Cryogenic Damage and Fracture Behaviors of G-11 Woven Glass-Epoxy Laminates*

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We discuss the cryogenic damage and fracture behaviors of G-11 woven glass-epoxy laminates. In conjunction with the cryogenic fracture toughness test, a finite element analysis was conducted to predict the fracture and deformation for models of the compact tension (CT) specimens. Effective elastic moduli were determined under the assumption of uniform strain inside the representative volume element. Hoffman's criterion was selected as the criterion for fiber-dominated failure, and matrix cracking was detected by the maximum strain criterion. The virtual crack extension method was adopted to calculate strain energy release rate which leads to determination of stress intensity factor. In order to verify the model, correlations between experimental and analytical results were made, in terms of the load-displacement response and the extent of damage growth. Reasonable agreements between the calculations and the experimental data were achieved.

Key Words: Fracture Mechanics, Damage Mechanics, Finite Element Method, Material Testing, Composite Material, Fracture Criterion, Fracture Toughness, Cryomechanics, Compact Tension Specimen, Superconducting Magnet

1. Introduction

Due to the attractive combination of mechanical, thermal, and electrical properties, polymeric composites have been used widely for load-bearing structural, thermal and electrical insulation purpose in cryogenic engineering applications such as in the construction of large superconducting magnet systems in the magnetic fusion energy (MFE) generation^{(1),(2)}. Additionally, the need for high strength and low weight materials has made polymeric composites ideal for use in aerospace applications, which often require the storage and transportation of cryogenic liquids^{(3),(4)}. To ensure their reliability in structural design, it is essential to understand the cryogenic fracture behavior of polymeric composites.

Polymeric composites are often fabricated using a glass fabric reinforcement. These woven composites are more complicated to evaluate than traditional composite

materials because of the interlaced yarns, but there are potential advantages. Some of these are an increased resistance to impact, the ability of "near-net-shape" manufacturing, and resistance to damage, such as delamination⁽⁵⁾. Recently, Shindo et al.⁽⁶⁾ carried out plane-strain fracture toughness (K_{IC}) tests with 8-mm-thick CT specimens at room temperature, liquid nitrogen temperature (77 K) and liquid helium temperature (4 K) to evaluate the fracture toughness and the temperature rise of woven glass-epoxy laminates, and studied the damage zone at the notch tip. It was found that the damage zone consists of broken and delamination fiber, fiber pull-out and broken epoxy resin. The presence of the damage can affect the mechanical properties and, subsequently, the response of the composite materials. One of the current major issue in the use of such composite materials is the understanding and prediction of damage modes and failure mechanisms at cryogenic temperatures. A thorough knowledge of the failure mechanisms is bound to lead to the design of efficient and durable structures.

Analytical modeling of damage in composite materials has attracted the attention of researchers. Numerous analytical models have been proposed for analyzing accumulated damage in composite materials⁽⁷⁾⁻⁽¹¹⁾. However, the most of the analytical research on woven composites has been focused on predicting the thermo-mechanical

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and thermal properties of woven composites^{(12)–(14)}. Attempts to model the strength and associated failure mechanisms are of recent origin. The intricate geometry of the weave combined with geometrical and material irregularity poses a formidable problem in the modeling and analysis of woven composites. Kollegal and Sridharan examined the behavior of a plain weave lamina subjected to compression⁽¹⁵⁾. Chapman and Whitecomb studied progressive damage due to cool-down for plain- and stain-weave carbon-carbon woven composite⁽¹⁶⁾. Zako et al. simulated the damage behavior of woven fabric composites by finite element analysis using an anisotropic damage model based on damage mechanics⁽¹⁷⁾.

