a more detailed model of the sub-structure. This analysis is further refined at the coupon level, where a crack is included in the model (Murri, Salpekar and O’Brien, 1991). The strain energy release rates for the various mode components are calculated and compared with a mixed-mode failure envelope. If the mixed-mode failure behaviour is known, one can iterate through all the steps and refine the design until it is acceptable.

Two related problems exist. First, there is a lack of confidence in the pure mode failure criteria \( G_{Ic} \) and \( G_{IIc} \). Secondly, there is little to inspire confidence in the generality of mixed-mode failure envelopes, nor are there good explanations or understanding of what determines mixed-mode failure criteria. Therefore, the objective of this work is to study quantitatively the local crack tip behaviour and compare the results to those obtained from the global applied conditions, in order to guide and justify the development of failure criteria and failure envelopes. Without a
sound basis for failure criteria, it is difficult to design generically against delamination. If successful, the current work will support the development of standards, as well as increase confidence in the accuracy and validity of building-block approaches as shown schematically in Figure 1.2.

To achieve this objective, a method for studying quantitatively the local crack tip behaviour, while knowing the globally applied conditions, was needed. At the macroscopic scale, the global approach uses the global applied loads and geometry to provide the crack tip stress field. On the other hand, the local approach looks into the fracture process at the scale of ply, interface, fibres, resin region and crack tip profile.

1.2 Global approach

Using this approach, global parameters such as applied load or deflection, geometry, crack length and material compliance are used to evaluate the local conditions in the crack tip area, using Linear Elastic Fracture Mechanics (LEFM). In isotropic and homogeneous materials such as metals, LEFM has proven very useful. However, the use of this approach with composite materials is more complex, as the material is both anisotropic and heterogeneous. Nevertheless, in the case of delamination, the crack is well defined and stress, crack length and specimen geometry can be combined in one similitude parameter, such as the stress intensity factor $K$ or the strain energy release rate $G$, to describe the crack tip stress field.

The stress intensity factor relates the far field applied loads to the local stress field. However, in the case of composite materials, the evaluation of the stress intensity factor is rather complicated. A more convenient approach is the energy method (e.g., Broek, 1986). This approach states that
the crack will grow if there is sufficient energy available to create new surfaces. In a body with crack of length $a$ subjected to a load $P$, the Griffith criterion for growth is:

$$\frac{d}{da} (F - U) = \frac{dW}{da}$$  \hspace{1cm} (1.1)

where $U$ is the elastic energy stored in the plate, $F$ is the work performed by the external load and $W$ is the energy required for crack growth. The left hand side is defined as $G$, the strain energy release rate, while the right hand side represents the material resistance to crack growth (Broek, 1986).

For a linear elastic system under a load $P$, the load application points will undergo a relative displacement $\delta$. By calculating the work done by the external force and the elastic energy stored in the plate, the expression for $G$ becomes (Broek, 1986),

$$G = \frac{P^2 \, dC}{2B \, da}$$  \hspace{1cm} (1.2)

where $C$ is the compliance and $B$ is the width of the specimen.

Thus the strain energy release rate is known if $dC/da$ is evaluated, either by experiment or by calculation. When the crack driving force just equals the energy necessary to create new surfaces, the crack begins to grow. $G$ is then equal to the critical strain energy release rate, $G_c$, also referred to as the fracture toughness.

As $G$ is the total energy, it can be partitioned into the energies due to different load components: mode I, mode II and mode III, depending if the crack is submitted to opening, in-plane shear or anti-plane shear loading (Figure 1.3). In practice, the crack can be submitted to any combination
of these three modes. A combination of mode I and mode II loading is very common and we will refer to it as "mixed-mode" loading.

For pure modes, the compliance can be calculated for some geometries using simple beam theory. In the case of a specimen such as the Double Cantilever Beam (DCB) (Figure 1.4a), for the mode I case, we have (Williams, 1990):

$$G_{IG} = \frac{3P_I \delta_I}{2Ba}$$  \hspace{1cm} (1.3)

and for the End-Notched Cantilever Beam (ENCB) under mode II loading (Figure 1.4b):

$$G_{II} = \frac{9P_{II} \delta_{II} a^2}{2B(L^3 + 3a^3)}$$  \hspace{1cm} (1.4)

where $h$ is the half beam thickness, $B$ is the width and $L$ is the specimen length. Correction factors (Hutchinson and Suo, 1992; Williams, 1989) have been developed to take into account the shear deformation and, if necessary, the effect of large displacements and the stiffening effect of bonded end blocks. The details of the equations used in this work will be presented later.

The subscript "G" used in equations (1.3) and (1.4) indicate that the strain energy release rates are calculated from the global values. Thus, with this approach, the global applied loads and geometry are used to determine the behavior of the crack. However, as composite materials are anisotropic and heterogeneous, some local perturbations might alter the behavior of the crack. Local perturbations such as resin-rich regions, fibre bridging, microcracking, crack path wandering, and friction have been noticed by investigators (e.g., Davies and Benzeggah, 1989; Davies, Moulin and Kausch, 1990). Thus, global applied loads are perhaps not transferred directly into equivalent local crack tip conditions.
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In addition, in the general and realistic case of mixed-mode loading, the global applied loads have to be partitioned into the different mode (I, II) components. Depending on the test geometry, the partitioning is done using beam theory analysis or numerical methods such as finite element, finite difference, boundary element or energy calculations. In all cases, the approximations and idealizations necessary to partition the loads and calculate $G_I$ and $G_{II}$ may lead to inaccuracies (e.g. Hashemi, Kinloch and Williams, 1991; Williams, 1988; Hutchinson and Suo, 1992). Consequently, it is again important to study the local crack tip behaviour.

1.3 Local approach

As a first attempt, qualitative observations of the microstructural aspects of delamination can provide some understanding about the mechanisms involved. Such work includes observations of fractographic surfaces resulting from delamination and in-situ microscopic observation of crack growth. However, these techniques lack quantitative results.

Another approach is to measure some local quantities such as the displacements in the crack tip area, from which the strains can be calculated. Furthermore, the stress intensity factor or the strain energy release rate can be related to the local displacement field, more specifically to the crack tip face displacements. A delaminated specimen subjected to a combined opening and shear mode loading is shown in Figure 1.2. The inset depicts the displacements of the crack tip faces. A grid applied on the specimen edge prior to loading would deform as shown. The applied opening mode forces the crack faces to move apart: this results in the crack opening displacement (COD) profile. The applied shear mode creates a sliding of a crack face with respect to the other; the resulting shift in the vertical lines of the grid is called the crack shear
displacement (CSD) profile. The COD and CSD are functions of $r$, the distance behind the crack tip.

The crack tip displacement field equations for rectilinearly isotropic materials, such as unidirectional composites, were derived by Sih et al. (1965). These equations relate the displacements to the stress intensity factor. The same authors also provided a relationship between the strain energy release rate and the stress intensity factor in rectilinearly isotropic materials. Thus the displacements can be related to the local strain energy release rate. The displacements of the crack face behind the crack tip are of the form:

\[ \text{COD} = A_I \sqrt{r} \sqrt{G_{II}} \]  
\[ \text{CSD} = A_{II} \sqrt{r} \sqrt{G_{III}} \]

where $A_I$ and $A_{II}$ are functions of the elastic properties of the laminate and will be developed later. These equations are only valid close to the crack tip.

The COD and CSD profiles are measured just behind the crack tip and therefore $G_{II}$ and $G_{III}$ are the actual, local strain energy release rates. They are evaluated using equations (1.5) and (1.6), and can then be compared with the one obtained from global measured values. As the $G$ evaluated in both cases is the same physical parameter, the two approaches should be equivalent, unless some local crack tip mechanisms have an effect.

One mechanism which is likely to have such an effect is fibre bridging under mode I loading: as the crack grows, some fibres cross over the crack surfaces, resulting in a measured increase in $G_{IG}$ at failure. One possible explanation is that the fibre bridges restrain the crack from opening and therefore, $G_{II}$ is smaller than $G_{IG}$. Using the superposition principle for the displacements,
the bridged crack is equivalent to the unbridged crack minus the crack loaded by distributed pressure on the crack tip faces corresponding to the stresses in the fibre bridges (Figure 1.5). Therefore,

\[ \text{COD}_{\text{bridged crack}} = \text{COD}_{\text{unbridged crack}} - \text{COD}_{\text{fibre forces}} \] (1.7)

The \( \text{COD}_{\text{bridged crack}} \) is the one actually measured and is related to \( G_{IL} \) via equation (1.5). If the fibre bridges are removed, the crack would open up while the load would remain the same. Thus, using equation (1.5), the \( \text{COD}_{\text{unbridged crack}} \) is related to the \( G_{IG} \) calculated from the applied load \( P_l \) and displacement \( \delta_{IU} \). Since \( \delta_{IU} \) is unknown, we will have to use an equation involving only \( P_l \) to calculate \( G_{IG} \). Finally, we define \( G_{IB} \) as the strain energy release rate associated with the fibre bridge forces; \( G_{IB} \) is related to the \( \text{COD}_{\text{fibre forces}} \) using equation (1.5). Therefore:

\[ \sqrt{\text{G}_{IL}} = \sqrt{G_{IG}} - \sqrt{G_{IB}} \] (1.8)

Consequently, measuring the local COD profile and the global load permits us to obtain \( G_{IL} \) and \( G_{IG} \) and therefore the fibre bridging behaviour and its effect on the increase in toughness.

1.4 Summary

The main goal of the present research is to perform simultaneous quantitative measurements of the applied conditions (load, displacement) and the crack tip face displacements, in order to compare the local and global behaviour and provide a better understanding of delamination behaviour. The thesis is organized as follow:
Chapter 1 Introduction

1) A review of the research found in the literature on delamination crack behaviour is first presented in Chapter 2. Based on the literature review, our objectives to study delamination crack tip behaviour are formulated.

2) Then, the in-situ scanning electron microscope experimental method is described in Chapter 3.

3) Experiments are conducted on a delamination crack under mode I loading. The results are presented and interpreted in Chapter 4.

4) Another series of experiments is conducted under mode II loading. The results are presented and interpreted in Chapter 5.

5) Finally, the main conclusions and the recommendations for further studies are given in Chapter 6.
1.5 Figures

Figure 1.1  Damage types in composite laminates

Figure 1.2  Range of scales of interest in the study of composite structural failure
Figure 1.3 The three modes of loading

Figure 1.4 (a) Double cantilever beam (DCB) and (b) end-notched cantilever beam (ENCB) specimens
Figure 1.5  Illustration of the equivalence between the bridge crack and the superposition of an unbridged crack and a crack loaded by pressure at the crack tip faces.
Chapter 2 Literature Review

Many studies have been dedicated to obtaining the strain energy release rate of composite materials in mode I, mode II and mixed-mode loading from the global conditions, as it is a useful value to predict the fracture failure of a material. The investigators have then tried to study what factors are affecting the resistance to delamination. Some have tried to understand the micromechanisms by directly observing the crack tip. A few have attempted to make measurements of the local crack tip behavior. Finally, even fewer investigators have used these measurements to verify how well the strain energy release rate calculated from the global conditions corresponds to what is happening at the crack tip.

2.1 Delamination fracture toughness

Numerous investigations have been aimed at obtaining the strain energy release rate, based on the global approach described previously. Tests are usually performed on a testing machine applying mode I, mode II or mixed-mode loads. In order to ensure that the delamination crack will propagate along the midplane, a starter film is introduced at the mid-plane of the laminate at manufacturing. From the measured global parameters such as load, displacement and crack length, the strain energy release rate is calculated, and it reaches its critical value when the crack grows. The critical strain energy release rate or fracture toughness is assumed to be a material property. However, several factors can affect the measured value. Thus, the test conditions are important in order to obtain a valid strain energy release rate. Moreover, some of these effects are due to local perturbations. Consequently, the global applied loads may not be entirely transmitted to the crack tip as predicted.
2.1.1 Effects of resin

The resin has been found to play a very important role in the delamination behaviour. Several authors have studied the effect of the type of resin on the delamination fracture of composite materials. To do so, the composite fracture toughness is measured for materials containing different types of resins.

Bradley and Cohen (1985) found that the composite fracture toughness is sensitive to the resin fracture toughness, the interfacial strength and the thickness of the resin rich region. Bradley (1989) plotted the composite fracture toughness $G_{lc}$ and $G_{llc}$ as a function of the neat resin fracture toughness, for various materials (Figure 2.1). The results showed that the composite $G_{lc}$ increases when the neat resin $G_{lc}$ increases. However, above a certain value, the increase in composite $G_{lc}$ becomes less significant. It was also found that, although the composite toughness is significantly higher in mode II than in mode I, the composite mode II toughness is not as sensitive to the resin toughness. In order to understand the relationship between the neat resin and the composite fracture toughness, the micromechanisms of delamination growth were studied, using three methods: the observation of post-fracture mode I, mode II and mixed-mode delamination surfaces using a scanning electron microscope (SEM) (Bradley and Cohen, 1985), the real-time observation of delamination growth under pure mode I and mode II using an SEM (Bradley and Cohen, 1985; Bradley, 1989) and the measurement of the strain field around the delamination crack tip using an SEM (Bradley, 1989).

2.1.1.1 Micromechanisms of mode I delamination

In mode I, for composites with brittle resins, the composite fracture toughness is higher than the neat-resin fracture toughness (Figure 2.1). This can be explained by the increase in fracture
surface due to a more tortuous path and the presence of fibres that may bridge the crack and restrain its opening, or dissipate energy when they break (Bradley and Cohen, 1985). For composites with thicker resin-rich regions, the crack path is less tortuous and there is less fibre bridging, therefore the toughness is lower, closer to the neat resin toughness. Moreover, Corleto et al. (1987) have shown, using linear finite element analysis, that the stresses in front of the crack tip are more distributed in the orthotropic composite than for the isotropic resin. Therefore, a higher load can be applied to the composite specimen before the critical crack tip stress is reached. This is confirmed by in-situ measurement of the strain field around the crack tip: the strain field observed in the composite is longer and narrower than in the neat resin (Bradley, 1989).

In a ductile resin, a larger plastic zone is formed in front of the crack tip (Figure 2.2) and the crack tip is more blunted. This is confirmed by the in-situ measurement of strain field in neat resin specimen, where the extent and intensity of the strain field is much higher in more ductile resins (Bradley, 1989). Consequently, the load is redistributed in front of the crack tip and the resistance to delamination is increased. In the composite, the plastic zone size increases with the neat resin fracture toughness, until it reaches the size of the resin rich region between plies and becomes constrained by the fibres (Figure 2.1). Above this point, the composite fracture toughness is less than the neat resin fracture toughness and an increase in the resin fracture toughness has little effect on the composite resistance to delamination. As opposed to brittle resin composites, a ductile resin composite with thicker resin rich region between plies has a higher fracture toughness, since a bigger plastic zone can develop (Bradley and Cohen, 1985).
It has also been noted that the improvement provided by the matrix is fully used only if there is good interfacial adhesion (Bradley, 1989).

2.1.1.2 Micromechanisms of mode II delamination

In mode II, for brittle resin composites, a very different micromechanism of fracture was observed: as can be observed in Figure 2.3, a series of sigmoidal shaped microcracks are formed in front of the crack tip with an orientation of approximately 45° to the fibre direction and the crack grows as a result of the coalescence of these microcracks (Hibbs and Bradley, 1987; O'Brien et al., 1989; Bradley, 1989). The microcracks are created on the principal normal stress plane, which for pure shear loading forms a 45° angle with the specimen midplane (Figure 2.4a). As the load is increased, the ligaments formed by microcracks rotate due to the shear loading and coalescence occurs between the microcracks (Figure 2.4b) resulting in the creation of hackles.

Since the microcrack creation and coalescence requires more energy than the continuous crack growth observed in mode I, \( G_{IIc} \) is higher than \( G_{Ic} \) (Figure 2.1). This is confirmed by the observation of a much longer damage zone in mode II. The in-situ measurement of strains ahead of a crack tip also show a long and narrow strain field which include shear deformation and microcracking and result in load redistribution and energy dissipation (Bradley, 1989). Furthermore, these results are consistent with the stress field ahead of the crack tip calculated by a linear finite element analysis (Corleto et al., 1987). In mode II, the shear stress ahead of the crack tip decays much more slowly than the normal stress ahead of the crack tip in mode I (see Figure 2.5), resulting in a load redistribution over a longer distance and therefore, in a higher
toughness. A model for the mode II fracture toughness based on the resin properties and the failure process has been proposed by Lee (1997).

For very ductile resin composites, the sigmoidal shaped microcracks are not observed and the fracture is similar to mode I, with plastic deformation, and $G_{lc}$ and $G_{llc}$ are comparable (Hibbs and Bradley, 1987; Russell and Street, 1987; O’Brien et al., 1989).

Thus, in mode II, we have two very different processes, the microcracking process for brittle resins and the plastic deformation process for ductile resins. $G_{llc}$ is not as sensitive to neat resin toughness as $G_{lc}$, probably because the two different processes accomplish the same load redistribution effect (Bradley, 1989).

### 2.1.2 Effect of starter film and precrack

Another factor that has an important effect on the fracture toughness is the insert. It has been shown (Davies et al., 1990) that when the starter film is thicker, the values of $G_{lc}$ and $G_{llc}$ obtained from the insert are higher and there is more scatter. This is because the thick film acts like a blunt crack.

Moreover, they studied the effect of the presence of a precrack created with mode I loading or mode II loading. Compared to the tests with no precrack, the mode I precrack gave a higher value of $G_{lc}$ while the mode II precrack gave a lower one. The high value with mode I precrack may be explained by fibre bridging the crack and increasing the resistance to crack growth. In the case of the planar, well defined mode II precrack, there is no fibre bridging and the $G_{lc}$ value is close to the one obtained with the thinnest insert. Therefore, for unidirectional carbon fibre/epoxy specimens, a starter film thickness of the order of 20 microns or less provides an
appropriate value of the initiation mode I toughness. O'Brien et Martin (1993) also reported tests with different inserts types and thicknesses. With inserts of 13 microns or less, the toughness value did not change much anymore.

$G_{IIc}$ values were also obtained (Davies et al., 1990; Prel et al., 1989; Carlsson and Gillespie, Jr., 1989; Russell, 1991) from the insert and for specimens with mode I precrack and mode II precrack. The highest values were obtained with specimens with no precrack, because the crack tip is blunted at the end of the starter film. This effect seems to be more severe than in mode I, as the value obtained for the thinnest starter film (20 microns) is still significantly higher than with the mode I precrack (Davies et al., 1990). The values obtained with mode I precrack were slightly lower than those obtained with a mode II precrack (Carlsson and Gillespie, Jr., 1989; Russell, 1991). A cyclic mode II precrack gave the same results as a static mode II precrack (O'Brien et al., 1989).

These results show the difficulty in creating a valid crack for standard testing. The starter film has to be as thin as possible to minimize the matrix-rich region, and yet a minimum thickness is required to avoid wrinkling of the film during moulding (Davies and Benzeggah, 1989). If a mode I precrack is used, it should be sufficiently long to avoid a matrix-rich pocket, but short enough to reduce the amount of fibre bridging (Davies and Benzeggah, 1989).

### 2.1.3 Effect of fibre bridging

Fibre bridging consists in fibres crossing over the crack faces (Figure 2.6). During the lay-up process, successive layers of fibres preimpregnated with resin are stacked, forming distinct plies. During cure, those plies become more or less intermingled, especially in unidirectional laminates.
When a crack grows, those fibres going from one layer to the other create a bridge between the two crack surfaces. Moreover, especially in tougher composites (Figure 2.8), if flaws in planes above or below the crack plane are loaded, the crack can change to that plane and a bridge is created (Davies and Benzeggah, 1989). These bridges restrain the opening of the crack and increase the fracture resistance. Thus the critical energy release rate $G_c$ may increase as the crack propagates and more fibre bridging is created (Figure 2.9). In this case, the critical energy release rate is not a unique value. There is an initiation value for $G_c$, then it increases and eventually reaches a stable plateau value if fibre bridges are broken at the same rate as they are created (Davies and Benzeggah, 1989).

If the specimen thickness increases, the initiation value does not change, but the propagation value increases significantly due to the increase in fibre bridging (Russell and Street, 1982; Prel et al., 1989). Thus the crack behaviour beyond the initiation value is not a material property.

Since R-curves are specimen dependent, it has become more popular to characterize a material resistance behaviour using bridging laws (Suo et al., 1992; Spearing and Evans, 1992). According to these authors, the determination of the bridging tractions $\sigma$ created by the bridging fibres as a function of the local crack opening $\delta$ can provide a general description of the resistance curve behaviour, applicable to various specimen geometries. It can be determined experimentally or by modelling. In polymer matrix composite materials, it can be used to model the increased resistance with crack growth that occurs under different micromechanisms: fibre bridging, micro-cracking damage, etc.

