Mixed-mode delamination of multidirectional composite laminates at 0°/θ° ply interfaces

P. Prombut 1, L. Michel 1, and J.J. Barrau 2

1 Department of Mechanical Engineering, ENSICA
1 Place Emile Blouin, 31056 Toulouse Cedex, France.
2 Department of Mechanical Engineering, University of Toulouse III
118 Route de Narbonne, 31062 Toulouse Cedex, France.
pprombut@ensica.fr

ABSTRACT
The objective of this study is to determine the mixed-mode I+II interlaminar fracture toughness at high mode I content for a 0°/45° ply interface. Mixed-mode I+II delamination tests on unidirectional and multidirectional specimens were performed using ADCB and AMMF methods. The procedure used for choosing the stacking sequence resulted in desirable propagation behaviors of the delamination. There was no change of delamination plane, an acceptable crack front profile, no initial specimen curvature, and no energy dissipation through global specimen damage. For data reduction, the experimental compliance method was the most consistent in determining the total energy release rate. Finite element simulation proved to be an indispensable analysis tool. A resin-rich interlayer was modeled at the delamination interface to eliminate the non-convergence behavior of the energy release rate. For delamination between plies of different orientations, the mode decomposition could only be analyzed by the finite element method. The ADCB configuration offered a very high mode I content (90-95%) while the AMMF offered a moderately high mode I (70-78%).

1. INTRODUCTION
Delamination is one of the most common modes of failure in continuous fiber-reinforced polymer-matrix composite laminates. The vast majority of work on this subject has focused on the evaluation of the critical energy release rate (ERR) in unidirectional (UD) laminates [1,2]. Further studies are required to improve the understanding of the delamination between plies of different orientations.
Mode II fracture toughness ($G_{IIc}$) of a 0°/45° ply interface has been determined using End Notched Flexure (ENF) [1] and End Loaded Split (ELS) [3] tests. These specimens with a mid-plane delamination could be used for mixed-mode I+II tests such as Mixed-Mode Flexure (MMF) [4,5] and Mixed-Mode Bending (MMB) [6]. The conventional Double Cantilever Beam (DCB) test, however, cannot be used to determined mode I fracture toughness ($G_{Ic}$) because non-unidirectional interfaces suffer the change of delamination plane [1,7], which invalidate the test results.
The objective of this study is to determine the mixed-mode I+II interlaminar fracture toughness ($G_c$) at high mode I content for a 0°/45° ply interface. A stacking sequence must be determined such that common concerns in delamination test of multidirectional (MD) specimens [7] are minimized. The experimental results have to be analyzed with suitable data reduction and finite element methods.
The Asymmetric Double Cantilever Beam (ADCB) [8,9] and Asymmetric Mixed-Mode Flexure (AMMF) [4,5] methods were used for the delamination tests in this study. First, the methods for determining the ERR are presented. The following section deals with the stacking sequence and experimental procedures. Next, the finite element modeling is described. The validity of the tests is evaluated before the discussion of the tests and analysis results.
2. DETERMINATION OF THE CRITICAL ENERGY RELEASE RATE

Extensive work on the data reduction of delamination tests has been reported in the literature. For our asymmetric mixed-mode tests, the experimental compliance method [10] and the beam theory [4,8] were used to determine the total ERR. The mode partitioning method was based on the relation between the energy release rate and the stress intensity factor [8,11]. It is usually referred to as a local method.

In addition, the critical ERR can be determined as follows:

2.1. Theoretical compliance determination

For laminates without a coupling in-plane/out-of-plane deformation, the moment-curvature relations can be described by the classical lamination theory [12]. In our delamination tests, \( M_y = M_{xy} = 0 \). The relations can be reduced to:

\[
M_x = D \cdot \kappa_x = -D \cdot \frac{\partial^2 w}{\partial x^2}, \quad D = D_{11} - \frac{D_{16}^2}{D_{66}} \left( \frac{D_{12} - D_{16}D_{26}}{D_{66}} \right)^2
\]  

(1a,1b)

where \( D \) is the bending stiffness of the laminate. \( D_{ij} \) is a component in the stiffness matrix.