Dramatic changes in the structure and properties of composite materials can occur when they are exposed to cryogenic temperature. The purpose of this paper is to investigate fracture and damage behaviors in G-11 woven glass-epoxy laminates focusing on the effect of cryogenic temperatures on the failure mechanisms. Fracture toughness tests were performed with CT specimens of different widths and thicknesses at 77 K. The damage zone around the notch tip of tested specimens was characterized using a microscope. A two-dimensional finite element model was also developed to investigate the effect of damage on the specimen response. A micromechanics model is used to predict the elastic properties of G-11 woven glass-epoxy laminates. The failure mechanisms of G-11 woven glass-epoxy laminates at cryogenic temperatures can be classified as matrix cracking and fiber-dominated failure (breaking of both fibers and matrix). Two failure modes in the form of matrix cracking and fiber-dominated failure were evaluated using the maximum strain criterion and the Hoffman's criterion, respectively. The validity of the model is demonstrated by the prediction of nonlinear load-displacement relation and damage evolution of a CT specimen under fracture toughness tests. The virtual crack extension method was proposed to calculate stress intensity factors.

2. Experimental Procedures

2.1 Materials and specimens

G-11 woven glass-epoxy laminates were used as specimen materials. The basis for G-11 is the glass fabric of E-glass. Its plain weave is produced by interlacing warp (length) threads (43 per 25 mm) and fill (width) threads (32 per 25 mm). The G-11 matrix is a bisphenol-A epoxy resin and the resin weight fraction is 35%.

There are no standard test procedures for evaluating fracture toughness for woven glass-epoxy laminates. Hence, these tests were performed in accordance with the recommendations of ASTM E1820-01⁽¹⁸⁾ for monolithic metallic materials. Some of the procedures suggested in the above standards may not be directly applicable to woven glass-epoxy laminates. Geometrically similar CT

Fig. 1 Geometry of test specimens; (a) 1 TCT specimen, (b) 0.32 TCT specimen

specimens with different widths ($W=20$ mm, 50 mm) and thicknesses ($B=8$ mm, 25 mm) were machined from 8- and 25-mm-thick G-11 woven glass-epoxy laminates, and were provided with a lateral notch (initial notch length to specimen width ratio $a_0/W=0.5$ and 0.55) made by saw. All of them were cut with a sharp fly cutter to give a notch root radius of 0.25 mm. Other specimen features were in accordance with the ASTM E1820-01, except that a modified notch was incorporated. As shown in Fig. 1, the modified notch permits clip gage attachment at knife edges in the loadline. 0.32 TCT specimen ($W=20$ mm, $B=8$ mm) and 1 TCT specimen ($W=50$ mm, $B=25$ mm) were tested.

2.2 Testing methods

Fracture toughness tests were conducted using screw and hydraulic driven testing machines at room temperature and liquid nitrogen temperature (77 K). The loading rate was kept constant at a crosshead speed of 0.2 mm/min. For all specimens, the fill fiber bundles were aligned with the load axis. Low temperature environment was achieved by immersing the loading fixture, specimen, and clip gage in liquid nitrogen. During the measurements, load (P) and load-line displacement (V_L) data were automatically logged by computer from a load cell and a clip gage, respectively.

Specimens of G-11 woven glass-epoxy laminates exhibited nonlinear fracture behavior due to damage events at the notch tip⁽⁶⁾. The CT specimens were tested by using the single-specimen unloading compliance technique described in ASTM E1820-01 to determine the J -integral versus crack growth resistance curve (J - R curve) for evaluating the fracture toughness. The compliances were estimated using a linear least-squares best fit to the P versus V_L data obtained on unloading. From a crack length (a) versus elastic compliance (C) correlation obtained from finite element calculation, the crack extension increment (Δa) at each unloading was inferred. For the CT specimens, values of J are given as

$$J = J_{el} + J_{pl} \quad (1)$$

Fig. 2 Definition of area for J_C calculation

where J_{el} is the elastic component of J obtained from finite element calculation and J_{pl} is the plastic component of J obtained by standard procedure⁽¹⁸⁾.

When fracture occurs before stable tearing, the J -integral value at the point of crack instability, J_C , is calculated⁽¹⁸⁾. For the CT specimens, the value of J_C is obtained from ASTM E1820-01 as

$$J_C = J_M + \frac{\eta_0 A_M}{Bb_0} \quad (2)$$

where

$$\eta_0 = 2 + 0.522 \frac{b_0}{W} \quad (3)$$

In the above equations, J_M is the elastic value of J at the maximum load P_M obtained from finite element calculation, A_M is the area under the plastic part of the load-displacement curve up to the maximum load P_M (Fig. 2) and b_0 is the initial uncracked ligament ($b_0 = W - a_0$).