This approach uses the Dugdale model (Dugdale, 1960), which uses an array of continuously distributed non-linear springs to simulate the material in the damage zone (see Figure 2.10).
Each point in the damage zone experiences the same relationship between the bridging tractions \( \sigma \) and the local crack opening \( \delta \). It is assumed that the traction law \( \sigma(\delta) \) is characteristic of a given material and damage type and is independent of geometry. The traction \( \sigma \) has an initial value \( \sigma_0 \) and becomes zero above a maximum separation value \( \delta_0 \).

Applying the J-integral conservation (Rice, 1968),

\[
G = G_0 + \int_0^{\delta} \sigma(\delta) d\delta
\]

(2.1)

where \( \delta \) is the end-opening of the damage zone (Figure 2.10), \( G \) is the applied strain energy release rate and \( G_0 \) is the energy dissipated at the crack front. When the end-opening is equal to the critical separation, \( \delta = \delta_0 \), \( G \) reaches a plateau corresponding to the steady-state resistance \( G_{ss} \), which is equal to the sum of \( G_0 \) and the area under the \( \sigma(\delta) \) curve and does not depend on the specimen geometry (see Figure 2.10). At this point, the length of the bridging zone, \( L \), also reaches a maximum, \( L_{ss} \), which depends on both the bridging mechanism and the specimen geometry (see Figure 2.10). Using finite element analysis, Suo et al. (1992) have provided equations for calculating the R-curve for various delamination beams and idealized bridging laws, once the parameters are known.

The difficulty is to determine the parameters of the bridging laws, and several experimental and analytical approaches have been taken. One very elegant approach has been suggested (Suo et al., 1992). Differentiating (2.1) yields:

\[
\sigma(\delta) = \frac{\partial G}{\partial \delta}
\]

(2.2)
Therefore, the bridging law can be measured experimentally by measuring the damage zone end-opening ($\tilde{\delta}$) together with the R-curve.

Another approach for measuring the bridging law has been adopted (Spearing and Evans, 1992), for a DCB specimen. By modelling a bridging ligament as a short cantilever beam deforming in shear and bending, they have shown that a linear strain softening law is the appropriate approximation of the bridging law:

$$\sigma = \sigma_0 \left(1 - \frac{\delta}{\delta_0}\right)$$  \hspace{1cm} (2.3)

Using (2.1) gives:

$$G_{ss} = G_0 + \frac{\sigma_0 \delta_0}{2}$$  \hspace{1cm} (2.4)

Then they measured the R-curve for several ceramic composites and for carbon fiber-PEEK DCB specimens. The initiation resistance $G_0$ and the steady-state bridging zone length $L_{ss}$ and resistance $G_{ss}$ were obtained from those curves. The maximum end-opening $\delta_0$ was calculated from beam theory, at a distance $L_{ss}$ from the crack tip, neglecting the effect of the fibre bridging on the beam profile. Then $\sigma_0$ was calculated using equation (2.4). Finally, the entire R-curve was calculated using an equation for DCB specimens loaded by end-moments (Suo et al., 1992). The results show good agreement with the experimentally measured R-curve, even for various specimen thicknesses and the fibre bridging parameters $\sigma_0$ and $\delta_0$ obtained are reasonable.

A test designed to measure directly the closure pressure vs. displacement curve has also been designed (Kaute et al., 1993). Ceramic matrix composites specimens were precracked from side to side, with only the fibre bridges holding the faces together and pulled apart. The resulting
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curve shows a steeply rising part followed by a shallow fall, typical of a strain-softening behaviour. They have also modelled the fibre bridging using basic constituents and interface properties and the shape of the closure pressure curve obtained is similar to the experimentally measured one.

2.1.4 Effect of friction

Fibre bridging is not observed in mode II loading (Russell and Street, 1985), but another energy dissipating mechanism is present. A mode II load applied by bending may create friction between the crack surfaces of the two delaminated beams. The relative sliding of the two half beams is then restricted and some energy is absorbed in overcoming this friction. For end notched flexure (ENF) geometry, the effect of friction has been evaluated using finite element analysis, and the error induced in the strain energy release rate was 2-5% for a coefficient of friction varying between 0.25 and 0.5 and a thickness to crack length ratio h/a less than 0.05 (Gillespie, Jr. et al., 1986). According to this analysis, the error induced by friction on the strain energy release rate is proportional to the coefficient of friction and to the thickness to crack length ratio. However the real value of the interfacial friction coefficient is unknown. The hysterisis was measured in the loading and unloading curve (Russell and Street, 1985), indicating less than a 2% overestimate of the strain energy release rate. However, this is a global value that would be insensitive to any localized friction near the crack tip.

2.1.5 Mixed-mode failure criterion

There has been extensive testing in pure mode I and pure mode II, and values for the fracture toughness $G_{Ic}$ and $G_{IIc}$ for different materials are well established. But delamination cracks are
often loaded under a combination of mode I and mode II loads. It would be useful to have a simple failure criterion, but unfortunately, this is not what has been observed experimentally (Garg, 1988). Investigators have proposed several different equations in order to fit their test results.

Different tests configuration have been used to introduce a mixed-mode loading (Figure 2.11). Cracked lap shear (CLS) and mixed-mode flexure (MMF) rely on an eccentric load path to induce mixed-mode loading. Their inconvenience is that they require a different lay-up of the specimen with different thickness of the two half beams to obtain a different mixed-mode ratio. Other configurations use special test jigs to apply the two loads. In the mixed-mode bending (MMB) test, a lever is used to apply simultaneously mode I and mode II and its position determines the mixed-mode ratio. Finally, mixed-mode can be created by asymmetrically loading a double cantilever beam specimen.

From the loads measured at initiation, the mode I and mode II components are partitioned and $G_I$ and $G_{II}$ are calculated. Depending on the test geometry, the partitioning is done using beam theory analysis or numerical methods such as finite elements, finite difference, boundary elements or energy calculations. In all the cases, the approximations and idealizations necessary to partition the loads and calculate $G_I$ and $G_{II}$ may lead to inaccuracies.

Figure 2.12 shows a plot of the sum of the critical mode I and mode II component as a function of the proportion of mode II loading: at the left of the graph are the values for pure mode I and at the right, for pure mode II. As pointed out by O'Brien (1997), the scatter in the toughness values increases significantly with the mode II loading, and there is considerable scatter for the the pure mode II toughness.
2.2 Direct observation of crack tip behaviour

Another approach to understanding the delamination process is to observe the micromechanisms. We have already mentioned that some authors used those observations to try to explain the changes in the fracture toughness when some factors were varied.

2.2.1 In-situ crack growth observation

Several authors have tried to directly observe the crack tip delamination process by developing a delamination test inside a scanning electron microscope (SEM). However, their approach has been largely qualitative.

Transverse ply cracking was observed using a tensile jig inside an SEM (Smith et al., 1985). An attempt to take strain measurements at the crack tip did not succeed because the reference mesh was not fine enough. Another group of investigators (Hibbs and Bradley, 1987) have performed real-time mode I and mode II delamination tests inside an SEM and observed the region of the crack tip on a polished edge of the specimen. By studying the individual processes, interfacial debonding, resin deformation, microcracks, they attempted to determine the overall mechanisms of crack advancement. Those qualitative observations can be related to a change of materials or loading modes. The fracture mechanisms of ceramic matrix composites was observed within a SEM using four point bending and double cantilever beam specimens to study the location and orientation of fibre bridges across the crack (Shercliff et al., 1994). In recent years, an increasing number of authors have conducted in-situ SEM studies to observe fracture and fatigue behaviour in metals, ceramics and composites (Davidson, 1993; Sun et al., 1993; Sun et al., 1995; Kohyama and Sato, 1993).
2.2.2 Measurements in the crack tip area

Using a photoresist technique to apply a reference grid on the edge of a double torsion epoxy specimen, Mao et al. (1983) took a few COD measurements in order to evaluate $K_Ic$.

Theocaris (1988; 1990) used the scanning electron microscope (SEM) to measure the distance between two points close to the crack tip under a series of incremental loads. The displacements were then analyzed by a system of linear equations, yielding the displacement and strain field around the crack tip. The resulting strain field is calculated from only one displacement measurement: it is therefore an ideal strain field, which might be very different from the real strain field.

Using a labour intensive method, Kortshot (1988) used the movement of silver particles on the surface with respect to a loose fine metallic mesh to calculate the displacements and strains.

Also using the SEM, Bradley (1989) measured the strain field around a delamination crack tip in a composite material. A dot map was created by burning small holes in the gold-palladium coating. The specimen was then loaded in an SEM and, from the measurements of the dots displacements, the strains were calculated. The method was used to compare the strain fields in the neat resin and in the composite to understand the micromechanisms involved.

Ferguson et al. (1991) compared the results of SEM measurements with those predicted by the global approach from the applied displacement (Figure 2.13). In the SEM, the crack opening and shear displacements profiles were measured. Mode I, mode II and different mixed-mode ratios were applied. The results were compared with the values calculated from the applied loads using LEFM equations. It was found that the shape of the COD and CSD over the first 400 microns behind the crack tip was described by an $r^{1/2}$ distribution, under both mode I and mode II, as
predicted by LEFM. The magnitudes of the crack opening displacements in pure mode I were found to be lower than predicted, while the mode II crack shear displacements were generally larger. Ferguson explained these discrepancies by local effects such as fibre bridging and variations in fibre volume fraction. The addition of a mode I load to a mode II load increases the magnitude of the crack shear displacements. This effect was explained by the removal of frictional forces between the two half beams. Addition of a mode II load to a mode I load did not have a consistent effect on the COD profile.

Bannister et al. (1992) have measured the crack opening profile from scanning electron micrographs in ceramic matrix specimens under double torsion loading and with a fibre bridging zone fully developed (Figure 2.14). They found that the measured profile was in-between the profiles expected for the initiation and the propagation values of the applied stress intensity factor, meaning that the fibre bridges are restraining the opening of the crack tip faces. As can be seen in Figure 2.14, the COD's are measured at a distance from the crack tip greater than 1 mm, and are 1 mm apart from each other: this is not close enough to the crack tip to represent the local crack tip behaviour.

Davidson (1993) used a stereoimaging technique to determine the displacements and strains from scanning electron microscope photographs (Figure 2.15). The automated image processing system requires fine surface details to compute the differences due to deformations between two images taken at different load or time. The appropriate surface texture is obtained by chemical, thermal or ion etching or by deposition of particles. Minimum displacements of 0.25 μm can be measured on photographs at 4000x magnification, but the size of the area studied is then very limited (less than 20 μm). The hydraulic loading stage, designed to fit inside a scanning electron
microscope, can apply a cyclic load up to 4 Hz and temperatures up to 800°C can be applied. The system has been used to study fatigue crack closure and fatigue crack growth under mode I loading in metals and metal matrix composites.

Farqhuar et al. (1994) have measured the crack opening displacements profiles in ceramic materials under mode I loading, in order to study the influence of particle size and volume fraction (Figure 2.16). They compared these measurements with the results from a finite element computation, to determine the bridging tractions. The global applied loads and displacements were not measured and the specimen is not a standard geometry.

In summary, several methods have been developed for the quantitative study of crack tip displacements. However, not many experiments have been conducted due to the very time consuming techniques used. The resolution of the measurements is also a limiting factor. The size and geometry of the specimens is often limited by the space available in the jig. The applied load and displacement is often not measured. Finally, few experiments have been conducted on polymer matrix specimens and delamination cracks.

2.3 Literature review summary

A summary of the literature review is presented in Table 2.1. As we have seen, the macroscopic approach consists in measuring the global parameters and calculating the crack driving force using LEFM. It has been extensively used to determine the fracture toughness of materials under various conditions. It reveals that some factors have an effect on resistance to delamination: type of resin, insert, precrack, fibre bridging, friction, mixed-mode ratio. Several of these factors have an effect localized at the crack tip. Thus a study of micromechanisms is necessary.
Several studies have been conducted on qualitative in-situ observation of delamination behaviour and provided useful information, but now quantitative measurements of the crack tip behavior are needed. Some authors have presented techniques to measure local crack tip parameters such as strain fields and COD, but most have some limitations: the global load or displacement is not measured, the specimen is not standard size, the zone where the measurements are taken is too far from the crack tip or too limited in size, there are few tests or measurements, the loading is limited to mode I only, etc. Ferguson is the only one who took extensive measurements of both COD and CSD profiles on the full distance from the crack tip where the $r^{1/2}$ singularity applies, under mode I, mode II and mixed-mode loading, and measured the applied displacement as well. This allowed him to compare the global and local behaviour under different loading conditions. The present thesis builds on the technique and results developed by Ferguson (1991), by improving the testing and analysis method.
### 2.4 Tables

#### Table 2.1 Literature review summary

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2.5 Figures

Figure 2.1 Variation of Mode I and Mode II delamination fracture toughness with the neat resin toughness, for a variety of composites, from (Bradley, 1989).

Figure 2.2 Damage zone size difference in a brittle and ductile resin composite (Bradley and Cohen, 1985).
Figure 2.3 Mode II in-situ delamination of AS4/3502; Formation (a and b) and coalescence (c and d) of microcracks (Corleto et al., 1987)

(a) (b)

(c) (d)

Figure 2.4 Principal normal stresses ahead of a crack tip in the resin-rich region between plies created by the mode II loading (a), coalescence of microcracks and rotation of hackles due to shear loading (b) (Hibbs and Bradley, 1987)
Figure 2.5  Finite element stress contour plots at the crack tip in an orthotropic, elastic split laminate (Corleto et al., 1987).

Figure 2.6  Fibre bridging
Figure 2.7  Intermingling of plies during cure in a unidirectional composite (Johnson and Mangalgiri, 1987).

(a) Lay-up before curing

(b) Laminate after curing

Figure 2.8  Formation of fibre bridging by initiation of a defect in the plastic zone in the ply above the original delamination (Johnson and Mangalgiri, 1987).
Figure 2.9  Example of a R-curve for a AS4/3501 specimen (Ferguson, 1992).

Figure 2.10 Delaminated specimen with damage zone, stress-displacement damage response and delamination R-curve (Suo et al., 1992).
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Figure 2.11 Mixed-mode test configurations (Crews and Reeder, 1988)

Figure 2.12 Mixed mode delamination criterion for AS4/3501-6 (O'Brien, 1997), from (Reeder, 1994).

(a) Cracked lap shear.  (b) Edge delamination tension.  (c) Arcan.
(d) Asymmetric DCB.  (e) Mixed-mode flexure.  (f) Variable mixed-mode.
Figure 2.13 Plot of COD Vs r for an applied $G_I$ of 34.9 J/m$^2$ (Ferguson, 1992).

Figure 2.14 Crack opening displacement measured in fully bridged glass-lead composite. The solid line shows displacement calculated using the measured applied stress intensity factors at initiation ($K=0.4$) and propagation ($K=0.9$) (Bannister et al., 1992).
Figure 2.15 Crack opening displacements as a function of the distance from the crack tip. The solid line shows the square root dependence (Davidson, 1993).

Figure 2.16 Crack opening displacements in zinc sulfide containing 10% and 20% of diamond particles (Farquhar, 1994). The applied $K$ is not known.
Chapter 3 Experimental Method

In order to study the relationship between global applied conditions and local crack tip behaviour, an experimental set-up has been developed (Figure 3.1). The quantitative study of the crack tip is made possible by a loading stage designed to fit inside the chamber of Hitachi S-2300 scanning electron microscope. This permits in-situ observation of the crack tip while a load is being applied. This loading stage is also instrumented to provide measurements of the global applied loads and displacements. The combination of both local and global approaches in this experimental set-up allows us to compare them and establish a relationship.

The complete experimental system is composed of a mechanical loading jig, an electronic control system, a data acquisition and analysis system and an image acquisition and analysis system (Figure 3.2). Extensive testing of each component has been performed to ensure their proper operation.

3.1 Specimen preparation

A split laminate specimen, also called a double cantilever beam (DCB), is used for mode I, mode II and mixed mode testing (Figure 3.3). Under mode II loading, this type of specimen is also commonly called an end-notched cantilever beam (ENCB). The stage geometry is designed to accommodate standard size specimens, despite the limitations of the SEM chamber size. The specimen width \( B \) is 25.4 mm or less. The length \( L \) can be as long as 160 mm in mode I and 70 mm in mode II and mixed mode. In order for the crack tip to be visible in the scanning electron microscope, the crack length, \( a \), has to be between 5 and 50 mm. The minimum specimen thickness, \( 2h \), is 3.07 mm, corresponding to the minimum loading arms spacing. In deciding the specimen dimensions, care has to be taken that the maximum load and displacement
required will not exceed the load cell and displacement sensor limitations, which will be described later. Nevertheless, all these specifications are well within the ASTM standard guidelines and a regular size specimen can be tested.

A typical specimen includes a Teflon crack starter, embedded during manufacture, that acts as a delamination starter crack. The specimen preparation is detailed in Appendix A.

Loading blocks are adhesively bonded at the cracked end (Figure 3.3). In the case of mode II or mixed mode loadings, stiffening tabs are also bonded to the specimen in the clamping zone, to prevent the deformation of the specimen. All the tabs are bonded using a jig that ensures good alignment. AF-126 epoxy film adhesive is used and cured at 120°C for 2 hours.

Then, the specimen edge is polished using standard metallographic techniques (120, 180, 320, 600 grit); the final stage consists in an aqueous suspension of 0.06 μm alumina.

In order to measure displacements at the crack tip, reference points are needed on the specimen edge and the finer the grid of reference points, the more accurate the measurements will be. For the best combination of accuracy and simplicity of use, we have opted for the deposition of gold through a fine mesh. Copper mesh with 500, 1000 and 2000 openings per inch have been used, which give a grid spacing of roughly 50.8, 25.4 and 12.7 μm respectively. The copper mesh is positioned on the specimen edge. A jig is used to maintain a good contact between the copper mesh and the specimen edge, otherwise the gold can slip under the mesh and the resulting grid is blurred. The assembly is placed in a vacuum evaporator, where a vacuum of 5·10⁻⁵ torr is established and gold is evaporated on the specimen through the mesh. The copper mesh is removed, leaving a gold grid on the specimen edge.
For some tests performed, polysterene latex microspheres of 1 micron diameter were also
deposited on the specimen edge, to provide additional and more closely spaced reference points.
The specimen is then placed in the loading stage.

3.2 Mechanical testing

Photographs of the loading stage are presented in Figure 3.4. One of the principal goals and
design criteria was to accommodate a standard size DCB specimen, which was a challenge
considering the limited dimensions of the SEM chamber. For this reason, an aluminium
vacuum-proof box extends the size of the chamber. The platform can move in two horizontal
directions, longitudinal (X) and transverse (Y), so that the crack tip can be localized in the SEM
beam. Two knobs are used to control the X and Y movements of the platform.

The test is operated in displacement control. The displacements are applied by stepper motors
through a gear reduction system. Each of the two loading arms is controlled by a stepper motor.
Due to the space limitation, the stepper motors are placed outside the chamber and the shafts go
through a sealed lead-through. Universal joints have to be used to accommodate the platform X
and Y movements.

3.2.1 Superposition principle

Although no mixed mode tests have been conducted for this work, the design of the loading jig
allows the simultaneous application of mode I and mode II loads. This is achieved by having two
independent loading arms and using the superposition principle (Bradley and Cohen, 1985)
illustrated in Figure 3.5. Mode I loads are applied by a symmetric deflection on each arm.
Mode II loads are induced by applying a displacement to one end of a DCB specimen while the
other end is clamped (see Figure 3.5). To avoid axial loads, the clamped end has to be free to move axially, and thus, roller bearings are used (see Figure 3.5). Due to space constraints and high load bearing requirements, the number of roller bearings are limited. There are only two contact points on each side of the specimen, which is not enough to ensure a rigid clamp. To provide an effective clamping condition (i.e., no specimen deformation in the clamping zone), steel stiffening tabs are bonded on each side of the specimen.

The clamping system has been tested to check that there is no axial strain. A specimen was instrumented with two strain gauges close to the clamping point, on the tensile and compressive sides and loaded up to 100 N in mode II. The test was repeated several times and Figure 3.6 shows a typical result. The axial strain, given by $(\varepsilon_{\text{compression}} + \varepsilon_{\text{tension}})/2$, never exceeded 15 microstrains, which is very small compared to the bending strains, which are of the order of 1500 microstrains.