Equation (1a), together with appropriate boundary conditions, can be solved for the deflection, \( w \), as a function of the applied force. The theoretical compliance can then be determined from the relation \( C = w/P \). The total ERR is obtained from equation (2).

\[
G_t = \frac{P^2}{2B} \cdot \frac{dC}{da}
\]

(2)

2.2. Composite beam theory

For a composite beam with \( n \) plies at various angles and a cross-section in the YZ plane, the characteristics of the beam can be defined as follow:

\[
Y_p = \sum_{i=1}^{n} \frac{1}{E_i S_i} \int \int E_i y^2 dS, \quad E_0 I_{pc} = \sum_{i=1}^{n} \int \int E_i y^2 dS
\]

(3a,3b)

where \( Y_p \) is the neutral level of the section. \( y \) is the ply position (in height direction). \( S_i \) is the ply section area. \( E_i \) is the ply modulus in X-direction. \( E_0 I_{pc} \) is the effective bending stiffness.

The original beam theory equation [4] is still valid for MD lay-ups, provided that the bending stiffness, \( EI \), in the original equation is replaced by the effective bending stiffness, \( E_0 I_{pc} \), obtained from equation (3b). Thus we have:

\[
G_t = \frac{1}{B} \left( \frac{M_1^2}{2(E_0 I_{pc})_1} + \frac{M_2^2}{2(E_0 I_{pc})_2} - \frac{(M_1 + M_2)^2}{2(E_0 I_{pc})_3} \right)
\]

(4)
3. EXPERIMENTS

3.1. Determination of stacking sequence

Stacking sequences for a delamination interface of $0^\circ/45^\circ$ were evaluated to minimize common concerns in fracture toughness test of multidirectional specimens [7]. A specimen is considered to consist of three parts. Arm 1 and arm 2 are in the cracked region. The uncracked part is called arm 3. The three arms must have no in-plane/out-of-plane coupling i.e. the matrix $B$ of the laminate is a zero matrix. The in-plane extensional/shear coupling must also be eliminate ($A_{16} = A_{26} = 0$). Stacking sequences without the aforementioned couplings were then determined and selected.

Two non-dimensional parameters, $D_c$ and $B_t$, are used to assess the ERR distribution across the specimen width. The $D_c$ indicates the curvature due to longitudinal/transverse bending coupling. It is determined using equation (5a) with $D_c = 0.25$ as an upper bound [13]. The $B_t$ is defined in equation (5b) [14]. This parameter indicates the skewness of the crack profile due to bending/twisting coupling of the specimen arms. It is recommended that $B_t$ be kept minimum.

$$D_c = \frac{D_{12}^2}{D_{11}D_{22}}, \quad B_t = \frac{D_{16}}{|D_{11}|} \quad (5a,5b)$$

Residual stresses arise from differential thermal shrinkage of the plies when the laminate is cured at high temperature and then cooled down to room temperature. The residual effect disappears if both arms of a specimen are symmetric laminates, which do not curve by residual stresses. Nairn [15] provided a complete formulation with examples thus any lay-up can be tested for the residual stress effect.

The ply characteristics of the T700/M21 composite are shown below. $E_{1f}$ is a flexure modulus determined from existing DCB and ELS test results. The UD prepregs is made by Hexcel Composites using T700GC carbon fiber and M21 epoxy resin. This prepregs has 35% of resin content by weight.

$$E_{1f} = 98.62, \ E_{22} = 7.69, \ G_{12} = 4.75 \text{ GPa}, \ \nu_{12} = 0.33, \ \text{Cured ply thickness} 0.260 \text{ mm}.$$

The best resulting stacking sequence in terms of $D_c$ and $B_t$ is shown below, with “//” indicating the delamination interface. The values of $D_c$, $B_t$, and residual stress effect are shown in Table 1. After the polymerization, the laminate was placed on a flat surface. No curvature was observed by visual inspection.

$$0/45/-45/-45/45/0 // 45/0/-45/0/-45/-45/45/0/45/0/-45$$

Table 1: $D_c$, $B_t$, and residual stress effect of the multidirectional specimens.