3. Prediction of Elastic Properties

A micromechanics model⁽¹⁹⁾ is used to predict the elastic properties of composites reinforced with plain weave fabrics. A representative volume element (RVE) is chosen for analysis and the yarn architecture is described by a few simple functions. These functions are employed to calculate various phase fractions from geometric parameters that can be measured on a cross section. Effective elastic moduli are determined under the assumption of uniform strain inside the representative volume element. Verification of the RVE method for predicting effective elastic moduli for woven glass-epoxy laminates are presented in previous paper^{(20), (21)}. The elastic properties of the matrix (epoxy) and fiber (E-glass) were approximated by the exponential functions⁽²²⁾, and the elastic properties of the composite yarns were calculated using equations of Hashin⁽²³⁾. The effective stiffness of the laminates is obtained simply as a volume average of the local stiffness field. The generalized Hooke's relation between stress and strain for a three-dimensional anisotropic material may be written as

Table 1 Effective stiffness components and predicted elastic moduli of G-11 at room temperature, 77 K and 4 K

Temperature	R.T.	77K	4K
Effective stiffness components			
C_{11}^* (GPa)	28.70	38.35	43.51
C_{22}^* (GPa)	23.07	33.95	39.76
C_{33}^* (GPa)	12.12	25.40	32.48
C_{12}^* (GPa)	4.47	10.01	13.33
C_{13}^* (GPa)	4.51	10.47	14.05
C_{23}^* (GPa)	4.55	10.77	14.51
C_{44}^* (GPa)	5.05	8.53	10.05
C_{55}^* (GPa)	5.04	8.57	10.09
C_{66}^* (GPa)	5.17	8.76	10.28
Young's moduli			
E_x (GPa)	26.66 (27.9)	32.98 (32.7)	35.94 (36.9)
E_y (GPa)	21.07 (24.1)	28.47 (31.1)	31.95 (34.3)
E_z (GPa)	10.75	20.48	25.03
Shear moduli			
G_{xy} (GPa)	5.17 (6.2)	8.76 (9.4)	10.28 (10.1)
G_{yz} (GPa)	5.05	8.53	10.05
G_{zx} (GPa)	5.04	8.57	10.09
Poisson's ratios			
ν_{xy}	0.13 (0.17)	0.19 (0.19)	0.21 (0.23)
ν_{yz}	0.34	0.36	0.37
ν_{zx}	0.13	0.21	0.24

Number in the parentheses indicates experimental data.

$$\begin{bmatrix} \sigma_{xx} \\ \sigma_{yy} \\ \sigma_{zz} \\ \sigma_{yz} \\ \sigma_{zx} \\ \sigma_{xy} \end{bmatrix} = \begin{bmatrix} C_{11}^* & C_{12}^* & C_{13}^* & 0 & 0 & 0 \\ C_{12}^* & C_{22}^* & C_{23}^* & 0 & 0 & 0 \\ C_{13}^* & C_{23}^* & C_{33}^* & 0 & 0 & 0 \\ 0 & 0 & 0 & C_{44}^* & 0 & 0 \\ 0 & 0 & 0 & 0 & C_{55}^* & 0 \\ 0 & 0 & 0 & 0 & 0 & C_{66}^* \end{bmatrix} \begin{bmatrix} \varepsilon_{xx} \\ \varepsilon_{yy} \\ \varepsilon_{zz} \\ 2\varepsilon_{yz} \\ 2\varepsilon_{zx} \\ 2\varepsilon_{xy} \end{bmatrix} \quad (4)$$