### 3.2.2 Calculation of the strain energy release rate

The superposition of the two loadings results in a mixed mode load. The ratio of mode I and mode II depends on the amplitude of the symmetric and asymmetric components of the deflections. Using the principle of superposition (Bradley and Cohen, 1985), we can separate the mode I and mode II contributions. We then have:

\[
P_I = \frac{(P_R + P_L)}{2}; \quad \delta_I = (\delta_R + \delta_L)
\]

\[
P_{II} = (P_R - P_L); \quad \delta_{II} = \frac{(\delta_R - \delta_L)}{2}
\]
where $P_R$ and $P_L$ are the right and left arm loads and $\delta_R$ and $\delta_L$ the right and left arm displacements and are experimentally measured. Equations (3.1) and (3.2) can then be used to calculate $P_I$, $P_{II}$, $\delta_I$ and $\delta_{II}$.

Using LEFM, the local conditions in the crack tip area can be predicted from the global parameters such as applied load or deflection, geometry, crack length and material compliance. For an orthotropic DCB specimen under mode I load or mode II load (see Figure 3.5), the expression for the strain energy release rate has been obtained using finite element analysis in conjunction with analytical considerations (Hutchinson and Suo, 1992). We call them $G_{IG}$ and $G_{IIG}$, to indicate that they are calculated from the global values:

$$G_{IG} = \frac{3P_I\delta_I}{2Ba\left(1+Y_I(\rho)\lambda^{-\frac{1}{4}}\frac{h}{a}\right)}$$

$$G_{IIG} = \frac{9P_{II}\delta_{II}a^2\left(1+Y_{II}(\rho)\lambda^{-\frac{1}{4}}\frac{h}{a}\right)^2}{2B\left(L^3 + 3a^3\left(1+Y_{II}(\rho)\lambda^{-\frac{1}{4}}\frac{h}{a}\right)\right)}$$

where

$$\lambda = \frac{a_{11}}{a_{22}}$$

$$\rho = \frac{a_{12} + a_{66}}{\sqrt{a_{11}a_{22}}}$$

$$Y_I(\rho) = 0.677 + 0.149(\rho - 1) - 0.013(\rho - 1)^2$$
$Y_{II}(\rho) = 0.206 + 0.078(\rho - 1) - 0.008(\rho - 1)^2 \quad (3.8)$

where $a_{ij}$ are the plane stress elastic compliance constants for the laminate:

$a_{11} = \frac{1}{E_1}, \quad a_{22} = \frac{1}{E_2}, \quad a_{12} = -\frac{\nu_1}{E_1}, \quad a_{66} = \frac{1}{G_{12}}$; $B$ is the specimen width and other terms are defined in Figure 3.5.

The expression for $G_{II}$ in equation (3.4) does not take into account the machine compliance $C_0$, which is independent of crack length. To obtain the total measured compliance $C$, we add the machine compliance $C_0$ to the expression of the specimen compliance obtained by Hutchinson and Suo (1992):

$$C = C_0 + \frac{L^3 + 3a^3 \left(1 + Y_{II}(\rho) \lambda^{-1/4} \frac{h}{a}\right)}{2EBh^3} \quad (3.9)$$

where $E$ is the laminate flexural modulus.

Since

$$G = \frac{P^2}{2B} \frac{dC}{da} \quad (3.10)$$

and $P = \delta/C$, we obtain

$$G_{II} = \frac{9P_{II} \delta_{II} a^2 \left(1 + Y_{II}(\rho) \lambda^{-1/4} \frac{h}{a}\right)^2}{4EB^2h^3C_0 + 2B \left(L^3 + 3a^3 \left(1 + Y_{II}(\rho) \lambda^{-1/4} \frac{h}{a}\right)\right)} \quad (3.11)$$

In mode II loading, a significant machine compliance is observed. $C_0$ can be obtained experimentally by loading a specimen in mode II, and measuring the compliance, $C$, for different crack lengths $a$ (Russell and Street, 1987). Since the geometry induces unstable mode II crack
growth, the increments in crack lengths were obtained by unloading the specimen and growing the crack in mode I, then reloading in mode II. As observed in Figure 3.7, there is indeed a linear relationship between $C$ and $L^3 + 3a^3 \left( 1 + Y_e (\rho) \lambda^{-\frac{1}{4}} \frac{h}{a} \right)$, which agrees with equation (3.9). Thus $C_0$ being equal to the $y$-intercept, we obtain a value of $4.8 \cdot 10^{-3}$ mm/N. The term $EBh^3$ is evaluated on each specimen from the measured compliance at the initial crack length using equation (3.9).

### 3.3 Crack tip faces displacements

For a cracked anisotropic material, Sih, Paris and Irwin (1965) presented equations to relate the displacements to the stress intensity factor, as well as a relationship between the strain energy release rate and the stress intensity factor, both in mode I and mode II. Thus the crack face displacements in an orthotropic material can be related to the strain energy release rate. The detailed determination of the equations is presented in Appendix B:

\[
COD = \frac{4}{\sqrt{\pi}} 2^4 \left[ \frac{2a_{12} + a_{66}}{2a_{11}} + \sqrt{ \frac{a_{22}}{a_{11}} } \right] \left( a_{11} a_{22} \right)^{\frac{1}{4}} r^{\frac{1}{2}} \sqrt{G_I} \tag{3.12}
\]

\[
CSD = \frac{4\sqrt{a_{11}}}{\sqrt{\pi}} 2^4 \left[ \frac{2a_{12} + a_{66}}{2a_{11}} + \sqrt{ \frac{a_{22}}{a_{11}} } \right] \left( a_{11} a_{22} \right)^{\frac{1}{4}} r^{\frac{1}{2}} \sqrt{G_{II}} \tag{3.13}
\]

Equations (3.12) and (3.13) have been derived considering only the first term of the elastic stress singularity, and far from the crack tip, higher order terms will become significant. Note that this first term is a function of $r^{1/2}$, whereas the stress and strain singularities are of the order of $r^{-1/2}$. 

-45-
Equations (3.12) and (3.13) are used to evaluate the local values of $G_I$ and $G_{II}$ that gives the best fit to the COD and CSD profiles measured experimentally on the SEM images. These values are called $G_{II}$ and $G_{III}$ to indicate that they are calculated from the local crack tip conditions.

### 3.4 Data acquisition and test control

During the loading, the applied loads, $P_R$ and $P_L$, and deflections, $\delta_R$ and $\delta_L$, are continuously measured with a data acquisition system. Four strain gauges are bonded on each load cell (Figure 3.8) and wired in a Wheatstone full-bridge connection. The load cells are then calibrated with weights (see Appendix C). The maximum allowable load is 890 N and the minimum measurable load is 0.5 N (accuracy 0.06% FS). The maximum distance that each loading arm can travel is 15 mm from the centre position (Figure 3.9). This gives a maximum applied displacement of 30 mm in pure mode I and 15 mm in pure mode II.

Each displacement sensor (see Figure 3.8) is a beryllium-copper cantilever beam in contact with the loading pins. Four strain gauges are bonded on the beam and wired in a Wheatstone full-bridge connection. The sensors are then calibrated with a Linear Voltage Displacement Transducer (LVDT) (see Appendix C). They are designed for a maximum displacement of 25 mm and the minimum measurable displacement is $1.5 \times 10^{-2}$ mm (accuracy 0.06% FS).

An LVDT with 50 mm range is used to measure the position along the crack direction (Figure 3.4). It is attached to the loading stage and thus records the change in the longitudinal position of the stage. At the beginning of the test, readings are taken from a ruler on the jig (see Figure 3.4) which is at a known distance from the loading point. Thus, at any moment, the distance between the loading point and the observed point is known.
During the test, the applied load and displacement as well as the longitudinal position are recorded through a data acquisition board. The stepper motors (see Figure 3.4) are controlled using a stepper motor board and controller. The data acquisition and motor control are accomplished simultaneously in one program, written using the National Instruments LabView graphical programming language (Figure 3.10).

3.5 Image acquisition

Simultaneously with the stepper motor control and the data acquisition, the image of the crack tip provided by the SEM is grabbed in the computer and overlaid with the measurements of the applied conditions (load, displacement, strain energy release rate, position from crack tip, time), using a Coreco Occulus frame grabbing board. The overlaid image is then recorded on videotape (Figure 3.11). In the loading path, small pauses are incorporated in order to ensure a stable image. After the test, the video images of interest can be digitised with the computer, and analysed.

Regularly, the loading is paused and the SEM image is recorded using a slow scan digital imaging system, PCI version 4, developed by Quartz Imaging Corporation. The images are then saved onto the computer hard disk. A magnification of 500x is usually used since it provides a good compromise between resolution and size of the viewed area. The resulting images have a significantly better resolution than the video images (1024 x 840 pixels vs. 640 x 484 pixels): for a magnification of 500x, this corresponds approximately to a viewing area of 240 x 200 μm, and one pixel is .24 μm. However, the process takes more time and memory. The slow scan images are used to obtain the best accuracy in the crack tip displacement at certain load levels. A series
of contiguous images are taken, starting well in front of the visible crack tip and extending to at least 600 μm behind, in order to provide the entire crack tip profile.

The video images are used to follow what is happening between these load levels and very close to the critical load where failure will occur very quickly. Since events unfold rapidly, there is no time to take images of the entire crack profile. Instead, we concentrate on one position behind the crack tip. For the same magnification, the viewed area is the same as for the slow scan image. However, since the image is 490 x 470 pixels, the resolution is lower: for a magnification of 500x, a pixel is 0.5 μm. Moreover, the image is noisier.

### 3.6 Image analysis

An image analysis software has been developed to measure on the image the crack opening and crack shear displacements as a function of distance behind the crack tip (Figure 3.12). The LabView graphical programming software was used together with the Concept Vi image analysis library from Graftek Inc.

The slow scan images taken at any load level are assembled in a montage using the Ulead PhotoImpact stitching capability (Figure 3.13).

The COD cannot be measured directly from the crack faces because the crack faces are often obscured. Thus the increase in distance between gold squares on each side of the crack is monitored instead. The vertical distance between the centroids of two squares on the zero load image is measured and subtracted from the distance between the same squares under load (Figure 3.14). The same technique is used to measure the CSD, with the horizontal distance being used.
The montage is rotated to make sure that the crack is horizontal and cropped to keep only the area of interest. Then the image is thresholded in order to select the squares. Some processing steps are then taken to filter out some small features, leaving only the squares of interest. The centroids of every square is then calculated. The effect of thresholding, processing and calculating the centroids is presented in Figure 3.15. A printout of the screen showing the image analysis software during the thresholding operation is shown in Figure 3.16.

The calibration of the image is done by using the distance between 2 centroids which are at least 10 grid spaces apart. A printout of the screen during the calibration operation is shown in Figure 3.17. For the early tests presented in this work, analyzed before the automatic technique for determining the centroids of the square was developed, the calibration was done using the line overlaid on the image by the SEM. It was found that in some instances this line was inaccurate by up to 5% and the results were recalibrated where necessary, using the distance between centroids.

Once the calibration is done, the actual measurements can be taken. Using the mouse, 2 squares on each side of the crack can be selected and the horizontal and vertical distance between their centroids is calculated and recorded. A printout of the screen showing the selection of the centroids is shown in Figure 3.18. The measurement can be repeated for other pairs of squares at a different location along the crack. Errors in the measurements are of the order of one pixel, which, for a magnification of 500x, represents 0.24 μm.

Close to the crack tip, where the strains are high, taking measurements away from the crack faces introduces errors. This error is bigger in mode II since the horizontal displacements close to the crack face are large, while in mode I the vertical displacements close to the crack face are
smaller. Thus, the measurements have to be taken as close as possible to the crack faces, and in an area very close to the crack tip \((r \leq 100 \, \mu m)\), surface features such as dust particles or deposited microspheres are used as reference points, rather than the squares. This requires finding such features and manually selecting them on the unloaded and loaded images: it is very time consuming. This method was used for the first few tests performed. Another solution was therefore adopted: a mesh with increased openings per inch is used to obtain a smaller distance between the squares, which reduces the error. This involves technical difficulties during the grid deposition: since the grid is finer and thinner, it is very fragile and can be easily torn or wrinkled; careful manipulation and improvement to the gold deposition jig were necessary to obtain a good grid definition. The finer available mesh, 2000 opening per inch, resulting in square spacing of 12.7 microns, is used for the later tests. The squares are then close enough to the crack faces that the material displacements are negligible compared to the crack face displacements.

3.7 Summary

An experimental method for measuring crack opening and shear displacement has been developed. An instrumented loading jig designed to fit inside a SEM was built. The specimen is a standard size DCB specimen. Mode I, mode II and mixed-mode load can be applied. The applied loads and displacements are measured. Using LEFM, the COD and CSD profiles can be calculated from those global values.

A gold grid deposited on the specimen edges provides reference points for measuring the crack tip displacements. On the SEM images, the COD and CSD profiles are measured using a semi-
automatic image analysis method. These measurements can then be compared with the LEFM analytical predictions.
3.8 Figures

Figure 3.1  Photograph of the complete experimental system.

Figure 3.2  Schematic of the experimental system
Figure 3.3  Configuration of the specimens used for (a) mode I and (b) mode II and mixed mode.

Figure 3.4  Photograph of the loading jig
Figure 3.5 Superposition of mode I and mode II on a DCB specimen.

Figure 3.6 Results from a strain gauged specimen loaded under mode II. The lines show the compressive and tensile bending strains while the markers show the axial strains.
Figure 3.7 Relationship between specimen compliance and crack length for determination of machine compliance $C_0$.

Figure 3.8 Photograph of the load cells and displacement sensors.
Figure 3.9  Schematic of the loading arms maximum displacements.
Figure 3.10 Algorithm for the Labview control and data acquisition program.
Figure 3.11 Example of a video image overlaid with data.
Figure 3.12 Algorithm showing the usual sequence of processing and analysis steps in the Labview image analysis program.
Figure 3.13 Example of a montage of slow scan images.
Figure 3.14 Measurement of the COD and CSD by the difference in distance between the centroid co-ordinates from the loaded to unloaded image.
Figure 3.15 Image analysis technique: (a) original image, (b) thresholded image, (c) processed image, (d) image with centroids.
Figure 3.16 Printout of the screen showing the image analysis software during the thresholding of the image.

Figure 3.17 Printout of the screen showing the image analysis software during the calibration of the image.
Figure 3.18 Printout of the screen showing the image analysis software during the measurements of the COD.
Chapter 4 Mode I Results

The objectives of the mode I tests are:

- to observe the expected square root singularity in the COD profiles close to the crack tip;
- to compare $G_{IL}$ to $G_{IG}$, for starter film and propagated crack tips, for brittle and tough materials, and to propose an explanation for any discrepancies;
- to compare the measured CODs with the results of a finite element model, along the entire length of the crack
- to study the local crack tip behaviour when there is an increased resistance with crack growth and fibre bridging; in particular, measure $G_{IL}$ for increasing crack lengths and compare it to $G_{IG}$: if the fibre bridging acts by shielding the crack tip elastically, $G_{IL}$ will be constant with crack length, or if its effect is to increase the resistance to crack growth by dissipating energy during fibre pullout, $G_{IL}$ and $G_{IG}$ will be equal, and increasing.

4.1 Verification of global/local agreement in a brittle material

The purpose of this series of tests is to measure the local crack tip displacement field and global applied parameters, during mode I loading, up to and including failure, for a given crack length. The SEM images of a delamination crack tip under different mode I load levels are analyzed to obtain the COD profiles and $G_{IL}$ is determined. By using a brittle material and very short precracks with no fibre bridging, a good agreement between $G_{IL}$ and $G_{IG}$ is expected.

4.1.1 Test description

Results are presented for the mode I testing of a DCB specimen of a 24-layer unidirectional AS4/3501-6 CFRP laminate, identified as specimen B1. The COD profiles have been measured
for 10 $G_{IG}$ levels covering 3 crack lengths (insert and small crack growths). The material properties used in the analytical calculations are presented in Table 4.1. The determination of these material properties is presented in Appendix D.

The loading path is described in Figure 4.1. The specimen with the initial Teflon starter crack was loaded, until a small amount of crack growth occurred. Thus, the crack was grown by 600 µm in order to create a sharp crack tip without developing fibre bridging. The initial crack length (Teflon insert) is denoted by $a_1$, while the second crack length is called $a_2$. The load was then removed to take images of the unloaded crack $a_2$. Then the crack was loaded again, until crack growth occurred, resulting in a crack length $a_3$. Images were taken at crack lengths $a_1$, $a_2$ and $a_3$, to compare the behaviour of a Teflon film crack tip and sharp crack tips, and during both unloading and loading phases, to study the effect of loading direction and history on the crack tip behaviour.

Slow scan images from the SEM were recorded at the following $G_{IG}$ levels, in chronological order: 0, 30, 72, 0 and 69 J/m$^2$ for crack $a_1$, 92, 40, 0, 35 and 72 J/m$^2$ for crack $a_2$ and 38, 81, 41 and 0 J/m$^2$ for crack $a_3$. After the test, these images were assembled in a montage: a typical image is shown in Figure 4.2. Some of the profile montages are presented in larger size in Appendix F. From these images, the COD versus $r$ profiles were obtained as described previously.

4.1.2 Results

COD profiles were generated for ten load levels covering 3 crack lengths (Figure 4.3 to Figure 4.12). Then, equations 3.12 and 3.13 are used to calculate $G_{IL}$; if it is equal to $G_{IG}$, the calculated
profile is plotted as a solid line, otherwise, it is plotted as a dashed line. It should be noticed that in all cases, no measurable CSDs were observed, thus only pure mode I is present at the crack tip. The load-displacement curves are presented in Appendix E and they are linear.

4.1.2.1 Square root profile: shape and magnitude

Presented in Figure 4.13 is the plot of the $COD^2$ vs. $r$ for an applied $G_{IG}$ of 69 J/m$^2$, demonstrating that the $COD$ varies linearly with the square root of $r$. All the other nine load levels give the same linear relationship between $COD^2$ and $r$: all the plots are presented in Appendix E. The data starts deviating from the predicted square root profile at approximately 500 μm behind the crack tip. Beyond this, the higher order terms begin to dominate the profile. This is in agreement with the observations of Ferguson (1992) and Poursartip, Gambone and Fernlund (1996).

Moreover, for Figure 4.4 to Figure 4.12, $G_{IL}$ equals $G_{IG}$. Therefore, for this brittle material and no fibre bridging, all of the applied load is transmitted to the crack tip and there is no local effect not accounted for by the analytical equations. It also confirms that this technique works very well.

4.1.2.2 Effect of insert

In Figure 4.3, $G_{IL}$ is roughly half of $G_{IG}$. This is the very first loading of the specimen and $G_{IG}$ is still low (30 J/m$^2$). Therefore, we believe that the starter film is still sticking to the crack faces, preventing them from opening freely. When the load is increased to 72 J/m$^2$ (Figure 4.4), the sticking force is overcome and $G_{IL}$ equals $G_{IG}$.
Chapter 4 Mode I Results

However, once a higher load has already been applied, the sticking behaviour is definitively removed, as shown by Figure 4.8 for example. Observing the B1 a1 images, we can see that, at the insert fibre bundle, some transverse insert fibres are interlocked with the epoxy matrix (Figure 4.14). When the load is increased, they are freed. This effect is even more visible on Figure E.22 (Appendix E).

Apart from this initial low load sticking, the insert seems to have no effect on the transmission of the applied loads to the crack tip. There is the same good global/local behaviour agreement for the a1, a2 and a3 sets of results, therefore the insert behaves similarly as a sharp precrack. This agrees with what has been reported in the literature: in mode I, for insert thicknesses greater than 13 microns, the toughness increases with insert thickness, and this is normally associated with the crack tip being more blunt. However, below 13 microns, the toughness stabilizes and is the same as the toughness obtained from a small precrack. Moreover, Ferguson (1992) measured the local COD profiles for specimens where no crack growth had occurred and he found that they were better fit by a linear relationship with $r$ than with $r^{1/2}$. He explained this result by the fact that the insert tip acts as a blunt crack tip and therefore the typical stress singularity is not formed in front of the crack tip. The Teflon insert used by Ferguson was 25 microns thick, which is greater than the 13 microns limit above which the film tip bluntness affects the fracture toughness. The Teflon insert used in this work is 13 microns thick at the tip and therefore the COD profiles follow a $r^{1/2}$ relation and there is good global/local agreement.

4.1.2.3 Effect of decreasing and increasing loads

The results for 40 J/m² (decreasing load, Figure 4.7) and 35 J/m² (increasing load, Figure 4.8) for a2 and for 38 J/m² (increasing load, Figure 4.10) and 41 J/m² (decreasing load, Figure 4.12) for a3
are similar and consistent: there is good agreement both in terms of shape and magnitude between the LEFM predictions and the local measurements. Therefore, the loading direction seems to have no effect on the crack tip behaviour.