<table>
<thead>
<tr>
<th></th>
<th>Arm 1 (12 plies)</th>
<th>Arm 2 (6 plies)</th>
<th>Arm 3 (18 plies)</th>
</tr>
</thead>
<tbody>
<tr>
<td>$D_c$</td>
<td>0.181</td>
<td>0.063</td>
<td>0.187</td>
</tr>
<tr>
<td>$B_t$</td>
<td>0.000</td>
<td>0.064</td>
<td>0.003</td>
</tr>
<tr>
<td>Residual stress effect</td>
<td>0%</td>
<td>0%</td>
<td>0%</td>
</tr>
</tbody>
</table>

Note: $D_c$ and $B_t$ of the unidirectional specimens are 0.008 and 0.000 respectively.
3.2. Experimental procedure
Composite laminates were made by stacking 18 plies of the UD prepreg. An aluminium foil (13 µm) or a Teflon release film (12 µm) was used as an insert to create the initial delamination. The test data was taken 5 mm away from the insert tip hence the influence of insert type was ignored. The lay-ups were then polymerized in a thermo-regulated hydraulic press under 6 bars at two temperature steps: 135°C for 40 minutes and 180°C for 120 minutes. The temperature ramp was ± 3°C/min. The nominal specimen dimensions are 170x20x80 mm (Length x Width x Initial crack length) for the ADCB test and 140x20x80 mm for the AMMF test. The resulting multidirectional specimens are called ADCB_MD and AMMF_MD while the unidirectional specimens are referred to as ADCB_UD and AMMF_UD. Figure 1 shows typical ADCB and AMMF test configurations. The thinner arm (arm 2) is attached to the upper fixture and displays higher curvature than the thicker arm. The crosshead speed of all tests was 0.5 mm/min during delamination propagation.

Figure 1: Test configurations. (a) ADCB test. (b) AMMF test.

4. FINITE ELEMENT MODELING
4.1. Distribution of energy release rate
Three-dimensional finite element models were used to determine the ERR distribution across the width of the specimens. A typical model is shown in Figure 2. The model is created with 3D composite 20-node volume elements. The longitudinal mesh refinement around the crack front is made such that the element length is equal the cured ply thickness. The refined zone is three times longer than the total thickness of the specimen. Four plies around the crack plane, in thickness direction, are modeled individually. The load blocks are modeled with rigid bodies. The ERRs are determined using a specific virtual crack extension (VCE) method [16] which is a built-in function of the finite element code SAMCEF used for the simulation.

4.2. Critical energy release rate and mode partitioning
The values of the critical energy release rate were determined from two-dimensional finite element models using crack lengths and their corresponding critical forces from the experimental data. The models were created with 8-node elements under a plane strain assumption. Each ply of the specimens was modeled individually along the thickness. The longitudinal meshing was identical to the 3D models. For mixed-mode delamination between plies with dissimilar orientation, the ERR components given by finite element simulation do not converge. [17-19]. The mismatch of material properties across the interface leads to an oscillatory characteristics of the stress and displacement fields near the delamination front [18]. Among the approaches
for eliminating the oscillatory singularity [17-20], we have selected the “resin interlayer approach” proposed by Raju et al [17]. In this method, a thin resin layer is modeled above and below the delamination plane. A crack is assumed to locate between these homogeneous isotropic layers. As a result, the oscillation vanished [19]. The resin interlayer approach perhaps provides the most physically appealing results, provided that physically realistic values of resin thickness and modulus are used [20]. Resin layers with the thickness of 0.013 mm were added above and below the delamination plane. The flexural modulus of the cured resin is given by Hexcel Composites as 3500 MPa. Four plies around the delamination were refined to have the element thickness of 0.0325 mm. The longitudinal refined zone was modified such that the element width becomes 0.013 mm, to have square elements around the delamination tip. See Figure 3.

5. VALIDATION OF STACKING SEQUENCE
5.1. Energy release rate distribution and change of delamination plane

Before analyzing the test results, validation procedures were performed on the tested specimens. Figure 4 shows the fracture surfaces of an ADCB_UD specimen, on the left, and an ADCB_MD specimen, on the right. The crack fronts are highlighted for clearness. The crack profile of the ADCB_UD is flat and quite symmetric. The ADCB_MD has a more curved and less symmetric profile. The loss of fiber bundles due to fiber bridging can be seen as dark vertical lines on these zero-degree surfaces. There is no indication of delamination migration.