where σ_{xx} , σ_{yy} , σ_{zz} , σ_{yz} , σ_{zx} , σ_{xy} are the stress components and ε_{xx} , ε_{yy} , ε_{zz} , ε_{yz} , ε_{zx} , ε_{xy} are the strain components, and C_{ij}^* ($i, j = 1, \dots, 6$) are the effective stiffness components. The effective stiffness components of G-11 calculated from the representative volume element and predicted elastic moduli along with experimental data are listed in Table 1. (E_x , E_y , E_z) are the tensile moduli, (G_{xy} , G_{yz} , G_{zx}) are the shear moduli, (ν_{xy} , ν_{yz} , ν_{zx}) are the Poisson's ratios and figures in brackets indicate the experimental data. The subscripts x , y , and z will be used to refer to the coordinate directions, and the Poisson's ratio ν_{xy} reflects shrinkage (expansion) in the y -direction due to tensile (compressive) stress in the x -direction. Also, note that x , y and z coordinates are warp, fill and normal directions, respectively. The tensile moduli E_x , E_y and the Poisson's ratio ν_{xy} were obtained from tensile tests according to JIS K7054⁽²⁴⁾. The shear modulus G_{xy} was also obtained from 45° off-axis tensile tests according to JIS K7059⁽²⁵⁾. The predicted elastic moduli agree well with the experimental data.

Fig. 3 Boundary conditions assumed for finite element calculations

4. Finite Element Analysis

4.1 Finite-element modeling and boundary conditions

To understand the cryogenic fracture and damage behaviors of G-11, a nonlinear analysis was performed using two-dimensional (2-D) finite element models. In the case of the 2-D computations, the boundary conditions of the 0.32 TCT specimen (the width, $W=20$ mm, the thickness, $B=8$ mm) and 1 TCT specimen ($W=50$ mm, $B=25$ mm) are shown in Fig. 3. H and a_0 denote the distance between two centers of the loading holes and the notch length, respectively. For the evaluation of elastic compliance and mode I strain energy release rate, a sharp crack was used to model the initial notch in CT specimens. Only one half of the specimen need to be analyzed due to symmetry. The loading pin is modeled as steel. Perfect bonding between specimen and loading pin is assumed to make the model simple. A incremental tensile load P was applied to the loading pin up to the maximum load P_M of the CT specimens. The following displacement boundary conditions were applied to the CT specimen.

$$\text{on plane } y=0 \quad u_y=0 \quad (5)$$

$$\text{at } x=0.25W, y=H/2 \quad u_x=0 \quad (6)$$

where u_x and u_y are the displacement components. These assumptions are consistent with the experiment. The finite element mesh is composed of 3 324–3 816 six-node triangular elements. The 2-D computations were conducted with either a plane strain or plane stress assumption.

4.2 Damage growth prediction

The finite element analysis in this paper consists of a stress analysis and a failure analysis. Stresses and strains in the CT specimens were calculated by a nonlinear finite element analysis. The failure analysis consists of a set of failure criteria and property degradation models. The incremental step method for nonlinear analysis is a step-by-step procedure, where the effect of damage on the mechanical properties are determined at the end of each step,

Table 2 Strains at the knee of G-11

Temperature	77K	4K
Warp		
ϵ_w^0 (%)	0.83	0.76
Fill		
ϵ_f^0 (%)	0.73	0.65
$\pm 45^\circ$ off-axis		
ϵ_s^0 (%)	1.26	1.23

and substituted into the next step, thus making the analysis linear over each step. Stresses and strains were calculated at each incremental step, and evaluated by the failure criteria to determine the occurrence of failure and the mode of failure. Mechanical properties in the damaged element are reduced appropriately according to the property degradation models. This process is continued until the desired load (maximum load P_M) is reached. The loading procedure proposed by Yamada⁽²⁶⁾ was divided into 160–1626 load steps. This procedure used the small and varying increments of load sufficient to just cause failure in the successive elements.