4.1.2.4 Behaviour at failure

After a $G_{IC}$ level of 72 J/m$^2$ for crack a$_2$, the applied displacement was increased until crack growth occurred, and the SEM images were recorded on videotape. Because events unfold rapidly, there is no time to monitor the same length of crack, so we focused on an area 430 µm behind the crack tip. The measured CODs at $r = 430$ µm for $G_{IC}$ levels of 109 and 126 J/m$^2$ are plotted in Figure 4.15 and show good agreement with the global predicted magnitudes. Unstable crack growth occurred just after the image at 126 J/m$^2$ was taken. An alternative view is presented in Figure 4.16, which shows the variation of the COD at $r = 430$ µm with increasing applied displacement. As can be seen, the COD increases linearly with the applied displacement, which indicates linear elastic behaviour right up to failure.

4.1.3 Comparison between global and local behaviour

Figure 4.17 presents the relation between $G_{IL}$ and $G_{IC}$ for all loading cases and crack lengths of specimen B1, which summarizes the findings described previously. As we can see, there is a one to one relation between $G_{IL}$ and $G_{IC}$ up to failure. The only exception is for the very first loading, where the insert is believed to stick to the composite, restraining the opening of the crack.
4.1.4 Comparison with FEM model

Because the analytical prediction is only valid very close to the crack tip, a finite element analysis was performed in order to compare the measured crack opening displacements with the FEM predictions along the full length of the crack.

The DCB specimen was modeled using the finite element code ANSYS® (version 5.3). A static linear analysis using 6 noded triangular plane stress elements was performed. Due to the stress singularity at the crack tip, the mesh was refined at the crack tip. Figure 4.18 shows the finite element mesh of the upper half of the specimen. The edge length of the elements at the crack tip is 2 μm and the model consists of 1342 elements in total. To model the singularity at the crack tip, the elements around the crack tip had their midside node displaced to the quarter point. This ensures a stress singularity of the form $1/\sqrt{r}$, in accordance with linear elastic fracture mechanics. Due to the symmetric nature of mode I loading, a symmetry boundary condition was imposed at the midplane of the mesh and the experimentally measured load was applied as two point loads. The model was also run with an applied displacement equal to the experimentally measured one. The material properties are the same as the ones used for the analytical curves (see Table 4.1).

The resulting displacements, $U_y$, at the crack face were used to calculate the CODs as a function of $r$. They were then compared to the CODs measured on specimen B1 a3 for the same applied load. For the purpose of this comparison, the CODs were measured in a zone extending far from the crack tip, until there was no more grid. The resulting curves are presented in Figure 4.19 for the complete crack and in Figure 4.20 for the crack tip area. We can see that the measured COD are between the COD obtained from the FEM model for the cases of applied load and applied
displacement. The difference between the FEM is probably due to some inaccuracies in the material properties. Nevertheless, they are very close.

Figure 4.20 includes the analytical LEFM COD curve, which is valid only close to the crack tip since higher order terms are neglected. As seen, the FEM and analytical curves are very close below 300 microns and start to diverge noticeably at around 400 to 500 microns, which is an indication of the size of the singular zone. This confirms the results from the comparison between experimental measurements and analytical curve mentioned previously, where a singular zone of roughly 500 microns was observed.

The model was also run for plane strain conditions, for both the applied load and applied displacement cases. The difference in CODs with plane stress conditions is less than 0.3%, and therefore, negligible. At the edge of the specimen, where we take our COD measurements, plane stress prevails, while the center of the specimen is in plane strain. Since the FEM results show that there is almost no difference in the CODs between plane stress and plane strain conditions, we conclude that the CODs measured on the edge of the specimen are representative of the CODs experienced at the center of the specimen.

4.2 Behaviour of a tougher material

A tougher material was tested in order to study the relationship between the increase in toughness and the local crack tip behaviour. Bradley (1989) has explained the increase in toughness in some materials by observing the failure micromechanisms. He observed more microcracking in front of the crack tip in tougher materials. This larger damage zone would yield more load redistribution in front of the crack tip and thus a lower strain at the crack tip. Therefore, a higher
load can be applied before failure occurs. If there is such a load redistribution, the COD profile behind the crack tip would be affected and would not agree with the LEFM prediction.

4.2.1 Test description

A IM7/8551-7 unidirectional CFRP laminate, called T4, was tested. This resin is a toughened epoxy, with rubber particles at the interfaces between plies. The material properties used in the analytical calculations are presented in Table 4.1 and their determination is described in Appendix D. The loading path is described in Figure 4.21. In this test, no precrack was created, so the crack tip is directly at the end of the Teflon. Slow scan images were recorded at the following $G_{IC}$ levels: 92, 175, 257 and 340 $J/m^2$.

4.2.2 Results

From these images, the COD profiles were obtained as described previously. They are presented in Figure 4.23 to Figure 4.26. The load-displacement curves are presented in Appendix E and they are linear.

As we can see, the shape of the COD profiles follows a square root dependency. This is better illustrated by Figure 4.27, where the square of the COD is plotted as a function of $r$ for the lowest and highest load levels: there is indeed a linear relationship between $COD^2$ and $r$.

However, we can see that $G_{IL}$ is slightly lower than $G_{IC}$ (Figure 4.28). This difference is more pronounced for the lower loads, and this might be linked to the sticking behaviour of the insert during initial loading mentioned in the previous section. Also, the elastic properties of this material are not as well established as the ones for AS4/3501, which could mean some error in the calculations of $G_{IC}$ and $G_{IL}$.
Chapter 4 Mode I Results

The only other explanation for this slightly lower magnitude of the COD profiles is the development of a damage zone in front of the crack tip. In Figure 4.29, we can see that, above 257 J/m², some small microcracks develop in front of the crack tip prior to the main crack growth. This was not observed on the brittle specimen B1. However, even at 340 J/m², those microcracks are still small and limited. They translate on the COD profiles (Figure 4.25 and Figure 4.26) by small COD measurements in front of the crack tip. The fact that the extent of this microcracking is small can be linked to the small reduction in the magnitude of the COD profiles.

However, microcracking and reduction in COD magnitude are not enough to explain the high toughness of this material, which is almost 4 times the toughness of AS4/3501: the effect on the COD profiles should be much bigger. For example, at 340 J/m², the COD profile corresponds to a local $G_I$ of 290 J/m², which is still much higher than the toughness of a brittle material such as AS4/3501. Therefore, we must conclude that the high toughness of this material is due for the most part to the capacity of the resin to sustain higher strains, rather than crack tip blunting due to microcracking.

4.3 Mode I resistance curve

The objective of this test is to study the relationship between the increase in $G_{IC}$ due to crack growth and the local crack tip behaviour. More precisely, we would like to see if the increase in $G_{IC}$ is due to a reduction in the crack driving force or to an increase in the energy consumed in crack propagation, which are the left hand side and right hand side terms in the following condition for crack growth equation:
Chapter 4 Mode I Results

\[ G_{IL} = G_{ICL} \]  

(4.1)

where \( G_{IL} \) is the local strain energy release rate obtained from the COD profile and \( G_{ICL} \) the local critical strain energy release rate, both for the actual, fibre bridged system.

Globally, we also have, at failure:

\[ G_{IG} = G_{ICG} \]  

(4.2)

where \( G_{IG} \) is the global strain energy release rate and \( G_{ICG} \) the value of \( G_{IG} \) at failure.

In the first case, when the fibre bridging reduces the crack driving force, we get from the superposition of displacements principle (see Chapter 1, equation 1.8):

\[ \sqrt{G_{IL}} = \sqrt{G_{IG}} - \sqrt{G_{IB}} \]  

(4.3)

where \( G_{IG} \) is the global strain energy release rate and \( G_{IB} \) is the strain energy release rate due to fibre bridging. This stress approach implies an elastic behaviour of the fibre bridges.

In the second case, when the energy consumed in crack propagation increases, we obtain from an energy balance,

\[ G_{ICL} = G_0 + \Gamma_B \]  

(4.4)

where \( G_0 \) is the strain energy release rate for the initial crack with no bridging and \( \Gamma_B \) is the energy consumed by fibre bridging breakage and pullout. This energy approach corresponds to a dissipative behaviour of the fibre bridges.

If the fibre bridging behaviour is purely elastic, \( \Gamma_B \) is zero and the criterion for crack growth becomes:
Chapter 4 Mode I Results

\[ G_{IL} = G_{IcL} = G_0 \]  

(4.5)

and \( G_{IcL} \) is constant with crack growth, which means that the COD profile at failure is the same, whatever the crack length. This behaviour is represented by a dashed line in Figure 4.30.

When the fibre bridging only dissipates energy, \( G_{IB} \) is zero and the criterion for crack growth becomes:

\[ G_{IL} = G_{IcL} = G_{IG} \]  

(4.6)

which is represented by the solid line in Figure 4.30.

A combination of both is possible, and in that case the failure points will fall in between the two bounding cases.

4.3.1 Test description

It has been shown (Russell, 1986) that a specimen with a higher volume fraction will have a greater increase in resistance to fracture with crack growth because, as the plies are closer together, more fibre bridging occurs. Several methods yield a higher volume fraction material, but the simplest way is to bleed out more resin during the cure by using more bleeder\(^1\) layers (Russell, 1986). Therefore an AS4-3501/6 specimen with 26 plies manufactured using 15 bleeder plies was used here. It is identified as R1.

The specimen was loaded in mode I until crack growth was observed in the SEM, then immediately partially unloaded. This process was repeated until the crack growth had reached 47 mm. For 5 crack lengths, called \( c_1 \) to \( c_5 \), slow scan images are recorded, at \( G_{IG} \) levels \( L_1 \)

\(^1\) Layer of absorbing material placed around the laminate to absorb any excess resin during processing.
Chapter 4  Mode I Results

(≈80 J/m²), L₂ (≈260 J/m²) and L₃ (≈400 J/m²), when possible (see Table 4.3). After the test, the COD profiles are measured from the slow scan images.

With the superposition principle (see Figure 1.5), \( G_{IG} \) is the global energy release rate calculated for the unbridged case \( (P_i, \delta_{IU}) \) rather than the actual measured \( (P_i, \delta_i) \). Since we do not have \( \delta_{IU} \), we have calculated \( G_{IG} \) for this test using \( P_i \) only (Hutchinson and Suo, 1991), instead of equation (3.3):

\[
G_{IG} = \frac{12P_i^2a^2(1+Y_i\rho a^{-1/4})^2}{B^2Eh^3}
\]

(4.7)

4.3.2 Results

The R-curve obtained is presented in Figure 4.32. It can be seen that there is a clear increase of \( G_{IG} \) with crack growth: it is multiplied by almost 6. The toughness increases very steeply at first, then less and less steeply until it reaches a plateau. The load-displacement curve, typical of mode I propagation tests, is presented in Appendix E.

The COD profiles for crack lengths \( c_1 \) to \( c_5 \) are presented in Figure 4.33 to Figure 4.37. Each series of dots corresponds to one level of \( G_{IG} \) applied. When there was good agreement between \( G_{IL} \) and \( G_{IG} \), the COD profile predicted analytically is plotted as a solid line. Otherwise, the dotted lines represent the same equation, but the value of \( G_{IL} \) is adjusted to obtain a better fit to the COD values. Table 4.3 shows the values of \( G_{IL} \) for the different crack lengths and applied, or global, \( G_{IG} \) levels.
Chapter 4 Mode I Results

For the crack length $c_1$ (Figure 4.33), which is directly at the Teflon tip, the $COD$ profile is in good agreement with the analytical curve for $G_{IL} = G_{IG} = 79 \ J/m^2$, both in terms of shape and magnitude. All the load is transmitted to the crack tip, since there is no fibre bridging.

For $c_2$ and $c_3$ (Figure 4.34 and Figure 4.35), we can see that at $L_1$, $G_{IL}$ is close to $G_{IG}$. Thus, up to $L_1$, all the applied load is transmitted to the crack tip. However, as $G_{IG}$ is increased to $L_2$ and $L_3$, we have $G_{IL} < G_{IG}$. This means that fibre bridging is restraining the crack tip opening.

For $c_4$ and $c_5$, the reduction is even greater. First, at $L_1$, there is already a reduction in $G_{IL}$. As $G_{IG}$ increases to $L_2$ and $L_3$, $G_{IL}$ is barely increasing.

For $c_2$, $c_4$ and $c_5$, some fibre bridging is observable directly on the images. A close-up of these fibre bridges has been included on Figure 4.34, Figure 4.36 and Figure 4.37, with an arrow indicating the position of the fibre bridge. The $COD$ profiles invariably show an inflection point right at the position of the fibre bridge: behind it, the $COD$ profiles open up.

4.3.3 Behaviour close to failure

The results for specimen B1 (no bridging) and for the 5 crack lengths of specimen R1 are presented as a plot of $G_{IL}$ vs. $G_{IG}$ in Figure 4.38. $G_{ICL}$ is obtained from video images (failure points, hollow markers) and therefore have less precision, thus error bars showing the standard deviations are included. Since events unfold rapidly at failure, the video images concentrate on one position behind the crack tip of the crack and we do not see if the crack is growing at the crack tip. Therefore, it is difficult to determine the exact moment of failure: the $COD$ might be measured on an image where the crack is already growing, and therefore is overestimated. In the present case, we know that this is more likely for
the short crack cases (c2 and c3) where the events occurred more rapidly. Especially, $G_{ILc}$ for $c_2$ might be higher than the real value.

As we can see on Figure 4.38, for $c_3$, $c_4$ and $c_5$, those last points seem to continue the almost linear trend set by the $G_{IL}/G_{IG}$ relationship obtained for lower loads. Moreover, as the crack length increases, the curve is moved downward: the longer the crack length, the lower the $G_{IL}$ at which there is deviation from a $G_{IL}=G_{IG}$ relation and the closer $G_{IL}$ is to $G_0$. Therefore, for longer crack length, failure points are similar to the elastic fibre bridge behaviour illustrated in Figure 4.30 and the dominant effect is the elastic shielding from the fibre bridges.

4.3.4 Specimens with smaller increase in toughness with crack growth

Two AS4/3501 specimens similar to R1, but with less volume fraction, and therefore less increase in toughness with crack growth, were also tested (see Appendix E). The resistance curve increase was much less than with the R1 specimen. A difference between $G_{IG}$ and $G_{IL}$ was also noticed, but only for the longest crack length on one specimen. This is not surprising, since there is less increase in toughness and therefore less fibre bridging and the applied $G_{IG}$ levels are not very high. Thus the reduction in $G_{IL}$ is less noticeable.

4.4 Summary

Mode I tests were conducted on an AS4/3501-6 unidirectional material, and, as expected from LEFM, the crack tip CODs exhibited a square root shape, for roughly 500 μm behind the crack tip. Good agreement was obtained between $G_{IL}$ and $G_{IG}$. The only exception is for the first loading from the insert, at low loads, where $G_{IL}$ is smaller than expected because the insert is sticking to the crack faces. For this material, the behaviour remains linear elastic up to fracture.
Also, the COD profiles agreed well with the results from a finite element model along the entire crack length.

A specimen with a higher fracture toughness was also studied. The $G_{II}$ is slightly lower than the $G_{IG}$. This result can be linked to the fact that some microcracking develops in front of the crack tip, redistributing the loads in front of the crack tip. However, it is not enough to account for the significantly high toughness of this material, which might simply be due to the capacity of the resin to undergo higher strains.

The increase in resistance with crack growth was also studied and correlated with the local crack tip behaviour. Up to an applied $G_{IG}$ around 80 J/m$^2$, $G_{IG}$ and $G_{II}$ agree well, for small amounts of crack growth. When $G_{IG}$ is further increased, $G_{II}$ does not increase as much, and for longer crack growth, it barely increases. This confirms that fibre bridges act by keeping the crack closed. Thus the global applied loads are deviated from the crack tip and a higher load can be applied before crack growth occurs. The longer the crack growth, the closer the local $G_{ICL}$ is to the initiation toughness $G_0$ and, therefore, the more the effect of fibre bridging is due to elastic behaviour rather than dissipative behaviour.
4.5 Tables

Table 4.1 Material properties of AS4/3501-6 and IM7/8551-7 unidirectional laminate

<table>
<thead>
<tr>
<th>Properties</th>
<th>AS4/3501-6</th>
<th>IM7/8551-7</th>
</tr>
</thead>
<tbody>
<tr>
<td>$E_1$</td>
<td>102 GPa</td>
<td>130 GPa</td>
</tr>
<tr>
<td>$E_2$</td>
<td>9 GPa</td>
<td>8.3 GPa</td>
</tr>
<tr>
<td>$\nu_{12}$</td>
<td>0.3</td>
<td>0.34</td>
</tr>
<tr>
<td>$G_{12}$</td>
<td>7.1 GPa</td>
<td>4.85 GPa</td>
</tr>
</tbody>
</table>

Table 4.2 Characteristics of specimens B1, T4 and R1

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Specimen B1</th>
<th>Specimen T4</th>
<th>Specimen R1</th>
</tr>
</thead>
<tbody>
<tr>
<td>material</td>
<td>AS4/3501-6</td>
<td>IM7/8551-7</td>
<td>AS4/3501-6</td>
</tr>
<tr>
<td>$V_f$ (%)</td>
<td>59</td>
<td>61</td>
<td>67</td>
</tr>
<tr>
<td>grid spacing (μm)</td>
<td>50.8</td>
<td>50.8</td>
<td>25.4</td>
</tr>
<tr>
<td>$h$ (mm)</td>
<td>1.77</td>
<td>2.08</td>
<td>1.71</td>
</tr>
<tr>
<td>$B$ (mm)</td>
<td>19.36</td>
<td>22.95</td>
<td>18.86</td>
</tr>
<tr>
<td>$L$ (mm)</td>
<td>145.5</td>
<td>137.5</td>
<td>131.5</td>
</tr>
<tr>
<td>$a$ (mm)</td>
<td>18.8 ($a_1$)</td>
<td>19.4 ($a_2$)</td>
<td>21.7</td>
</tr>
<tr>
<td></td>
<td>21.4 ($a_3$)</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Table 4.3 $G_{IL}$ (J/m$^2$) for the five crack lengths and the three levels of $G_{IG}$ (J/m$^2$).

<table>
<thead>
<tr>
<th>Crack length (mm)</th>
<th>$G_{IG}$ (J/m$^2$)</th>
<th>$G_{IG} \approx L_1$</th>
<th>$G_{IG} \approx L_2$</th>
<th>$G_{IG} \approx L_3$</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$G_{IG}$</td>
<td>$G_{IL}$</td>
<td>$G_{IG}$</td>
<td>$G_{IL}$</td>
</tr>
<tr>
<td>19.5 ($c_1$)</td>
<td>105</td>
<td>79</td>
<td>80</td>
<td>250</td>
</tr>
<tr>
<td>22.2 ($c_2$)</td>
<td>333</td>
<td>82</td>
<td>70</td>
<td>252</td>
</tr>
<tr>
<td>26.7 ($c_3$)</td>
<td>458</td>
<td>85</td>
<td>80</td>
<td>252</td>
</tr>
<tr>
<td>35.2 ($c_4$)</td>
<td>556</td>
<td>94</td>
<td>70</td>
<td>263</td>
</tr>
<tr>
<td>47.0 ($c_5$)</td>
<td>637</td>
<td>99</td>
<td>55</td>
<td>277</td>
</tr>
</tbody>
</table>
4.6 Figures

Figure 4.1 Description of specimen B1 loading path for the tests presented in Figure 4.3 to Figure 4.20. The horizontal steps show where the loading was held constant to perform slow scans in the SEM.

Figure 4.2 Montage of the SEM crack tip images for specimen B1 (crack a2), for $G_{IC} = 72 \text{ J/m}^2$. 

specimen load 

SPECIMEN B1 LOADING

- a1 
- a2 
- a3 

crack growth $G_{IC} = 96 \text{ J/m}^2$

$G_{IC} = 92 \text{ J/m}^2$

$G_{IC} = 69 \text{ J/m}^2$

$G_{IC} = 41 \text{ J/m}^2$

$G_{IC} = 38 \text{ J/m}^2$

$G_{IC} = 41 \text{ J/m}^2$

$G_{IC} = 35 \text{ J/m}^2$

$G_{IC} = 41 \text{ J/m}^2$

$G_{IC} = 72 \text{ J/m}^2$

$G_{IC} = 30 \text{ J/m}^2$

$0.6 \text{ mm/min}$
Chapter 4 Mode I Results

Figure 4.3 Plot of COD vs. $r$ (distance from the crack tip) for $G_{ig} = 30 \text{ J/m}^2$ on specimen B1 (a1). Solid line shows COD profile calculated from $G_{il} = G_{ig}$. Dashed line shows COD profile calculated from $G_{il}$ that gives the best fit.

Figure 4.4 Plot of COD vs. $r$ (distance from the crack tip) for $G_{ig} = 72 \text{ J/m}^2$ on specimen B1 (a1). Solid line shows COD profile calculated from $G_{il} = G_{ig}$. 
Figure 4.5  Plot of COD vs. $r$ (distance from the crack tip) for $G_{IG} = 69$ J/m$^2$ on specimen B1 (a$_1$). Solid line shows COD profile calculated from $G_{IL} = G_{IG}$.