Figure 2: Typical 3D finite element model of a test specimen.

Figure 3: Mesh refinement of a 2D model with resin interlayer.

Figure 4: Fracture surfaces of an ADCB_UD specimen (left) and an ADCB_MD specimen (right).

Figure 5: Normalized total energy release rates of the specimens.
Figure 5 shows the ERR distributions from 3D FE analysis. The two symmetric curves (hollow marks) are from ADCB_UD and AMMF_UD specimens. Qualitatively, the differences between the UD and MD distributions correspond well with the crack front profiles observed in Figure 4.

5.2. Global damage of the specimens
An elasto-plastic damageable material behavior has been implemented, through a specific material routine, into SAMCEF [21]. This material model is capable of predicting the degradation of the laminate i.e. a progressive decrease of the elastic modulus, and the plastic deformation which results in residual displacements. When the material model is applied to our fracture toughness models, neither loss of rigidity nor residual displacement could be observed in the resulting force-displacement diagram. Therefore, the specimen is free of global ply damage and no energy is dissipated through this mechanism.

6. RESULTS AND DISCUSSION
6.1. Force-displacement results
The results of each test configuration were obtained by averaging the critical forces and displacements corresponding to each crack length. The average forces in the delamination range of 85-105 mm were selected for finite element simulations. The correlation between the FE and the experimental results was extremely good for the UD tests. For the MD configurations, a reasonable correlation was observed. The displacements predicted by the FE simulations were lower than the experimental results. The average differences for ADCB_MD and AMMF_MD are 12% and 8% respectively.

6.2. Convergence of energy release rates
The variation of the ERRs from the “resin layer” model and the “bare interface” model (the model without resin interlayer) is shown in Figure 6. The ADCB_MD configuration was used for this study. The $G_i$ values, normalized with the converged total ERR, are plotted against the element sizes at the crack tip. Contrary to the literature [17-19], $G_i$ of the bare interface model also varies with the mesh size. For the resin layer model, the total ERR and its individual components converge as the element size decreases. The ERRs obtained from the Virtual Crack Closure Technique (VCCT) [22] have similar variation, but our VCE method predicts slightly more conservative values.

![Figure 6: Comparison of ERR components from resin-layer and bare-interface models: Mode I and total ERRs (left), mode II ERRs (right).](image)

The ERR values can be slightly influenced by the thickness of the resin interlayer. From observations of our laminates, very thin resin-rich layers with an average thickness
around 0.020 mm exist between neighboring plies. The resin thickness of 0.010 mm modeled on each size of the delamination would therefore be physically realistic. The model, however, become very heavy for our computing resource when square elements at the crack front and a smooth mesh transition were imposed. The resin thickness of 0.013 mm was then chosen for our FE modeling. The total ERR difference of less than 1% when the resin thickness was changed from 0.010 to 0.013 mm was considered as insignificant.

6.3. Comparison of total energy release rate

Figure 7 compares the total energy release rates determined by experimental, analytical, and finite element methods. The experimental methods include a simplified area method (Area) and the experimental compliance method (CC_EXP). For the analytical methods, the theoretical compliance determination (CC_PT) and the composite beam theory (BT_C) methods are presented in section 2. The beam theory method (BT) is used as well. The finite element results (FEA) are plotted in gray color.

Average forces, displacements, and their corresponding crack lengths were used in the analytical and finite element analyses. For the CC_EXP method, the individual ERRs of the specimens in each test configuration were averaged to obtain the values plotted in Figure 7. The dispersion of one standard deviation about the average ERRs can therefore be presented. The Area method serves as a guideline for the $G_t$ values. The ERRs from this method oscillate around the values from other methods. An excellent correlation of all analysis methods can be found for UD tests. The differences between the methods are well within the range of the dispersion. The total ERR results can then be considered reliable. As for the MD tests, the in-plane properties of the lay-up, obtained by the CLT, were used in the BT analysis. However, this approach does not produce good results since the total ERR values differ greatly from all other methods.