“Knee behavior” of woven composites is a phenomenon by which the stress-strain curve exhibits a knee or change in slope when the composite is subjected to an extensional in-plane load. The knee at cryogenic temperatures is caused by progressive failure of the epoxy resin (matrix)⁽²⁷⁾. Matrix cracking failure is predicted when the following maximum strain criteria are satisfied.

$$\epsilon_{xx} \geq \epsilon_w^0 \quad (7)$$

$$\epsilon_{yy} \geq \epsilon_f^0 \quad (8)$$

$$|2\epsilon_{xy}| \geq \epsilon_s^0 \quad (9)$$

where ϵ_w^0 , ϵ_f^0 and ϵ_s^0 are the strains that locates the knee in the stress-strain curve of G-11 woven glass-epoxy laminates subjected to an in-plane tensile or shear load. The measured ϵ_w^0 , ϵ_f^0 and ϵ_s^0 of G-11 at 77 K and 4 K are presented in Table 2. At room temperature, bimodulus behavior does not exist.

For matrix cracking, the tensile and shear moduli and Poisson's ratio of epoxy are reduced as follows.

$$E_M^d = \frac{E_M}{10\,000} \quad (10)$$

$$\nu_M^d = 0.0 \quad (11)$$

$$G_M^d = \frac{E_M^d}{2(1+\nu_M^d)} \quad (12)$$

where E_M , E_M^d , G_M^d and ν_M^d are the tensile modulus of epoxy, and the reduced tensile and shear moduli and Poisson's ratio of epoxy, respectively. Effective elastic properties within the damage element were recalculated using the reduced tensile and shear moduli and Poisson's ratio of epoxy.

Table 3 Mechanical properties of G-11

Temperature	R.T.	77K	4K
Tensile strengths			
F_{tx} (MPa)	325	656	668
F_{ty} (MPa)	273	507	506
F_{tz} (MPa)	272($F_{cz}/2$)	456($F_{cz}/2$)	502($F_{cz}/2$)
Compressive strengths			
F_{cx} (MPa)	332	660	699
F_{cy} (MPa)	331	642	650
F_{cz} (MPa)	545	912	1003
Shear strength			
F_{sxy} (MPa)	108	147	142

The Hoffman's failure criterion⁽²⁸⁾ was selected as the criterion for fiber-dominated failure. The Hoffman's failure criterion is a supplement to the Tsai-Hill criterion. The linear stress terms account for possible differences in tensile and compressive strengths. For plane stress, the Hoffman's failure criterion becomes

$$X_1\sigma_{yy}^2 + X_2\sigma_{xx}^2 + X_3(\sigma_{xx} - \sigma_{yy})^2 + X_4\sigma_{xx} + X_5\sigma_{yy} + X_7\sigma_{xy}^2 \geq 1 \quad (13)$$

and for plane strain

$$X_1(\sigma_{yy} - \sigma_{zz})^2 + X_2(\sigma_{zz} - \sigma_{xx})^2 + X_3(\sigma_{xx} - \sigma_{yy})^2 + X_4\sigma_{xx} + X_5\sigma_{yy} + X_6\sigma_{zz} + X_7\sigma_{xy}^2 \geq 1 \quad (14)$$

where

$$X_1 = \frac{1}{2} \left[\frac{1}{F_{ty}F_{cy}} + \frac{1}{F_{tz}F_{cz}} - \frac{1}{F_{tx}F_{cx}} \right] \quad (15)$$

$$X_2 = \frac{1}{2} \left[\frac{1}{F_{tz}F_{cz}} + \frac{1}{F_{tx}F_{cx}} - \frac{1}{F_{ty}F_{cy}} \right] \quad (16)$$

$$X_3 = \frac{1}{2} \left[\frac{1}{F_{tx}F_{cx}} + \frac{1}{F_{ty}F_{cy}} - \frac{1}{F_{tz}F_{cz}} \right] \quad (17)$$

$$X_4 = \frac{1}{F_{tx}} - \frac{1}{F_{cx}} \quad (18)$$

$$X_5 = \frac{1}{F_{ty}} - \frac{1}{F_{cy}} \quad (19)$$

$$X_6 = \frac{1}{F_{tz}} - \frac{1}{F_{cz}} \quad (20)$$

$$X_7 = \left(\frac{1}{F_{sxy}} \right)^2 \quad (21)$$

In the above expressions, F_{tx} , F_{ty} , F_{tz} , F_{cx} , F_{cy} , F_{cz} , F_{sxy} are the three uniaxial tensile strengths, the three uniaxial compressive strengths and the shear strength, respectively. The strengths used for the analysis are presented in Table 3. The strengths F_{tx} , F_{ty} , F_{sxy} were obtained from tensile tests according to JIS K7054 and JIS K7059. The strengths F_{cx} , F_{cy} , F_{cz} were also obtained from compression tests according to JIS K6911⁽²⁹⁾. The strength F_{tz} is assumed to be $0.5F_{cz}$.