Figure 4.6  Plot of COD vs. $r$ (distance from the crack tip) for $G_{IG} = 92$ J/m$^2$ on specimen B1 (a$_2$). Solid line shows COD profile calculated from $G_{IL} = G_{IG}$. 
Figure 4.7 Plot of COD vs. $r$ (distance from the crack tip) for $G_{IG} = 41 \text{ J/m}^2$ on specimen B1 ($a_2$). Solid line shows COD profile calculated from $G_{IL} = G_{IG}$.

Figure 4.8 Plot of COD vs. $r$ (distance from the crack tip) for $G_{IG} = 35 \text{ J/m}^2$ on specimen B1 ($a_2$). Solid line shows COD profile calculated from $G_{IL} = G_{IG}$.
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Figure 4.9  Plot of COD vs. r (distance from the crack tip) for $G_{IG} = 74 \text{ J/m}^2$ on specimen B1 (a2). Solid line shows COD profile calculated from $G_{IL} = G_{IG}$.

Figure 4.10  Plot of COD vs. r (distance from the crack tip) for $G_{IG} = 38 \text{ J/m}^2$ on specimen B1 (a3). Solid line shows COD profile calculated from $G_{IL} = G_{IG}$.
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Figure 4.11 Plot of COD vs. \(r\) (distance from the crack tip) for \(G_{IG} = 81 \text{ J/m}^2\) on specimen B1 (a3). Solid line shows COD profile calculated from \(G_{IL} = G_{IG}\).

Figure 4.12 Plot of COD vs. \(r\) (distance from the crack tip) for \(G_{IG} = 41 \text{ J/m}^2\) on specimen B1 (a3). Solid line shows COD profile calculated from \(G_{IL} = G_{IG}\).
Figure 4.13 Plot of $COD^2$ vs. $r$ for $G_{IC} = 69$ J/m$^2$ on specimen B1 (a1) showing the square root dependence over the first 500 microns.
Figure 4.14 Pictures showing the interlocking of the Teflon insert fibre bundle and the composite matrix, for the 2 insert fibre bundles closest to the crack tip.
Chapter 4 Mode I Results

Figure 4.15 Plot of COD vs. $r$ (distance from the crack tip) for $G_{IG}$ values of 74, 109 and 126 J/m$^2$ on specimen B1 (a2). Solid lines show COD profile calculated from $G_{IL} = G_{IG}$.

Figure 4.16 Plot of COD versus applied $\delta_f$ at $r = 430 \mu$m for specimen B1 (a2).
Figure 4.17 Plot of $G_{IL}$ vs. $G_{IG}$ for specimen B1, showing that $G_{IL}=G_{IG}$ for all cases but one. The exception occurs for the first loading from the insert and might be explained by the insert sticking effect.
Figure 4.18 Finite element mesh of the mode I DCB specimen, showing the upper half of the specimen, which is symmetric, and 2 successive enlargement of the crack tip area.
Figure 4.19 Comparison between COD profile obtained experimentally on specimen B1 (a₃) and by finite element method, for applied load and applied displacement conditions.

Figure 4.20 Comparison between COD profile obtained experimentally on specimen B1 (a₃) and by finite element method, in the region close to the crack tip.
Figure 4.21 Description of specimen T4 dimensions and loading path of tests presented in Figure 4.23 to Figure 4.27. The horizontal steps show where the loading was held constant to perform slow scans in the SEM.

Figure 4.22 Montage of the SEM crack tip images for specimen T4, for $G_{IC} = 92$ J/m$^2$. 
Figure 4.23 Plot of COD vs. r (distance from the crack tip) for $G_{IG} = 92 \text{ J/m}^2$ on specimen T4. Solid line shows COD profile calculated from $G_{IL} = G_{IG}$. Dashed line shows COD profile calculated from $G_{IL}$ that gives the best fit.

Figure 4.24 Plot of COD vs. r (distance from the crack tip) for $G_{IG} = 175 \text{ J/m}^2$ on specimen T4. Solid line shows COD profile calculated from $G_{IL} = G_{IG}$. Dashed line shows COD profile calculated from $G_{IL}$ that gives the best fit.
Figure 4.25  Plot of COD vs. $r$ (distance from the crack tip) for $G_{IG} = 257$ J/m$^2$ on specimen T4. Solid line shows COD profile calculated from $G_{IL} = G_{IG}$. Dashed line shows COD profile calculated from $G_{IL}$ that gives the best fit.

Figure 4.26  Plot of COD vs. $r$ (distance from the crack tip) for $G_{IG} = 340$ J/m$^2$ on specimen T4. Solid line shows COD profile calculated from $G_{IL} = G_{IG}$. Dashed line shows COD profile calculated from $G_{IL}$ that gives the best fit.
Figure 4.27  Plot of $COD^2$ vs. $r$ for $G_{IG}$ values of 92 and 340 J/m$^2$ on specimen T4 showing the square root dependence over the first 500 microns.

Figure 4.28  Plot of $G_{IL}$ vs. $G_{IG}$ for specimen T4 showing $G_{IL} < G_{IG}$ in all cases.
Figure 4.29 SEM images of specimen T4 showing the formation of microcracks in front of the Teflon crack tip.
Chapter 4 Mode I Results

Figure 4.30 Comparison of $G_{IL}$ and $G_{IG}$ for elastic and dissipative behaviour.

Figure 4.31 Montage of the SEM crack tip images for specimen R1 (crack $c_5$), for $G_{IG} = 277$ J/m$^2$. 
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Figure 4.32 R-curve measured for high fibre volume specimen R1. It shows a clear increase of $G_{IC}$ as a function of crack growth.

Figure 4.33 Plot of COD vs. $r$ (distance from the crack tip) for $G_{IG} = 79$ J/m$^2$ on specimen R1 ($c_1$). Solid line shows COD profile calculated from $G_{IL} = G_{IG}$. $G_{ICG} = 105$ J/m$^2$. 

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Figure 4.34 Plot of COD vs. $r$ (distance from the crack tip) for $G_{IG}$ values of 82 and 250 J/m$^2$ on specimen R1 ($c_2$). Dashed line shows COD profile calculated from $G_{IL}$ that gives the best fit.

Figure 4.35 Plot of COD vs. $r$ (distance from the crack tip) for $G_{IG}$ values of 85, 252 and 380 J/m$^2$ on specimen R1 ($c_3$). Solid line shows COD profile calculated from $G_{IL} = G_{IG}$. Dashed line shows COD profile calculated from $G_{IL}$ that gives the best fit.
Figure 4.36 Plot of \( COD \) vs. \( r \) (distance from the crack tip) for \( G_{IG} \) values of 94, 263 and 400 J/m\(^2\) on specimen R1 (\( c_4 \)). Dashed line shows \( COD \) profile calculated from \( G_{IL} \) that gives the best fit.

Figure 4.37 Plot of \( COD \) vs. \( r \) (distance from the crack tip) for \( G_{IG} \) values of 99, 277 and 426 J/m\(^2\) on specimen R1 (\( c_5 \)). Dashed line shows \( COD \) profile calculated from \( G_{IL} \) that gives the best fit.
Figure 4.38 Plot of $G_{IL}$ vs. $G_{IG}$ for specimen R1. Dotted line represents a one-to-one correspondence. Dashed line represents $G_0$. Clear marker are obtained from video images and standard deviation is shown as error bar.
Chapter 5 Mode II Results

The objective of this series of tests was to compare the $G_{IIG}$ and $G_{III}$ for a brittle material, from the insert and from a grown crack. As shown previously, $G_{IIG}$ is calculated from the applied conditions while $G_{III}$ is obtained from the CSD profile. If all the applied load is transmitted to the crack tip, we should have:

$$G_{IIG} = G_{III}$$ (5.1)

Local crack tip phenomena, such as extensive microcracking and significant mode I openings, were observed. Therefore, the effect of these micromechanisms were studied in more detail and quantified.

5.1 Tests description

Results are presented for 20 applied $G_{IIG}$ levels distributed over 5 crack lengths and three DCB specimens of 24 layer unidirectional AS4/3501-6 CFRP laminate. The loading paths are described in Figure 5.1 to Figure 5.3 and the specimens dimensions, in Table 5.1. The loading is paused at regular $G_{IIG}$ intervals to take slow scan images.

5.1.1 Test from the insert and short crack growth

A specimen with an insert (B8 a1), was first loaded to $G_{IIG}$ levels of 122 and 225 J/m$^2$ and unloaded. The crack was grown 600 μm from the insert under mode I loading and then loaded at $G_{IIG}$ levels of 225, 326, 363, 423, 480 and 550 J/m$^2$ (B8 a2). The crack was extended 4900 μm from the insert and loaded at the following $G_{IIG}$ levels: 112, 210, 269, 324, 432, 543, 648 and 770 J/m$^2$ (B8 a3). Finally, the crack was loaded up to failure, which occurred at a $G_{IIG}$ of 880 J/m$^2$. 

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5.1.2 Test from the insert

A specimen (B11 a1) was loaded, from the insert, to $G_{II}$ levels of 104, 297, 494 and 695 J/m$^2$. Unstable crack growth occurred at 978 J/m$^2$. The principal goal was to study the effect of loading from the insert by comparing the results from tests from the insert (B8 a1 and B11 a1) with the ones with a grown crack (B8 a2, B8 a3 and B13 a1).

5.1.3 Long precrack test

A specimen (B13) with a long crack grown in mode I ($a_1$=44.64 mm) was loaded at $G_{II}$ levels of 103, 306, 499, 603 and 721 J/m$^2$. Then, unstable crack growth occurred, at a $G_{II}$ of 833 J/m$^2$. The goal is to study the effect of a large starter crack on the crack tip behaviour.

For all tests, slow scan images from the SEM are recorded at the indicated $G_{II}$ levels, at a magnification of 500x and a resolution of 1024 x 840 pixels. After the test, these images are assembled in a montage: a typical image is shown in Figure 5.4. More montages are shown in Appendix F. From these images, the COD and CSD versus $r$ profiles are obtained as described previously.

5.2 Crack tip behaviour

On all three specimens, matrix microcracks (Figure 5.5) are formed ahead of the crack tip in the resin rich region between the fibres as mode II is applied, which is what has been reported in the literature (Hibbs and Bradley, 1987; O’Brien et al., 1989). These microcracks form at a 45° angle to the fibre direction, which is the plane of principal normal stress for a pure mode II loading. Between two microcracks a ligament is created. As the mode II load is increased, the
angle between microcracks and the fibre direction increases. Finally the microcracked zone grows by coalescence of the microcracks at the fibre/matrix interface, either at the top or bottom of the matrix region (Figure 5.5 and Figure 5.6). All these findings are in agreement with studies found in the literature (Russell and Street, 1985; Hibbs and Bradley, 1987; O'Brien et al. 1989).

In Figure 5.5, the difference between the zone containing microcracks and the one where the microcracks have coalesced is quite visible: in the first zone, called the damaged zone, the shear displacements are continuous, while they are discontinuous in the coalesced zone. In Figure 5.7, we define the length of the zone grown by coalescence from the initial crack length $a_0$ to be $r_c$. The length of the zone containing microcracks that have not coalesced is called $L_d$.

One difficulty encountered was the determination of the crack tip position, because of the presence of the damage zone. In theory, the crack faces’ shear displacements should be zero at the crack tip. However, we measured significant shear displacements in front of the crack tip. The damage zone in front of the crack tip makes the crack behave as if it were longer: if we shift the analytical LEFM CSD profile towards the damage zone to a new crack length $a_{eff}$, we can find a position for which there is a good agreement with the shear displacement measurements. The effective crack size, $a_{eff}$, is equal to the sum of the initial crack length $a_0$, the coalesced zone length $r_c$ and a damage radius that we will call $r_d$ (see Figure 5.7):

$$a_{eff} = a_0 + r_c + r_d$$

(5.2)

The $G_{II}$ levels were recalculated using $a_{eff}$ instead of $a_0$: the difference was usually small. Nevertheless, all the $G_{II}$ values presented in this chapter are corrected to take into account $\Delta a = r_d + r_c$. 

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Since the crack tip position is advancing, it is not possible to plot the CSD as a function of \( r \), the distance from the crack tip. Instead, the CSDs are plotted as a function of \( x \), the position along the crack with an arbitrary origin, the same for all load levels.

### 5.2.1 CSD profiles

CSD profiles were generated for the indicated \( G_{II} \) levels at crack lengths of 18.4 mm (a1), 19.1 mm (a2) and 23.3 mm (a3) for specimen B8 (Figure 5.8 to Figure 5.9) and for specimen B11 a1 (Figure 5.10) and B13 a1 (Figure 5.11). The grid used for specimen B11 and B13 is four times finer than for specimen B8, thus providing much more accurate measurements.

Note that in the damage zone ahead of the crack tip, where there are no well defined crack faces, the crack face shear displacements have to be measured between points just above and below the damage zone. Since the strains in this area are very large, errors can be introduced if the measurements are not taken just above and below the damage. Due to the coarse mesh used for specimen B8, the displacements of the square centroids (see section 3.6) include elastic deformation of the undamaged material. Thus manual measurements very close above and below the damage were used close to the crack tip. At a certain distance behind the crack tip, the magnitude of the manually measured CSDs matched the automatic ones, and the latter were used for the rest of the profile. Since manual measurements require finding appropriate surface features, automatic measurements are more uniform: this explains why part of the CSD profiles for specimen B8 are more irregular. For specimens B11 and B13, the mesh was fine enough to use only the automatic method, and the profiles are smoother.
In Figure 5.8, for some load levels, not enough images were taken in the damage area. This lack of measurements makes the determination of the effective crack tip more hazardous and, in those cases, the local $G_H$ line is not shown.

For the $G_{IIc}$ levels lower than 200 J/m$^2$, for all specimens, the measured CSD profiles are in very good agreement with the analytical LEFM predictions, both in terms of shape of the curve and in magnitude (Figure 5.8 to Figure 5.11). The good agreement between $G_{IIc}$ and $G_{IIIc}$ indicates that all the load is transmitted to the crack tip and there is no effect from friction.

As can be seen, the data starts deviating from the predicted square root profile at approximately 1000 µm behind the crack tip. Beyond this, the higher order terms begin to dominate the profile.

The length of the zone were the measurements and theoretical curve coincide is reduced for higher $G_{IIc}$ levels, since there is more damage, affecting the displacement field.

### 5.2.2 CSD at failure

With specimen B8 a$_3$, the load was increased up to failure and images were recorded on video. Since events unfold rapidly, we focused at one position $r$ behind the crack tip. The measured CSD for $G_H$ levels of 767 and 880 J/m$^2$ are shown in Figure 5.12. It is not possible to measure the $a_{eff}$ at failure, therefore it is determined using equation (5.2), approximating $r_d$ and $r_c$ by linear extrapolation (the linear increase of $r_d$ and $r_c$ with $G_{IIc}$ will be shown later). The analytical predictions obtained fit the experimental measurements reasonably well, demonstrating that the model developed is valid up to failure.
5.2.3 COD profiles

The COD profiles were measured for the indicated $G_{II}$ levels at crack lengths of 18.4 mm ($a_1$), 19.1 mm ($a_2$) and 23.3 mm ($a_3$) on specimen B8 (Figure 5.13 and Figure 5.14) and for specimens B11 $a_1$ (Figure 5.15) and B13 $a_1$ (Figure 5.16). For a pure mode II test, the COD are expected to be 0. However, all the figures show that this is not the case. There is a significant amount of mode I opening observed. Careful observation of the SEM images suggests that local opening displacements are created by surface roughness features sliding on top of each other (Figure 5.17).

LEFM analytical profiles are back calculated from the COD measurements in Figure 5.13 to Figure 5.16, by adjusting the value of $G_{II}$ until there is good agreement. On all the figures, we notice that the measurements agree well with a square root singularity for a length of roughly 1 mm from the crack tip. Then the CODs seems to reach a plateau. This can be explained by the fact that, at a certain distance behind the crack tip, a maximum surface roughness has been reached and the height of these bumps is roughly constant.

On Figure 5.14 ($a_3$), it can be seen that initially, with no mode II load, a mode I component of roughly 7 J/m$^2$ is present. Since the crack has been grown 4 mm from the insert, there is debris preventing it from shutting closed.

5.2.4 Relation between mode I and mode II

For all the specimens, the magnitude of the CODs increases with the mode II load, as the wavy crack surfaces oscillations slide over each other (Figure 5.17). This finding is confirmed in Figure 5.18, where the $G_{II}$ is plotted against the $G_{III}$. When there is only an insert (B8 $a_1$ and
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B11 a1), $G_{II}$ is much smaller for the same $G_{IIc}$. This can be explained by the fact that the Teflon insert is smoother than the crack grown in the material.

$G_{II}$ values as high as 33 J/m$^2$ (B8 a3 and B13 a1) are measured: this is 25% of $G_{IC}$, therefore the mode I component is very significant. This would mean that what is thought to be a pure mode II loading is in fact a mixed-mode loading with a significant ratio of mode I. Thus the final failure might be due, at least partly, to the mode I component. Moreover, these high $G_{II}$ values are measured before unstable failure is reached; $G_{II}$ at failure is likely to be even higher. On Figure 5.18, the $G_{II}$ values at failure have been evaluated by linear interpolation and plotted against $G_{IIcG}$ (dashed lines) for the 3 cases were $G_{IIcG}$ is known. Even though these values are not exact, they show a trend: the higher the $G_{IIcG}$, the lower the mode I component. This would confirm the findings by several authors that $G_{IIc}$ from an insert is higher than from a precrack (O'Brien, 1997), since, as we have seen, $G_{II}$ is lower due to less surface waviness.

Figure 5.19 shows the effect of not knowing that there is a mode I component on the mixed-mode failure envelope, for the 3 failure points shown in the previous graph. The pure mode I value comes from specimen B1 (Chapter 4). If we neglect the mode I component, the $G_{IIc}$ values are different and there seems to be significant scatter in the data. If the $G_{IL}$ values are included, then each data point is shifted up by a different amount and there is a linear relation between them rather than scatter. Similarly, O'Brien (1997) points out that the scatter in mixed-mode delamination test results for AS4/3501-6 increases significantly for high mode II ratios (Figure 5.20). These points do not take into account a possible local mode I component. According to our findings, the tests with lower $G_{IIc}$ would have the highest mode I component. Moving those points to the left (more mode I), and up (addition of $G_{IC}$) might bring them closer to each other.
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A simple model can be developed to model the relation between $G_{II}$ and $G_{II}G$ (see inset in Figure 5.21), where the surface waviness is characterized by an angle $\theta$. Using equations (3.12) and (3.13), we have the following equation:

$$\tan(\theta) = \frac{COD}{CSD} = \frac{A_I \sqrt{G_{II}} \sqrt{r_I}}{A_{II} \sqrt{G_{II}G} \sqrt{r_{II}}}$$

where

$$A_I = \frac{4}{\sqrt{\pi}} 2^2 \left[ \frac{2a_{12} + a_{66}}{2a_{11}} + \sqrt{\frac{a_{22}}{a_{11}}} \right]^{\frac{1}{2}} (a_{11}a_{22})^{\frac{1}{2}}$$

$$A_{II} = \frac{4\sqrt{a_{11}}}{\sqrt{\pi}} 2^2 \left[ \frac{2a_{12} + a_{66}}{2a_{11}} + \sqrt{\frac{a_{22}}{a_{11}}} \right]^{\frac{1}{2}}$$

and $r_I$ and $r_{II}$ are the distances between the surface oscillations and the COD and CSD profile origins, respectively.

We have

$$r_{II} = r_I + r_d$$

where the damage radius $r_d$ is obtained from the shift in CSD profile ($r_d$ will be studied in more detail later) and it increases with $G_{II}G$.

Therefore

$$G_{II} = \left[ \left( \frac{A_{II}}{A_I} \right)^2 \left( 1 + \frac{r_d}{r_I} \right) \tan^2 \theta \right] G_{II}G$$
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The model is compared to the measurements in Figure 5.21 and the angles $\theta$ giving the best agreement are represented on the surface waviness images in Figure 5.22.

5.3 Crack advance

5.3.1 Microcrack behaviour

The angle that the microcracks make with the plane of the crack has been measured in the damage zone for each load level for specimen B8 $a_3$, and is presented in Figure 5.23. We can see that when the microcracks appear, on the left of each curve, they make a 45° angle with the crack. As the load is increased, the microcrack angle increases, up to a maximum of roughly 90° (at the right of each curves), then they fail. Since the ligaments at the right are failing, the curves are moving to the left as the load increases. At the same time, they are becoming more spread out, as the damage zone is growing in length.