For the ADCB_MD, a good agreement is observed among the Area, CC_EXP, CC_PT, and BT_C methods. The FEA method predicts the ERRs of about 10% lower, reflecting
its smaller displacement predictions. The ERR results of the AMMF_MD have larger
dispersion than the other configurations. At the points where the dispersions are
relatively small, the FEA predicts ERRs that are about 10% lower the CC_EXP results.
The difference is once again consistent with the displacement predictions. The $F-\delta$
loading paths of the AMMF_MD were noticeably nonlinear. Consequently, the accuracy
of the Area, CC_PT, and BT_C methods might be affected by this non-linearity.
The total ERRs of the $0^\circ/0^\circ$ and $0^\circ/45^\circ$ ply interfaces can now be compared using the
CC_EXP results. In the ADCB configuration, the $0^\circ/45^\circ$ interface produces about 10%
lower ERR and slightly steeper R-curve. The more significant difference found in the
AMMF configuration could be attributed to fiber bridging and moment arm shortening.
The AMMF_MD is the most susceptible to these two phenomena since it has the largest
opening displacement. Its steep R-curve can be observed in Figure 7. Considerable
dispersion raises the question of suitability of the MD lay-up for the AMMF test.

6.4. Mode decomposition
The ratios $G_I/G_t$ from the local and finite element methods are compared in Figure 8.
Similarly to the BT method with MD specimens, the local method uses in-plane
laminate properties of the MD lay-up. The three arms of our MD specimens have
identical in-plane properties, so the mode ratio depends only on the thickness ratio of
the specimens. Under the same test configuration, the local method resulted in no
difference between the partitioning of UD and MD specimens. The resulting mode
ratios of the MD specimens are questionable because the influence of the stacking
sequence on the bending stiffness is not taken into account.

![Figure 8: Mode partitioning by local and finite element methods.](image)

The FE predictions for the UD tests are in reasonable agreement with the local method.
The difference in the AMMF_UD is greater than in the ADCB_UD due to higher non-
linearity of the AMMF configuration. While non-linearity is taken into account in the
FE simulation, it is neglected by the local method. Based on the correlation of the UD
results, we considered both partitioning methods to be reliable. The local method is,
however, more sensitive to non-linearity. To the authors’ knowledge, the mode
partitioning of delamination between plies of different orientations cannot be done
analytically. Thus, finite element simulation is the only means to obtain the mode
decomposition. The local method can be used as an analytical alternative only for
unidirectional lay-ups.
7. CONCLUSION

Mixed-mode I+II delamination tests on UD and MD specimens were performed using ADCB and AMMF methods. The procedure used for choosing the MD stacking sequence in this present work resulted in desirable propagation behaviors of the delamination. There was no change of delamination plane, an acceptable crack front profile, no initial specimen curvature, and no energy dissipation through global specimen damage.

The experimental results were analyzed using various analytical methods. The experimental compliance method seemed to be the most consistent in determining the total energy release rate. The theoretical compliance determination and the composite beam theory methods can be used with confidence when the \( F-\delta \) loading path is linear. These two analytical methods are applicable for both UD and MD lay-ups while the BT method can only be used with UD lay-ups.

Finite element simulation proved to be an indispensable analysis tool. When the FE force-displacement response correlated well with the experimental results, the total ERRs agreed very well with those obtained from the analytical methods. A resin-rich interlayer was modeled at the delamination interface to eliminate the non-convergence behavior of the ERRs. For delamination between plies of different orientations, the mode decomposition could only be analyzed by the finite element method. The beam theory-based local method could be used as an analytical alternative only for unidirectional lay-ups.

The ADCB configuration offered a very high mode I content (90-95%) while the AMMF offered a moderately high mode I (70-78%) for mixed-mode I+II delamination studies. The results from this work can be combined with results at lower mode I ratios and pure mode II to produce a delamination propagation criterion for the 0°/45° interface. The criterion can then be extrapolated to determine the pure mode I fracture toughness.

REFERENCES