For fiber-dominated failure, the tensile and shear moduli and Poisson's ratio of epoxy and fiber are reduced as follows.

$$E_F^d = \frac{E_F}{10000} \quad (22)$$

$$\nu_F^d = 0.0 \quad (23)$$

$$G_F^d = \frac{E_F^d}{2(1+\nu_F^d)} \quad (24)$$

$$E_M^d = \frac{E_M}{10000} \quad (25)$$

$$\nu_M^d = 0.0 \quad (26)$$

$$G_M^d = \frac{E_M^d}{2(1+\nu_M^d)} \quad (27)$$

where E_F , E_F^d , G_F^d and ν_F^d are the tensile modulus of fiber, and the reduced tensile and shear moduli and Poisson's ratio of fiber, respectively. Effective elastic properties within the damage element were recalculated using the reduced tensile and shear moduli and Poisson's ratios of epoxy and fiber.

4.3 Strain energy release rate and stress intensity factor

A two-dimensional finite element analysis was conducted to calculate the stress intensity factor. Sih et al.⁽³⁰⁾ presented the relationship between stress intensity factor K and strain energy release rate $\mathcal{G}$ for anisotropic bodies: for the special case of orthotropy, with the crack surface parallel to the first principal direction of material properties, the relationship for mode I was given by

$$K_I = \sqrt{\mathcal{G}_I E'} \quad (28)$$

where

$$E' = \sqrt{\frac{2}{S_{11}S_{22}}} \left(\sqrt{\frac{S_{22}}{S_{11}}} + \frac{2S_{12} + S_{66}}{2S_{11}} \right)^{-1/2} \quad (29)$$

In this expression, S_{ij} ($i, j = 1, 2, 6$) are the elastic compliances. Note that the coordinates x , y and z coincide with principal material directions of the orthotropic material. The two-dimensional strain-stress relationship may be written as

$$\begin{bmatrix} \varepsilon_{xx} \\ \varepsilon_{yy} \\ 2\varepsilon_{xy} \end{bmatrix} = \begin{bmatrix} S_{11} & S_{12} & 0 \\ S_{21} & S_{22} & 0 \\ 0 & 0 & S_{66} \end{bmatrix} \begin{Bmatrix} \sigma_{xx} \\ \sigma_{yy} \\ \sigma_{xy} \end{Bmatrix} \quad (30)$$

The virtual crack extension method proposed by Le-Ngoc and McCallion⁽³¹⁾ is to calculate the total strain energy, Π_0 for the initial crack system, and then move the node at the crack tip by an amount Δa^* and recompute the total strain energy for the extended crack system, Π_1 . The displacements on the externally loaded boundary found for the initial crack length a^* are held unchanged while the length is extended to $a^* + \Delta a^*$. The strain energy release rate can then be approximated by

$$\mathcal{G} = -\frac{1}{B} \frac{\Pi_1 - \Pi_0}{\Delta a^*} \quad (31)$$

Equation (31) can then be used to calculate the stress intensity factor.

Fig. 4 Comparison between predictions and experiments on load-displacement curves at room temperature and 77 K for $a_0/W=0.5$ (1 T)

5. Results and Discussion

5.1 Load-displacement curves

In order to verify the proposed model, the predictions were compared with the test data. A 2-D plane strain model of 1 TCT specimens will be studied first to highlight the effect of damage on load-displacement response. 1 TCT specimens were tested to failure without unloadings, measuring load-displacement response. Figure 4 presents the comparison between the measured (solid lines) and the calculated (dashed lines) load-displacement curves at room temperature and 77 K for $a_0/W=0.5$. It is evident that the prediction is relatively close to the experiment up to about 0.2 mm displacements, but it deviates slightly for large displacements. Similar results were found for the analysis of 0.32 TCT specimens.