In Figure 5.24, the microcrack angles are presented for the highest load level, together with the COD and CSD profiles. The CODs start where the microcracks have reached 90°: the crack can only open in mode I once the ligaments have coalesced. Also, the origin of the line of best fit is roughly at the middle of zone containing hackles. Moreover, this point seems to correspond to an inflexion in the hackle angle curve: the slope is less on the left.

5.3.2 Coalescence and damage zone measurements

For the lower applied $G_{IIc}$ loads, the COD profiles have the same origin with increasing mode II load, since the ligaments created by the microcracking can still withstand an opening load. However, at higher load levels, for specimen B8 ($a_2$ and $a_3$) and B13 $a_1$, the origin of the COD
profile is shifting: the crack is advancing by a distance $r_c$ because the microcracks are coalescing. A visual observation of the images confirms that the distance $r_c$ obtained from the shift in the COD profiles agrees very well with the length of the coalesced zone.

Once the coalescence length $r_c$ has been determined from the advance in COD profile, the damage radius $r_d$ can be obtained from the effective crack size $a_{eff}$, following equation (5.2): $r_d$ and $r_c$ are presented in Figure 5.25 to Figure 5.27 for specimens B8 a$_3$, B11 a$_1$ and B13 a$_1$ respectively. From the observation of the damage zone on the images (B8 a$_3$), the end of the damage zone has been measured and it can be observed (Figure 5.25) that the distance $L_d$ between the end of the coalesced zone and the end of the damage zone is roughly twice the values $r_d$ from the CSD profiles: the damage radius $r_d$ is half of the damage zone length.

Looking at Figure 5.28, the coalescence length $r_c$ can be compared for all three specimens. On this figure, the values for B8 a$_1$ and B8 a$_2$ have been added, despite the lack of many data points and the lower $G_{II}^{UL}$ levels, to see if they follow the general trend observed in the other sets of results. We can see that for all of them there is almost no coalescence below 200 or 300 J/m$^2$, then there is a linear increase, but the rate of increase is very different for each specimen: high for B13 a$_1$, less high for B8 a$_3$ and B8 a$_2$ and almost 0 for B11 a$_1$. Also, if we look at Figure 5.18, we can see that B11 a$_1$ has the lowest $G_{IL}$ for the same $G_{II}^{UL}$. For a lower surface waviness, $G_{IL}$ is lower and therefore the ligaments are less likely to reach the limit strain and coalesce. However, the coalescence rate is very different between B8 a$_2$, B8 a$_3$ and B13 a$_1$ (Figure 5.28), yet they have very similar $G_{IL}$ to $G_{II}^{UL}$ relationships (Figure 5.20). The main difference between them is that B13 a$_1$ has the longest precrack, followed by B8 a$_3$ and B8 a$_2$ and B11 a$_1$ has no precrack.
Therefore, there is a correlation between crack length and rate of coalescence, but we have no explanation for this trend yet.

On Figure 5.29, the damage radius \( r_d \) is compared for all specimens. The trend is similar for all specimens: steady increase in \( r_d \), with a relatively similar rate of increase. The rate of increase is lower for higher \( G_{IIc} \): for B13 a1, \( r_d \) becomes almost constant above 300 J/m\(^2\), for the other specimens, the slope is lower than at the beginning.

### 5.3.3 Damage zone size modelling

By analogy with the plastic zone in metals (Irwin, 1960), Williams (1989) evaluated the damage size in mode I by assuming that the mode I stresses in the damage zone have reached a maximum of \( \sigma_d \), which would be equal to the damage stress measured in an unnotched tensile specimen. This damage stress is analogous to the yield stress in a metal. This approach can be applied to mode II loading: the shear stress in the damage zone is then equal to \( \tau_d \), the shear stress measured in an unnotched specimen loaded in bending when damage appears. This approach can be modified, as proposed by Irwin (1960), to redistribute the load not carried in the damage zone, by assuming that the presence of a damage zone makes the crack displacements larger and stiffness lower, as if the crack were longer, of size \( a_{eff} = a_0 + \delta \) (Figure 5.30).

The elastic shear stress distribution at the tip of the effective crack size, in the plane \( \theta = 0^\circ \), is:

\[
\tau_{xy} = \frac{K_{II}}{\sqrt{2 \pi r}}
\] (5.8)
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The shear stress in the region \( \delta + \lambda \) is limited to the damage stress \( \tau_d \) (Figure 5.30). Hence, for \( r = \lambda, \tau_{xy} = \tau_d \), and equation (5.8) becomes:

\[
\tau_d = \frac{K_{II}}{\sqrt{2\pi\lambda}} \quad \text{or} \quad \lambda = \frac{1}{2\pi} \left( \frac{K_{II}}{\tau_d} \right)^2 \quad (5.9)
\]

The load lost in region A must be carried by region B:

\[
\delta\tau_d = \int_0^{\lambda} \frac{K_{II}}{\sqrt{2\pi r}} dr - \tau_d\lambda \quad (5.10)
\]

\[
(\delta + \lambda)\tau_d = K_{II} \frac{\sqrt{2\pi\lambda}}{\pi} \quad (5.11)
\]

Replacing \( \tau_d \) according to equation (5.9), it follows that

\[
\delta + \lambda = 2\lambda \quad (5.12)
\]

\[
\delta = \lambda = \frac{1}{2\pi} \left( \frac{K_{II}}{\tau_d} \right)^2 \quad (5.13)
\]

Therefore \( \delta \) is half the length of the damage zone. Since this model does not take into account coalescence of the crack, \( \delta \) is the same as the previously defined damage radius \( r_d \) (equation (5.2) with \( r_c = 0 \)) and:

\[
r_d = \frac{1}{2\pi} \left( \frac{K_{II}}{\tau_d} \right)^2 \quad (5.14)
\]

Sih, Paris and Irwin (1965) derived a relationship between \( G_{II} \) and \( K_{II} \) for an orthotropic material:

\[
G_{II} = K_{II}^2 \frac{a_{11}}{\sqrt{2}} \left[ \frac{a_{22}}{a_{11}} + \frac{2a_{12} + a_{66}}{2a_{11}} \right]^2 \quad (5.15)
\]
Thus, combining the Williams and Irwin approaches, we get:

$$r_d = \frac{1}{2\pi} \frac{G_{II}}{\tau_d^2} a_1 \left[ \frac{a_{22}}{a_{11}} + \frac{2a_{12} + a_{66}}{2a_{11}} \right]^{-\frac{1}{2}}$$  \hspace{1cm} (5.16)

To evaluate $\tau_d$, the shear damage stress, short beam shear tests were conducted (see Appendix D). A deviation from linearity on the load-displacement curve was obtained at 67 MPa and failure occurred at 101 MPa. By comparison, Adams and Lewis obtained a shear failure strength for AS4/3501-6 of 106 MPa with the Short Beam Shear Test Method and 114 MPa with the Iosipescu Shear Test Method, values which are very close to ours. In the analogy with plastic zone in metals, the shear damage stress is the equivalent here of the yield stress. Therefore, the appropriate value to use for the shear damage stress $\tau_d$ in equation (5.16) is the point of deviation from linearity, with a value of 67 MPa.

As mentioned previously, $r_d$ values have been obtained experimentally by measuring the shift needed to have agreement between the measured CSD profile and the analytical predictions. The values $r_d$ appeared to be roughly equal to half the length of the total damage zone $L_d = \delta + \lambda$ determined by visual inspection, which agrees well with equation (5.13). Figure 5.31 offers a comparison of the measured $r_d$ values with the model: the dashed lines are calculated using equation (5.16) and the elastic properties of AS4/3501-6, for the two bounding values of $\tau_d$ obtained by the Short Beam Shear Test, at the deviation from linearity ($\tau_d=67$ MPa) and at failure ($\tau_d=101$ MPa). All specimens fall in the range between the two bounds. The slope of the model using the deviation from linearity (67 MPa) agrees well with the initial rising part of the
measured $r_d$, for most specimens. Above $G_{II}=300$ $J/m^2$, $r_d$ increases with a smaller slope, which would agree better with a model using the failure value of $\tau_d$ (101 MPa).

5.4 Delamination resistance curves

Figure 5.32 shows the increase in toughness with crack growth ($\Delta a = r_d + r_c$). Up to a value $G_{II}$ of 100 $J/m^2$, the load increases with no damage. Then, as the load continues to increase, the damage increases and so does the resistance to crack growth. The increase in toughness is steeper for shorter initial cracks. Ideally, the R-curve is thought to be a material property and thus the same irrespective of the initial crack size (Broek, 1987). To illustrate this, on Figure 5.33, the crack extension $\Delta a = r_d + r_c$ is plotted to the right and the crack length is plotted to the left. For a given $P$ and $\delta$, $G_{II}$ is plotted as a function of crack length, according to equations (3.3) and (3.11). For the points of final fracture (marked as an x), the value $\Delta a = r_d + r_c$ is not known. It was approximated by linear extrapolation from the previous points. Final fracture should occur when (Broek, 1987):

$$\frac{\partial G_{II}}{\partial a} = \frac{\partial G_{IIIc}}{\partial a}; G_{II} = G_{IIIc}$$

(5.17)

which is when the $G_{II}$ line becomes tangent to the R-curve line (Figure 5.33). When the initial crack length is increased, the slope of the $G_{II}$ lines is reduced, therefore it is expected that it would be possible to go further on the R-curve line. But as we can see on Figure 5.33, this is not what is happening in our case, since the R-curves are different for different crack lengths. The R-curve difference is due to the fact that $\Delta a = r_d + r_c$ is higher for longer crack. As we have seen previously (Figure 5.28 and Figure 5.29), this is mainly due to $r_c$ being much higher. In the insert
case (B11 a1), we have previously pointed out that $r_e$ increases less, probably due to the lower $G_{II}$. However, the difference between the short and long precrack cases is harder to explain, since they have similar $G_{II}$.

To characterize the resistance curve, the damage zone can be represented by a series of continuously distributed nonlinear springs (Hutchinson and Suo, 1992). The shear stress $\tau$ is a function of the shear displacement $\delta_s$ and this function is identical for each spring. When $\delta_s$ reaches a maximum $\delta_{s0}$, the spring cannot withstand any load anymore. According to the J-integral conservation (Rice, 1968):

$$G_{II} = G_0 + \int_0^{\delta_s} \tau(\delta_s) d\delta_s$$  \hspace{1cm} (5.18)

where $\delta_s$ is the shear displacement at the initial crack tip. According to Hutchinson and Suo (1992), differentiating (5.18) yields the spring law:

$$\tau(\delta_s) = \frac{\partial G_{II}}{\partial \delta_s}$$  \hspace{1cm} (5.19)

A second order polynomial can be fitted to a curve of $G_{II}$ vs. $\delta_s$ and its derivative corresponds to the stress-displacement curve.

Since specimen B11 a1 experienced almost no coalescence, it is a good candidate to evaluate the $\tau$ vs. $\delta_s$ curve. Figure 5.34 shows the curve $G_{II}$ vs. $\delta_s$ (the end-opening at $x=1080$ microns, the initial crack tip) for specimen B11 a1. A linear fit through the 4 points gives a slope of 66 MPa. Thus, according to equation (5.19), $\tau$ would be constant with $\delta_s$ and equal to 66 MPa, corresponding to a rigid-plastic behaviour of the damaged material (Figure 5.35). This value is
extremely close to the value obtained for the deviation from linearity on the load-displacement curve from the short beam shear test.

However, some experimental results are not accounted for by this model. In particular, it does not conform to the behaviour observed during coalescence of the microcracks. For example, from the crack growth observed from the COD profile of specimen B8 a3, it is noticed that the ligaments at the initial crack length coalesce at $G_{II}=211$ J/m$^2$ and $\delta_{s0}=2$ μm. According to equation (5.18), $G_{II}$ reaches a steady state value when the ligament at the initial crack tip is submitted to its maximum displacement $\delta_{s0}$ and the entire area under the strain softening law has been observed. In our observation, $G_{II}$ continues to increase after $\delta_{s0}$ has been reached at the initial crack tip. For this reason, it is not correct to take the derivative of the $G_{II}$ versus $\bar{\delta}_{s}$ curve for specimens B8 a3 and B13 a1 (Figure 5.36) in the zone where coalescence is occurring, because the increase in $\bar{\delta}_{s}$ is due not only to the progression of damage but also to the advance of the crack by coalescence. In Figure 5.36, the slope of $G_{II}$ versus $\bar{\delta}_{s}$ is effectively decreasing when coalescence starts to occur, confirming that $\bar{\delta}_{s}$ is increasing more rapidly due to microcracks coalescence. This is especially true for B13 a1, which, as we have shown previously, is experiencing considerably more coalescence. We are left with only the two first points of the $G_{II}$ versus $\bar{\delta}_{s}$ to evaluate the derivative, which lacks precision. We get a constant material stress-displacement curve at a value of 90 MPa for B8 a3 and 65 MPa for B13 a1. The value for specimen B13 a1 is very close to the one for specimen B11 a1 described earlier, and agrees with the damage stress obtained from the Short Beam Shear Test (67 MPa). The value for B8 a3 is higher, but is still less than the shear strength obtained from the Short Beam Shear Test.
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(101 MPa). We can see on Figure 5.36 that, below 600 J/m², B8 a₃ and B11 a₁ have overall a very similar \( G_{II} \) versus \( \overline{\delta}_s \) curve. This is probably due to the fact that, below 600 J/m², the coalescence in B8 a₃ is still small. If we do a linear fit through the B8 a₃ points below 600 J/m², we get a slope of 52 MPa. We can therefore estimate that the constant material stress-displacement curve for B8 a₃ is somewhere between 52 and 90 MPa.

Contrary to what is predicted by the model, \( G_{II} \) continues to increase after \( \delta_{s0} \) has been reached at the initial crack tip. At subsequent points of crack coalescence, we have measured a \( \delta_{s0} \) which is increasing, on all the specimens. This contradicts the assumption for equation (5.18) stating that any point in the damage zone was experiencing the same strain softening law. The experimental results show that as the crack is growing, we are moving along the stress-displacement curve (Figure 5.35) and this has to be taken into account in the model. Thus, as the microcracks coalesce, more damage is created and the ligament can withstand a higher deformation. When we observed the images for B8 a₃, we measured an increase in the height of the damage zone at the tail, \( h_d \), with the mode II load (Figure 5.37 and Figure 5.38). Thus the height of the damage is increasing, yielding more energy absorption, an increase in \( G_{II} \) and a bigger surface under the strain softening curve. We can also notice on Figure 5.38 that for the highest load, \( h_d \) decreases while \( r_d \) doesn’t change and \( r_c \) increases more rapidly: may be we are reaching the steady state point were \( r_d \) and \( h_d \) would be constant because all the area under the stress-displacement curve has been used. However, since unstable crack growth occurred right after, it is not possible to confirm this trend.

Such an increase in damage height is not observed for B11 a₁, since there is so little coalescence (the total length of coalescence is enclosed in Figure 5.37). We can consider \( \delta_{s0} \) and therefore,
the plateau zone in the R-curve, have not been reached yet and the model is applicable. The height of the damage zone at the tail, $h_d$, is 17.3 microns.

For specimen B13 a$_1$, Figure 5.39 shows how $h_d$ and $r_d$ are both almost constant for increasing $G_{II}$ values above 300 J/m$^2$ while $r_c$ is increasing rapidly. This is similar to what is happening for the highest load of B13 a$_1$. At 300 J/m$^2$, $r_d$ and $h_d$ have attained their steady state values and the damage zone is just moving without increasing in size. We do not know why $G_{IIc}$ is still increasing then.

5.5 Summary

- **CSD** profiles have been generated for specimens under pure mode II loading. For low applied $G_{II}$ levels, a square root singularity has been observed for roughly 1000 µm from the crack tip. There is good agreement between $G_{IIc}$ and $G_{IIc}$, therefore friction is not reducing the load transmitted to the crack tip.

- When $G_{II}$ increases, 45° microcracks appear in front of the crack tip, forming a long damage zone. At the tail of the damage zone, there is coalescence of the microcracks. The origin of the CSD profile is shifted ahead of the crack tip and goes down to 0 less rapidly: the square root behaviour is less and less pronounced for increasing $G_{II}$ levels.

- **COD** profiles have been measured, and their magnitude is quite significant, even though a pure mode II is applied. These CODs are due to the waviness of the crack: as crack features slide over each other due to the mode II load, they open the crack. $G_{IIc}$ is lower when the crack tip is at the end of the insert than when there is a crack, since there is less waviness.
That could explain why initiation $G_{IIc}$ values are found to be higher than propagation ones, since $G_{II}$ is lower. The variable nature of surface waviness, and therefore, the variable amount of $G_{IIc}$, can also explain the higher scatter in the $G_{IIc}$ data in the literature.

- The point at which the COD become 0 indicates where the microcracks are intact and have not coalesced: this allows the determination of the length of coalescence, $r_c$. The distance between the origin of the COD profile and the origin of the CSD profile determines the damage zone $r_d$. Above a threshold value of 200-300 J/m$^2$, $r_c$ increases linearly. The rate of increase of $r_c$ with $G_{II}$ is almost 0 when there is no precrack, since $G_{IIc}$ is lower: coalescence might be provoked by reaching a certain level of crack opening. The rate of increase of $r_c$ is higher with longer precracks. As for $r_d$, it increases with $G_{IIc}$ in a relatively similar way for all specimens.

- Two approaches are taken to try to model the damage zone:
  
  In an analogy with Irwin's plastic zone in metals, it is assumed that the damage zone has the same effect as if the crack was longer and that the shear stress has reached a limit and is constant in the damage zone. The result is that $r_d$ should be proportional to $G_{II}$, and the rate of increase depends on the shear damage stress and the material elastic properties. The experimentally measured $r_d$ increase with $G_{II}$ at a rate which correspond to a shear damage stress which is in a reasonable range, between the deviation from linearity stress and the failure stress obtained from short beam shear tests.
Invoking the Dugdale model, the damage zone can also be represented by a series of non-linear springs having identical stress-displacement curves. According to the J-integral conservation, the spring law can be deduced from the derivative of the $G_{II}$ versus $CSD$ at the initial crack tip curve obtained experimentally. We obtained a rigid-plastic damage response with a shear stress of 66 MPa, which is equal to the value obtained from the deviation from linearity in the short beam shear test.
## Table 5.1 Characteristics of specimens B8, B11 and B13.

<table>
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<th></th>
<th>Specimen B8</th>
<th>Specimen B11</th>
<th>Specimen B13</th>
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<td>AS4/3501-6</td>
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<td>AS4/3501-6</td>
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<td>$V_f$ (%)</td>
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<td>59</td>
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<tr>
<td>$h$ (mm)</td>
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<td>$B$ (mm)</td>
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<tr>
<td>$a$ (mm)</td>
<td>18.4 ($a_1$)</td>
<td>18.4 ($a_1$)</td>
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5.7 Figures

Figure 5.1 Description of specimen B8 loading path

Figure 5.2 Description of specimen B11 loading path
Figure 5.3  Description of specimen B13 loading path

Figure 5.4  SEM crack tip image (B8 a1) for $G_{II} = 225 \text{ J/m}^2$

Figure 5.5  SEM image showing microcracks: the damage and coalescence zones are delimited by a transition from displacement continuity across the crack plane to displacement discontinuity.
Figure 5.6 Mechanisms of microcracks creation, rotation and coalescence (in agreement with Russell and Street, 1985; Hibbs and Bradley, 1987; O’Brien et al. 1989)

Figure 5.7 Definition of crack tip damage zone parameters.
Chapter 5 Mode II Results

Figure 5.8 Plot of $CSD$ vs. $x$ (longitudinal position) for specimen B8 ($a_1$ and $a_2$). Lines show $CSD$ profiles based on $G_{II}L$ equal to $G_{IIG}$.

Figure 5.9 Plot of $CSD$ vs. $x$ (longitudinal position) for specimen B8 $a_3$. Lines show $CSD$ profiles based on $G_{II}L$ equal to $G_{IIG}$.

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Chapter 5 Mode II Results

Figure 5.10 Plot of CSD vs. x (longitudinal position) for specimen B11 a1. Lines show CSD profiles based on $G_{II}L$ equal to $G_{II}G$.

Figure 5.11 Plot of CSD vs. x (longitudinal position) for specimen B13 a1. Lines show CSD profiles based on $G_{II}L$ equal to $G_{II}G$. 

- $G_{II}G = 695 \text{ J/m}^2$
- $G_{II}G = 494 \text{ J/m}^2$
- $G_{II}G = 297 \text{ J/m}^2$
- $G_{II}G = 104 \text{ J/m}^2$
- $G_{II}G = 721 \text{ J/m}^2$
- $G_{II}G = 603 \text{ J/m}^2$
- $G_{II}G = 499 \text{ J/m}^2$
- $G_{II}G = 306 \text{ J/m}^2$
- $G_{II}G = 103 \text{ J/m}^2$

- $G_{II}L = G_{II}G$
Figure 5.12 Plot of CSD vs. \( x \) (longitudinal position) for specimen B8 \( a_3 \). Unstable failure occurred at 880 J/m\(^2\). Lines show orthotropic LEFM prediction based on \( G_{II} \) values of 770 and 880 J/m\(^2\). The value \( r_d+r_c \) for 880 J/m\(^2\) has been estimated by linear extrapolation.
Chapter 5 Mode II Results

Figure 5.13 Plot of COD vs. $r$ (distance from the crack tip) for specimen B8 ($a_1$ and $a_2$) loaded under pure mode II. Dashed lines show COD profiles for $G_{II}$ that best fit the measurements.

Figure 5.14 Plot of COD vs. $r$ (distance from the crack tip) for specimen B8 $a_3$ loaded under pure mode II. Dashed lines show COD profiles for $G_{II}$ that best fit the measurements.
Chapter 5  Mode II Results

Figure 5.15  Plot of COD vs. $r$ (distance from the crack tip) for specimen B11 $a_1$ loaded under pure mode II. Dashed lines show COD profiles for $G_{IL}$ that best fit the measurements.

Figure 5.16  Plot of COD vs. $r$ (distance from the crack tip) for specimen B13 $a_1$ loaded under pure mode II. Dashed lines show COD profiles for $G_{IL}$ that best fit the measurements.
Figure 5.17 Local opening displacement created under global shear loading

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Chapter 5 Mode II Results

Figure 5.18 Plot of $G_{IIr}$ vs. $G_{IIC}$ global for specimen B8, B11 and B13. Hollow markers indicate test from insert. The x markers show values at failure ($G_{IIC}$ evaluated by linear extrapolation).

Figure 5.19 Effect of neglecting the mode I opening due to surface waviness on the mixed-mode failure envelope.
Figure 5.20 Mixed mode failure criterion for AS4/3501 (O’Brien, 1997). The arrows show where the data points would move if some local mode I opening due to surface roughness is taken into account.

Figure 5.21 Plot of $G_{II}$ vs. $G_{III}$. Dashed lines represent the model.
Figure 5.22  Surface waviness angles used in the model presented in Figure 5.21.
Figure 5.23 Microcracks angle for increasing $G_{IIg}$ (specimen B8 a₃). The solid trendlines extremities correspond to the visible edges of the damage zone, while their inflection point is positioned at the origin of the CSD profile.

Figure 5.24 Relationships between microcrack angle and crack faces displacements.
Figure 5.25 Plot of the coalesced zone length \( r_c \), damage radius \( r_d \) and zone length \( L_d \) vs. \( G_{IK} \) for specimen B8 a_3.

Figure 5.26 Plot of the coalesced zone length \( r_c \) and damage radius \( r_d \) vs. \( G_{II} \) for specimen B11 a_1.
Figure 5.27 Plot of the coalesced zone length $r_c$ and damage radius $r_d$ vs. $G_{IIc}$ for specimen B13 A1.

Figure 5.28 Plot of coalesced zone length $r_c$ vs. $G_{IIc}$ obtained from the shifts in the measured COD profiles from specimens B8, B11 and B13. Hollow markers indicate tests from the insert.
Figure 5.29  Plot of damage radius $r_d$ vs. $G_{II}$ obtained from the shifts in the measured COD and CSD profiles from specimens B8, B11 and B13. Hollow markers indicate tests from insert.

Figure 5.30  Estimate of damage zone size
Figure 5.31 Plot of $r_d$ vs. $G_{II}$ for specimens B8, B11 and B13. The dashed lines represent the Irwin-Williams model for $\tau_d$ values of 67 MPa (deviation from linearity) and 101 MPa (failure). Hollow markers indicate test from insert.

Figure 5.32 R-curve for specimen B11 a1, B8 a3 and B13 a1.
Chapter 5 Mode II Results

Figure 5.33 R-curve for specimen B8 a3, B11 a1 and B13 a1 showing how $G_{II}$ depends on crack size.

Figure 5.34 Plot of $G_{II}$ vs. $\bar{\delta}_s$ at the damage zone tail for specimen B11 a1.
Figure 5.35 Stress-displacement curve characterizing the damaged material in specimen B11 a1.

Figure 5.36 Plot of $G_{II}$ vs. $\bar{\delta}$ at the tail of the damage zone for specimen B8 a3, B11 a1 and B13 a1. Specimen B11 a1 showed almost no coalescence.
Figure 5.37 Damage zone height, at the tail of the damage zone (B8 a3, B11 a1 and B13 a1)
Figure 5.38  Damage zone height at the tail of the damage zone, $h_d$, as a function of $G_{II}$, for specimen B8 a$_3$.

Figure 5.39  Damage zone height at the tail of the damage zone, $h_d$, as a function of $G_{II}$, for specimen B13 a$_1$. 

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Chapter 6 Conclusions and Further Work

The main objective of this work was to study and understand delamination crack tip behaviour. To achieve this objective, the local crack tip behaviour was quantified and compared to the global behaviour. The conclusions that can be drawn from this work are presented in this chapter, as well as recommendations for future work.

6.1 Conclusions

• An in-situ SEM experimental method has been developed.

Full size standard delamination specimens can be loaded up to failure in mode I, mode II and mixed mode inside a SEM. The globally applied conditions and the local crack tip displacements can be simultaneously measured and compared. This experimental method is not meant to become a common testing method: the goal is to use it to further the understanding of delamination behaviour. This knowledge can be used to help in defining standard tests for failure criterion determination, to develop tougher materials and to establish design procedures.

• The classic LEFM prediction corresponds to the local behaviour for a brittle material with minimal fibre bridging under mode I loading.

Extensive testing has been conducted: COD profiles have been measured for 10 $G_{IG}$ levels covering 3 crack lengths (insert and small crack growths) for a brittle material and 4 $G_{IG}$ levels and 1 crack length (insert) for a tougher material. The square root dependency of the $COD$s for the first 500 μm behind the crack tip has been observed repeatedly. The
magnitudes of the COD profiles for the brittle material correspond to what is expected from the global applied conditions, right up to failure.

The measured COD profile all the way to the loading points has also been compared to the calculated displacement from a finite element model and the agreement is very good.

- **The increase in crack resistance with mode I crack growth is explained.**

The COD profiles have been measured for 12 $G_{IC}$ levels covering 5 increasing crack lengths. With crack growth, the COD profiles have a lower magnitude than expected due to the closing action of fibre bridges on the crack tip. Therefore, more load can be applied before crack growth occurs and $G_{IC}$ increases significantly. It appears that crack growth occurs when the COD profile reaches the same magnitude as the critical COD profile of the initial crack with no fibre bridging, and therefore, the behaviour of the fibre bridging is elastic rather than dissipative.

- **The mode II crack tip behaviour and damage development has been quantified.**

The CSD and COD profiles have been measured for 20 $G_{IIc}$ levels over 5 crack lengths. The development and coalescence of microcracks in front of the crack tip under mode II loading has been observed and measured. The damage zone size and damaged material stress-displacement behaviour have been modelled.
• The presence of local mode I under pure mode II loading has significant consequences for material characterization and design.

Significant CODs have been measured when the global loading is nominally pure mode II. Their presence can be explained by the roughness of the crack faces. They are reduced when the starter crack consists of the starter film with no precrack.

This finding has significant consequences on the development of a standard pure mode II test for the determination of $G_{IIc}$. Furthermore, the crack waviness should be taken into account at the design stage, and more studies are needed to be able to do this modelling. Manufacturing issues are also involved: the smaller the fibre waviness, the smaller the mode I component induced by the pure mode II loading and the more load the part will be able to withstand.

6.2 Further work

Two main areas can be the object of further work: improvements to the experimental method and the use of the method to study other problems of interest.

• Several improvements to the experimental method could make the measurements simpler, faster and more accurate. In particular, the video images quality could be increased by removing the present noise and distortions. This would provide valuable quantitative measurements just before failure.

• More tests should be conducted to study the effect of various parameters. Tests on tough materials could help explain the mechanisms leading to higher resistance to delamination.
Chapter 6 Conclusions and Further Work

As a first step, this work has concentrated on unidirectional materials, but real parts have more complicated lay-ups and it would be useful to conduct tests on specimen with various fibre orientations and lay-ups.

Finally, as the precrack characteristics has a big influence on the determination of delaminated material toughness, tests should be conducted on specimens with varying insert thickness.

- Mixed-mode tests for a series of mode I to mode II ratios should be conducted to study the mixed-mode failure envelopes. In particular, this would provide a crucial knowledge of how the globally applied loads are related to the local mode I and mode II components. As we have seen from the pure mode II tests, the local mode I and mode II components can be different from what is expected from the globally applied conditions. This can have serious consequences on the mixed mode failure envelope, which might explain the widely different failure envelopes presented in the literature. The determination of the failure envelope is essential for design purposes.

- Finally, the effect of path dependency (i.e. mode I loading applied before a mode II loading, and vice-versa) on the local crack tip behaviour could also be explored.
References


References


References


APPENDIX A  Specimen Preparation

The specimen preparation involves three major steps: the bonding of the tabs, the polishing of the edge and the deposition of the gold grid.

A.1 Bonding of the loading and clamping tabs

Once the specimen is cut to the desired dimensions, loading tabs and, in the case of mode II and mixed-mode, clamping tabs must be bonded to it. A jig is used to ensure a good alignment of the tabs with the specimen (Figure A.1).

The first step is to prepare the jig by cleaning it and applying a mold release agent for epoxy. The bonding surfaces, on the tabs and on the specimen are scrubbed with sandpaper (grit 400) and cleaned with acetone.

A piece of adhesive epoxy film (AF-126) is cut to the tab dimension and bonded to it. Then the tabs and the specimen are placed in the jig. After closing the jig, pressure is applied on the assembly by the use of C-clamps. The adhesive is cured in the oven at 250°F (120-125°C) during two hours. The assembly is allowed to cool down before being opened carefully, to avoid propagating the crack.

The clamping tabs need to be milled to ensure that the total thickness of the composite, adhesive and tabs just fit in between the clamping rollers. The required thickness is 0.55 inch.

A.2 Polishing

The edge of the specimen has to remain flat during polishing. Two Plexiglas blocks are placed on each side of the specimen and they are clamped together with a c-clamp.
Appendix A Specimen Preparation

Standard metallographic techniques are used for polishing the edge. Sandpaper wheels of grit 120, 180, 320 and 600 grit are successively used. The specimen is rotated by 180° between each step, to be able to see if sanding mark have changed direction, indicating that a finer grit can then be used. The final stage of the polishing is an aqueous suspension of 0.06 μm alumina. Finally, the specimen is cleaned with acetone.

The specimen edge is then carbon coated to avoid charging in the SEM of the areas uncovered by the gold grid. The deposition is done in a vacuum evaporator for twenty seconds.

The goal of the polishing is to obtain a smooth, scratch free surface which will give uniform SEM images. Overpolishing might create a corrugated surface where the fibers stick out and their edges become very bright on the SEM images. These bright edges are undesirable because they interfere with the image analysis.

A.3 Gold grid deposition

The goal of this operation is to evaporate gold through a copper mesh placed on the edge of the specimen and obtain a gold grid when the mesh is removed. The difficulty is to ensure a good contact between the mesh and the specimen. Otherwise, the gold slips under the mesh, resulting in an indistinct grid. However, the mesh is very fragile and has to be manipulated careful.

A special jig is designed for this purpose (Figure A.2). The specimen is centered in the lower part of the jig, using the shims and side screws. Two bottom screws are used to lift the specimen up. The centering of the specimen with respect to the slot in the top plate is checked.
A curved slotted plate holds the mesh, applying pressure on the mesh edges and stretching the mesh very gently when it is pressed down and flattened. Silicone rubber pieces 1 mm thick are placed on each side of the slot, on the concave side. They stick easily to the slotted plate and to the mesh, yet the bond is not too strong. Since they are compressible, they also ensure a more uniform contact and pressure on the mesh.

The mesh, protected by plastic film, is cut into a 3x35mm piece with a scalpel. Using tweezers, it is then placed carefully on the slotted plate silicone rubber, avoiding wrinkle. Then the plate is placed on the lower part of the jig, with the mesh touching the sample. The roof plate is placed above and lightly screwed (the slotted plate remains bent). Then the specimen is pushed up with the bottom screws, slowly bending the slotted plate, until there is contact.

The jig is then placed in the vacuum evaporator and gold is deposited during 15 seconds at 30 Amps. Finally, the sample is carefully removed of the jig by unscrewing the bottom screws, lowering the sample and removing the top plates.
Figure A.1 Photograph of the tab bonding jig.
Figure A.2  Photograph of the gold deposition jig.
APPENDIX B  Derivation of COD and CSD Orthotropic Equations

Sih et al. (1965) have derived the expressions for the displacement fields in an area close to the crack tip and, for the case of plane problems; they obtained, for the mode I:

\[ u = K_1 \sqrt{2r} \text{Re} \left[ \frac{1}{\mu_1 - \mu_2} \left( \mu_1 p_2 \sqrt{\cos \theta + \mu_2 \sin \theta} - \mu_2 p_1 \sqrt{\cos \theta + \mu_1 \sin \theta} \right) \right] \]  \hspace{1cm} (B.1)

\[ v = K_1 \sqrt{2r} \text{Re} \left[ \frac{1}{\mu_1 - \mu_2} \left( \mu_1 q_2 \sqrt{\cos \theta + \mu_2 \sin \theta} - \mu_2 q_1 \sqrt{\cos \theta + \mu_1 \sin \theta} \right) \right] \]  \hspace{1cm} (B.2)

and, for the mode II:

\[ u = K_{II} \sqrt{2r} \text{Re} \left[ \frac{1}{\mu_1 - \mu_2} \left( p_2 \sqrt{\cos \theta + \mu_2 \sin \theta} - p_1 \sqrt{\cos \theta + \mu_1 \sin \theta} \right) \right] \]  \hspace{1cm} (B.3)

\[ v = K_{II} \sqrt{2r} \text{Re} \left[ \frac{1}{\mu_1 - \mu_2} \left( q_2 \sqrt{\cos \theta + \mu_2 \sin \theta} - q_1 \sqrt{\cos \theta + \mu_1 \sin \theta} \right) \right] \]  \hspace{1cm} (B.4)

where

\[ p_j = a_{11} \mu_j^2 + a_{12} - a_{16} \mu_j \]  \hspace{1cm} (B.5)

\[ q_j = a_{12} \mu_j + \frac{a_{22}}{\mu_j} - a_{26} \]  \hspace{1cm} (B.6)

the complex numbers \( \mu_j \) are the roots of the characteristic equation:

\[ a_{11} \mu^4 - 2a_{16} \mu^3 + (2a_{12} + a_{66}) \mu^2 - 2a_{26} \mu + a_{22} = 0 \] \hspace{1cm} (B.7)

and \( a_{ij} \) \((i, j=1, 2, 6)\) are the 6 independent elastic constants.
Appendix B Derivation of the COD and CSD orthotropic equations

The expressions for $\mu_j$ have been obtained by Ferguson (1992) for the case of unidirectional composites. These materials are specially orthotropic, therefore the elastic constants $a_{16}$ and $a_{26}$ are zero and equation (B.7) becomes:

$$a_{11}\mu^4 + (2a_{12} + a_{66})\mu^2 + a_{22} = 0 \quad (B.8)$$

where $a_{11} = 1/E_1$, $a_{22} = 1/E_2$, $a_{12} = -\nu_{12}/E_1$ and $a_{66} = G_{12}$ for plane stress. Equation (B.8) has two complex and two wholly imaginary roots:

$$\mu_1 = \pm \sqrt{-d + e} \quad (B.9)$$
$$\mu_2 = \pm \sqrt{-d - e} \quad (B.10)$$

where $d = 2a_{12} + a_{66}$ and $e = \sqrt{(2a_{12} + a_{66})^2 - 4a_{11}a_{22}}$.

Ferguson (1992) then derived the equations for the displacements on the crack faces, behind the crack tip, at $\theta = 180^\circ$. In mode I, $u = 0$ at $\theta = 180^\circ$. Substituting equation (B.6), where $a_{26}$ is zero, into (B.2) and simplifying yields:

$$\nu = K_i \sqrt{2\pi} \text{Re} \left[ \frac{\mu_1 + \mu_2}{\mu_1\mu_2} a_{22}i \right] \quad (B.11)$$

Ferguson (1992) has shown that selecting both negative or positive roots of $\mu_1$ and $\mu_2$ will not always give real solutions. Therefore, one root must be positive and the other negative, and, after simplifications:
Appendix B Derivation of the COD and CSD orthotopic equations

\[
\mu_1 + \mu_2 = \sqrt{\frac{a_{22}}{a_{11}} - \frac{2a_{12} + a_{66}}{a_{11}}} \quad (B.12)
\]

\[
\mu_1 \mu_2 = -\frac{a_{22}}{a_{11}} \quad (B.13)
\]

\[
\nu = K_f 2\sqrt{r a_{11} a_{22}} \left[ \frac{2a_{12} + a_{66}}{2a_{11}} + \sqrt{\frac{a_{22}}{a_{11}}} \right]^\frac{1}{2} \quad (B.14)
\]

Similarly, in mode II, \( v=0 \) and \( \theta=180^\circ \) and

\[
u = K_f 2\sqrt{r a_{11} a_{22}} \left[ \frac{2a_{12} + a_{66}}{2a_{11}} + \sqrt{\frac{a_{22}}{a_{11}}} \right]^\frac{1}{2} \quad (B.15)
\]

Sih et al. (1965) also derived the relationships between the stress intensity factor and strain energy release rate for an orthotropic material:

\[
G_I = \pi K_f^2 \left[ \frac{a_{11} a_{22}}{2} \right]^\frac{1}{2} \left[ \frac{a_{22}}{a_{11}} + \frac{2a_{12} + a_{66}}{2a_{11}} \right]^\frac{1}{2} \quad (B.16)
\]

\[
G_{II} = \pi K_{II}^2 \left[ \frac{a_{11} a_{22}}{2} \right]^\frac{1}{2} \left[ \frac{a_{22}}{a_{11}} + \frac{2a_{12} + a_{66}}{2a_{11}} \right]^\frac{1}{2} \quad (B.17)
\]

Therefore, the COD and CSD can be expressed as a function of the applied \( G \):

\[
COD = 2v = \frac{4}{\sqrt{\pi}} \frac{1}{2^4} \left( a_{11} a_{22} \right)^\frac{1}{4} \left[ \frac{a_{22}}{a_{11}} + \frac{2a_{12} + a_{66}}{2a_{11}} \right]^\frac{1}{4} \sqrt{r \sqrt{G_I}} \quad (B.18)
\]

\[
CSD = 2u = \frac{4\sqrt{a_{11}}}{\sqrt{\pi}} 2^3 \left[ \frac{a_{22}}{a_{11}} + \frac{2a_{12} + a_{66}}{2a_{11}} \right]^\frac{1}{4} \sqrt{r \sqrt{G_{II}}} \quad (B.19)
\]
APPENDIX C Load Cells and Displacement Sensors Calibrations

C.1 Load cells

The load cell is attached to a vertical surface in the same way it is attached to the jig. Then increasing weights are suspended to the loading pin and the voltage is recorded. The loading and unloading was repeated 4 times for each load cells. The resulting curves are shown for the left and right load cell in Figure C.1 and Figure C.2. We can see that the calibration curves are very much linear and repeatable.

C.2 Displacement sensors

The displacement sensors calibration curves were measured after installation in the jig. The load cell was moved to different position and a LVDT was used to measure the load cell displacement while the displacement sensor voltage was measured. The loading and unloading was repeated twice. The resulting curves are shown for the left and right displacement sensors in Figure C.3 and Figure C.4. The calibration curves are very linear and repeatable.
C.3 Figures

Figure C.1 Calibration curve for the left load cell.

Figure C.2 Calibration curve for the right load cell.
Appendix C  Load Cells and Displacement Sensors Calibrations

Figure C.3  Calibration curve for the left displacement sensor.

Figure C.4  Calibration curve for the right displacement sensor.
APPENDIX D Material Properties

D.1 Determination of the elastic properties

A modified three-point bending test based on ASTM standard D790-90 (Standard Test Methods for Flexural Properties of Unreinforced and Reinforced Plastics and Electrical Insulating Materials) was used to determine simultaneously the Young’s and shear moduli (Fisher et al., 1981). Different proportions of shear and bending deformations are present at different span-to-depth ratios (SDR): shear is predominant for short SDR while bending is more important for large SDR. Taking into account both bending and shear contributions, we have:

\[ \frac{1}{k} = \frac{L^3}{4E_f wh^3} + \frac{3L}{8whG_{12}} \]  

(D.1)

where \( k \) is the slope of the load-displacement curve, \( L \) is the support span, \( h \) the specimen depth, \( w \) the specimen width, \( E_f \) is the flexural modulus in the fibre direction and \( G_{12} \) is the in plane shear modulus. It is possible to determine \( E_f \) and \( G_{12} \) by measuring \( k \) for different \( L/h \) values.

Some corrections have to be made to take into account the contribution of machine compliance and the local indentation of the specimen by the loading and reacting rollers to the specimen deflection. This contribution was determined by an indentation test were the deflection was due only to roller indentation and machine compliance, with no bending or shear stresses being applied. This is achieved placing the specimen on solid flat plate and applying a compressive stress with the loading roller. Therefore, since there is one loading roller and two reacting rollers, the corrected value \( k_{corr} \) to use in equation (D.1) is:
where \( k_{\text{measured}} \) is the slope of the load-displacement curve obtained from the three-point bending test and \( k_{\text{indent}} \) is the slope of the load-displacement curve obtained from the indentation test.

The tests were conducted on two specimens from the same plates as the ones use in the delamination tests, AS4/3501-6 and IM7/8551 respectively. Each specimen was tested for 8 different spans, taking care in remaining in the elastic region and not introduce damage. Finally, an indentation test was performed on each specimen. The results for the flexural and shear modulus of AS4/3501 and IM7/8551 are presented in Table D.1. They can be compared with the values provided by the manufacturer (Table D.2). The difference is probably due to differences in the manufacturing: the properties are affected by differences in fibre volume fraction.

Figure D.1 shows the apparent modulus, calculated using only the first term in (D.1) and therefore neglecting the shear deformation, as a function of span-to-depth ratio. As expected, it is lower than the actual flexural modulus, especially at low span-to-depth ratios.

The transverse modulus, \( E_2 \) and the Poisson’s ratio, \( \nu_{12} \), used were the ones obtained from the manufacturer, Hercules (Table D.2)

### D.2 Determination of shear strength

A short beam shear strength test has been conducted on 20 AS4/3501-6 specimens, according to the ASTM standard D2344-84 (Apparent Interlaminar Shear Strength of Parallel Fiber Composites by Short-Beam Method). The span-to-depth ratio was 4. For all 20 specimens, the
Appendix D Material Properties

failure observed was a horizontal shear failure. The shear stress was calculated from two load values: the load at which deviation from linearity occurred and the failure load. The first value is called damage strength and the second is the shear strength. The results are presented in Table D.3.

D.3 Determination of fibre volume fraction

Table D.4 presents the fibre volume fractions for the material studied obtained by matrix digestion and image analysis. For the matrix digestion method, the fibre volume fraction was measured according to the ASTM D 3171-76 standard. For the image analysis method, Scanning Electron images of a cross-section of the material were taken in the Scanning Electron Microscope, at a magnification of x800. A total of 14 images (11 for IM7-8551) were taken at equal intervals through the thickness for each specimens. The number of fibres in each image was counted manually, then multiply by the fibre cross-section area and divided by the total area of the image. The fibre diameter was provided by the manufacturer.
### D.4 Tables

**Table D.1** Flexural and shear modulus obtained experimentally for AS4/3501 and IM7/8551.

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<th>AS4/3501-6</th>
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<td>$k_{indent}$ (kN/mm)</td>
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<tr>
<td>$E_1^f$ (GPa)</td>
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<td>$G_{12}$ (GPa)</td>
<td>7.1</td>
<td>4.85</td>
</tr>
</tbody>
</table>

**Table D.2** Elastic properties for AS4/3501-6 and IM7/8551-7 provided by the manufacturer (Hercules).

<table>
<thead>
<tr>
<th></th>
<th>AS4/3501-6</th>
<th>IM7/8551</th>
</tr>
</thead>
<tbody>
<tr>
<td>$E_1$ (GPa)</td>
<td>138</td>
<td>142</td>
</tr>
<tr>
<td>$E_2$ (GPa)</td>
<td>9.96</td>
<td>8.3</td>
</tr>
<tr>
<td>$G_{12}$ (GPa)</td>
<td>7.1</td>
<td>4.55</td>
</tr>
<tr>
<td>$\nu_{12}$</td>
<td>0.3</td>
<td>0.34</td>
</tr>
</tbody>
</table>
### Table D.3  Apparent Interlaminar Damage and Shear Strength for AS4/3501-6

<table>
<thead>
<tr>
<th>Specimen thickness (mm)</th>
<th>Average value</th>
<th>Standard deviation</th>
</tr>
</thead>
<tbody>
<tr>
<td>3.53</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Specimen width (mm)</td>
<td>6.39</td>
<td></td>
</tr>
<tr>
<td>Specimen length (mm)</td>
<td>21.5</td>
<td></td>
</tr>
<tr>
<td>Damage strength (MPa)</td>
<td>67.17</td>
<td>2.53</td>
</tr>
<tr>
<td>Shear strength (MPa)</td>
<td>101.01</td>
<td>3.15</td>
</tr>
</tbody>
</table>

### Table D.4  Fibre volume fractions measured by matrix digestion and image analysis.

<table>
<thead>
<tr>
<th>Method</th>
<th>AS4/3501-6 (specimens B)</th>
<th>AS4/3501-6 (specimens R)</th>
<th>IM7/8551 (specimens T)</th>
</tr>
</thead>
<tbody>
<tr>
<td>digestion</td>
<td>$V_f$</td>
<td>56.9</td>
<td>67.4</td>
</tr>
<tr>
<td></td>
<td>(standard deviation)</td>
<td>(1.00, 3 samples)</td>
<td>(0.00, 2 samples)</td>
</tr>
<tr>
<td>image analysis</td>
<td>Fibre diameter</td>
<td>7 μm</td>
<td>7 μm</td>
</tr>
<tr>
<td></td>
<td>$V_f$</td>
<td>60.3</td>
<td>65.1</td>
</tr>
</tbody>
</table>
Figure D.1  Apparent flexural modulus for AS4/3501 and IM7/8551 as a function of span to depth ratio. The apparent modulus is lower than the actual value because the shear deformation has been neglected, especially at low span-to-depth ratios.
APPENDIX E Additional Results

All the results not presented in the main body of the thesis because they were repetitious or inconclusive are included here.

E.1 Additional mode I results

E.1.1 Load-displacement curves

The global load versus global displacement curves for specimens B1 (crack lengths $a_1$, $a_2$ and $a_3$) and T4 are presented in Figure E.1 to Figure E.5. The figures include the points where COD profiles were generated and the points at which delamination growth was observed in the SEM. These curves show a linear relationship between the load and displacement for both the brittle (B1) and tough (T4) material. A abrupt drop in the load with crack growth is noticeable on Figure E.3, since the crack growth is large enough (2 mm) to have an effect. It is not noticeable on Figure E.2 and Figure E.5, since the amount of crack growth detected in the SEM is very small.

The load-displacement curve for the mode I crack propagation test is shown in Figure E.6, with the points where COD profiles were generated and the points at which delamination growth was observed in the SEM. The result is typical of DCB crack propagation tests performed in a standard load frame.

E.1.2 Singularity zone size in the brittle mode I test

COD profiles were generated for 10 load levels covering 3 crack lengths on a brittle specimen, as presented in section 4.1. In order to show the singular zone size, the COD$^2$ vs. $r$ can be plotted. Moreover, the CODs are proportional to the square root of $G_{II}$. Thus by normalizing the COD$^2$
Appendix E Additional Results

with respect to $G_{IL}$, all the profiles should fall on the same curve. Figure E.7 to Figure E.9 show plots of $COD^2/G_{IL}$ for crack lengths $a_1$ to $a_3$. As seen from these figures, the plots are linear with $r$ for a region that extends to approximately 500 microns behind the crack tip, for almost all $G_{IG}$. For crack length $a_3$ and $G_{IG}$ of 81 and 41 J/m$^2$, the region seems to be smaller: approximately 300 and 400 microns respectively.

We can therefore conclude that for most profiles, the singularity zone is roughly 500 microns.

E.1.3 Results of preliminary mode I resistance curve tests

The objective of these tests is to study if the increase in mode I delamination toughness is related to a reduction in crack tip opening due to fibre bridging by comparing $G_{IG}$ to $G_{IL}$.

E.1.3.1 Tests description

The specimen was loaded in mode I until crack growth was observed in the SEM, then immediately partially unloaded. This process was repeated until the crack length was close to 40 mm. Two AS4/3501 specimens were tested: B3 and B5 (see Table E.1). For 3 crack lengths on B3 ($c_1$ to $c_3$), and 6 crack lengths on B5 ($c_1$ to $c_6$), slow scan images are recorded, at $G_{IG}$ levels varying between 74 and 84 J/m$^2$. After the test, the COD profiles are measured from the slow scan images.

E.1.3.2 Results

The R-curve obtained for specimen B3 and B5 are presented in Figure E.10 and Figure E.11. We can see that there is an increase of $G_{IG}$ with crack growth, but it is less pronounced than the
increase observed in the high volume fraction specimen R1 (see Chapter 4). The R-curve increase is more important for specimen B3 than for B5.

The COD profiles are presented in Figure E.12 to Figure E.14 for crack lengths c₁ to c₃ of specimen B3, and in Figure E.15 to Figure E.20 for crack lengths c₁ to c₆ of specimen B5. Where there is good agreement between $G_{IL}$ and $G_{IG}$, the COD profile predicted analytically is plotted as a solid line. Otherwise, the dotted lines represent the same equation, but the value of $G_{IL}$ is adjusted to obtain a better fit to the COD values. Table E.2 and Table E.3 shows the values of $G_{IL}$ and $G_{IG}$ for the different crack lengths, for specimen B3 and B5, respectively. Those values are presented graphically in Figure E.21. As we can see, we have good agreement between $G_{IG}$ and $G_{IL}$ in most cases, except B3 c₁ and B3 c₃.

In the case of B3 c₁, the crack has not been grown yet and therefore, there is no fibre bridging to explain the reduction in the $G_{IL}$. However, we have seen previously with B1 a₁ (no crack growth) for $G_{IG}$=30 J/m² (see Chapter 4) that there was a similar reduction during the initial loading, and that it disappeared subsequently. This was attributed to the sticking of the starter film to the crack faces, preventing the crack from opening. Figure E.22 shows how fibre bundles in the Teflon insert are interlocked with the specimens matrix. This might reduce the opening until sufficient load is applied to overcome the interlocking forces.

B3 c₃ is the longest crack growth. Therefore, the effect of fibre bridging is maximum and it explains the difference between $G_{IL}$ and $G_{IG}$.

For all the other cases, $G_{IG}$ and $G_{IL}$ are equal. We would expect some difference to appear as crack growth increases. However, we observe that the increase in $G_{IG}$ with crack growth is not
that pronounced, especially in B5. Moreover, the applied $G_{IC}$ is always less than 84 J/m$^2$, and as we have seen with specimen R1 (see Chapter 4), the difference between $G_{IL}$ and $G_{IC}$ becomes noticeable when $G_{IC}$ is higher. This is explained by the fact that the fibre bridges become tight only above a certain load level.

**E.1.3.3 Summary**

The effect of fibre bridging is noticeable, but not evident, in specimen B3 and not visible at all in B5. This is because the increase in toughness with crack growth is not steep enough. Also, the load levels that we can apply to take slow scan images are limited to the 80-90 J/m$^2$ range, otherwise we get too close to $G_{IC}$ and the crack might grow. Therefore, it was decided to use a specimen which exhibits more fibre bridging and toughness increase: specimen R1 (see Chapter 4) has a higher volume fraction and therefore, more fibre bridging and a more pronounced R-curve. The results obtained with specimen B3 and B5 are consistent with the results obtained from specimen R1, but much less conclusive.

**E.2 Additional mode II results**

**E.2.1 Load-displacement curves**

The global load versus global displacement curves for specimens B8 (crack lengths $a_1$, $a_2$ and $a_3$), B11 and B13 are presented in Figure E.23 to Figure E.27. The figures include the points where CSD profiles were generated and the points at which unstable delamination growth was observed in the SEM. For specimen B8 and B11, the curves show a linear relationship between the load and displacement. A abrupt drop in the load with crack growth is noticed when unstable crack
growth occurred. However, for specimen B13, there is a deviation from a linear load-displacement curve prior to unstable crack growth. This can be explained by the larger coalescence growth in this specimen, which reduces the compliance.
### E.3 Tables

#### Table E.1  Characteristics of specimens B3 and B5

<table>
<thead>
<tr>
<th></th>
<th>Specimen B3</th>
<th>Specimen B5</th>
</tr>
</thead>
<tbody>
<tr>
<td>material</td>
<td>AS4/3501-6</td>
<td>AS4/3501-6</td>
</tr>
<tr>
<td>$V_J$ (%)</td>
<td>59*</td>
<td>59*</td>
</tr>
<tr>
<td>grid spacing (µm)</td>
<td>50.8</td>
<td>50.8</td>
</tr>
<tr>
<td>$h$ (mm)</td>
<td>1.77</td>
<td>1.76</td>
</tr>
<tr>
<td>$B$ (mm)</td>
<td>19.61</td>
<td>19.82</td>
</tr>
<tr>
<td>$L$ (mm)</td>
<td>145.5</td>
<td>137.5</td>
</tr>
<tr>
<td>$a$ (mm)</td>
<td>18.7 ($c_1$)</td>
<td>18.7 ($c_1$)</td>
</tr>
<tr>
<td></td>
<td>19.8 ($c_2$)</td>
<td>19.4 ($c_2$)</td>
</tr>
<tr>
<td></td>
<td>39.6 ($c_3$)</td>
<td>22.0 ($c_3$)</td>
</tr>
<tr>
<td></td>
<td></td>
<td>24.1 ($c_4$)</td>
</tr>
<tr>
<td></td>
<td></td>
<td>29.6 ($c_5$)</td>
</tr>
<tr>
<td></td>
<td></td>
<td>37.7 ($c_6$)</td>
</tr>
</tbody>
</table>

*Note: $V_J$ determination methods described in Appendix D

#### Table E.2  $G_{IL}$ (J/m$^2$) for the three crack lengths of B3.

<table>
<thead>
<tr>
<th>Crack length (mm)</th>
<th>$G_{IG}$ (J/m$^2$)</th>
<th>$G_{IG}$ (J/m$^2$)</th>
<th>$G_{IL}$ (J/m$^2$)</th>
</tr>
</thead>
<tbody>
<tr>
<td>18.7 ($c_1$)</td>
<td>105</td>
<td>83</td>
<td>50</td>
</tr>
<tr>
<td>19.8 ($c_2$)</td>
<td>120</td>
<td>74</td>
<td>71</td>
</tr>
<tr>
<td>39.6 ($c_3$)</td>
<td>173</td>
<td>81</td>
<td>60</td>
</tr>
</tbody>
</table>
### Table E.3 $G_{IL}$ (J/m$^2$) for the five crack lengths of B5.

<table>
<thead>
<tr>
<th>Crack length (mm)</th>
<th>$G_{ICG}$ (J/m$^2$)</th>
<th>$G_{IG}$ (J/m$^2$)</th>
<th>$G_{IL}$ (J/m$^2$)</th>
</tr>
</thead>
<tbody>
<tr>
<td>18.7 (c$_1$)</td>
<td>89</td>
<td>74</td>
<td>74</td>
</tr>
<tr>
<td>19.4 (c$_2$)</td>
<td>114</td>
<td>72</td>
<td>72</td>
</tr>
<tr>
<td>22.0 (c$_3$)</td>
<td>121</td>
<td>83</td>
<td>83</td>
</tr>
<tr>
<td>24.1 (c$_4$)</td>
<td>135</td>
<td>75</td>
<td>75</td>
</tr>
<tr>
<td>29.6 (c$_5$)</td>
<td>146</td>
<td>81</td>
<td>81</td>
</tr>
<tr>
<td>37.7 (c$_6$)</td>
<td>160</td>
<td>84</td>
<td>84</td>
</tr>
</tbody>
</table>
E.4 Figures

Figure E.1 Load-displacement curve for specimen B1 (a₁) showing the points where the COD profiles were taken (first loading ramp on Figure 4.1).

Figure E.2 Load-displacement curve for specimen B1 (a₁, a₂) showing the points where the COD profiles were taken (second loading ramp on Figure 4.1).
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Figure E.3  Load-displacement curve for specimen B1 (a₂) showing the points where the COD profiles were taken (third loading ramp on Figure 4.1).

Figure E.4  Load-displacement curve for specimen B1 (a₃) showing the points where the COD profiles were taken (fourth loading ramp on Figure 4.1).
Figure E.5 Load-displacement curve for specimen T4 showing the points where the COD profiles were taken (loading ramp on Figure 4.23).
Figure E.6  Load-displacement curve for specimen R1 showing the points where the COD profiles were taken and where crack growth occurred.
Figure E.7 Plot of $\frac{COD^2}{G_{II}}$ for specimen B1 a1 showing the square root dependency zone.
Figure E.8 Plot of $\frac{\text{COD}^2}{G_{\text{IL}}}$ for specimen B1 a$_2$ showing the square root dependency zone.
Figure E.9 Plot of $\frac{COD^2}{G_{II}}$ for specimen B1 a$_3$ showing the square root dependency zone.
Appendix E Additional Results

Figure E.10 R-curve measured for specimen B3.

Figure E.11 R-curve measured for specimen B5.
Appendix E Additional Results

Figure E.12 Plot of COD vs. r (distance from the crack tip) for $G_{IG} = 83 \text{ J/m}^2$ on specimen B3 (c₁). Solid line shows COD profile calculated from $G_{IL} = G_{IG}$. Dashed line shows COD profile calculated from $G_{IL}$ that gives the best fit.

Figure E.13 Plot of COD vs. r (distance from the crack tip) for $G_{IG} = 74 \text{ J/m}^2$ on specimen B3 (c₂). Solid line shows COD profile calculated from $G_{IL} = G_{IG}$.
Figure E.14 Plot of COD vs. r (distance from the crack tip) for $G_{IG} = 81 \text{ J/m}^2$ on specimen B3 ($c_3$). Solid line shows COD profile calculated from $G_{IL} = G_{IG}$. Dashed line shows COD profile calculated from $G_{IL}$ that gives the best fit.

Figure E.15 Plot of COD vs. r (distance from the crack tip) for $G_{IG} = 89 \text{ J/m}^2$ on specimen B5 ($c_1$). Solid line shows COD profile calculated from $G_{IL} = G_{IG}$. 
Appendix E Additional Results

Figure E.16 Plot of COD vs. $r$ (distance from the crack tip) for $G_{IG} = 72 \text{ J/m}^2$ on specimen B5 ($c_2$). Solid line shows COD profile calculated from $G_{IL} = G_{IG}$.

Figure E.17 Plot of COD vs. $r$ (distance from the crack tip) for $G_{IG} = 83 \text{ J/m}^2$ on specimen B5 ($c_3$). Solid line shows COD profile calculated from $G_{IL} = G_{IG}$.
Figure E.18 Plot of COD vs. \( r \) (distance from the crack tip) for \( G_{IG} = 75 \text{ J/m}^2 \) on specimen B5 \((c_4)\). Solid line shows COD profile calculated from \( G_{IL} = G_{IG} \).

Figure E.19 Plot of COD vs. \( r \) (distance from the crack tip) for \( G_{IG} = 81 \text{ J/m}^2 \) on specimen B5 \((c_5)\). Solid line shows COD profile calculated from \( G_{IL} = G_{IG} \).
Figure E.20 Plot of COD vs. r (distance from the crack tip) for $G_{IG} = 81$ J/m$^2$ on specimen B5 ($c_6$). Solid line shows COD profile calculated from $G_{IL} = G_{IG}$. 
Figure E.21 Plot of $G_{IL}$ vs. $G_{IG}$ for specimen B3 and B5. Dotted line represents a one-to-one correspondence.
Figure E.22 Pictures showing the interlocking of the Teflon insert fibre bundles and the composite matrix, for specimen B3.
Appendix E Additional Results

Figure E.23 Load-displacement curve for specimen B8 (\(a_1\)) showing the points where the COD profiles were taken (loading ramp on Figure 5.1).

Figure E.24 Load-displacement curve for specimen B8 (\(a_2\)) showing the points where the COD profiles were taken (loading ramp on Figure 5.1).
Appendix E  Additional Results

Figure E.25 Load-displacement curve for specimen B8 (a3) showing the points where the COD profiles were taken (loading ramp on Figure 5.1).

Figure E.26 Load-displacement curve for specimen B11 showing the points where the COD profiles were taken (loading ramp on Figure 5.2).
Figure E.27 Load-displacement curve for specimen B13 showing the points where the COD profiles were taken (loading ramp on Figure 5.3).