The tests conducted to obtain J - R curves were done using an unloading compliance technique. Representative load-displacement curves from unloading compliance tests are shown in Fig. 5. For the 0.32 TCT specimens, the failures of the specimens were catastrophic (dynamic), and occurred shortly after the maximum load. For the 1 TCT specimens, fracture progresses by a series of instabilities. At each unloading point, the instantaneous J and crack length were calculated.

5.2 Damage progression

The load-displacement curves are initially linear but deviate progressively from linearity as the slow crack growth or damage occurs in the region ahead of the notch tip. In order to characterize the damage zone produced in front of the notch root during loading, several specimens were unloaded before the final unstable fracture occurred. The notched roots of 0.32 T and 1 TCT specimens were examined under a microscope. Figure 6 shows a photograph of a 0.32 TCT specimen after loaded to 1.49 kN at room temperature. The crack growth is a simple extension of the notch root. Figure 7 shows the damage near

Fig. 5 Load-displacement curves for 0.32 T and 1 TCT specimens

Fig. 6 A photograph of a 0.32 TCT specimen with $a_0/W=0.5$ at room temperature after loaded to 1.49 kN

the notch tip at 77 K after the specimens were loaded to some stress levels. A substantial amount of damage can occur as the load is increased. This may be attributed to the brittleness of the epoxy resin, leading to matrix cracking. Figure 8 shows a photograph of a 1 TCT specimen after loaded to 17.40 kN at 77 K.

To further validate the model, the predicted damage progressions of the 2-D plane stress model are exam-

Fig. 7 Photographs of 0.32 TCT specimens with $a_0/W=0.5$ fractured at 77 K for various loading levels: (a) 2.55 kN; (b) 3.53 kN; and (c) 3.73 kN

ined using the experimental data. Figure 9 presents the predicted damage progression in 0.32 TCT specimens at room temperature and 77 K as a function of the applied load. The measured damage area was shown by a dotted line. At room temperature, damage initiated from the notch tip, and propagated. The failure mode was dominated by breaking of both fibers and matrix. The damage was confined to area near the notch tip until the load approached the final failure load. The predicted damage pattern in Fig. 9 (a) is qualitatively consistent with the photograph of the damaged specimen in Fig. 6. At 77 K, matrix cracking initiated from the notch tip. As the load level was increased, matrix cracking becomes larger and a smaller number of fiber-dominated failure occur near the notch tip. The size of damage at 77 K was much more extensive than that predicted at room temperature. The experimental ob-

Fig. 8 A photograph of a 1 TCT specimen with $a_0/W=0.55$ at 77 K after loaded to 17.40 kN

Fig. 9 Illustration of the predicted damage growth pattern in 0.32 TCT specimens with $a_0/W=0.5$ as a function of the applied load at (a) room temperature and (b) 77 K

Fig. 10 Illustration of the predicted damage growth pattern in a 1 TCT specimen with $a_0/W=0.55$ corresponding to applied load of 17.40 kN at 77 K

servation in Fig. 7 (b) basically agrees with the prediction in Fig. 9 (b) expect that the matrix cracking in the fill direction was overestimated. Figure 10 shows the predicted damages of a 1 TCT specimen after loaded to 17.40 kN. The measured damage area was shown by a dotted line. At this load, many matrix cracks appear and a smaller number of fiber-dominated failure occur near the notch tip. The predicted damages deviate from the test data.

5.3 Fracture toughness evaluation

Using the 2-D plane strain model, computations were

Fig. 11 J - R curves at 77 K for 0.32 T and 1 TCT specimensTable 4 J_Q and J_C of G-11

Temperature	R.T.			77K			
Specimen size	0.32T		1T	0.32T		1T	
a_0/W	0.5	0.6	0.5	0.5	0.55	0.6	0.5
J_Q (kJ/m ²)	—	—	—	invalid	—	—	48.0
J_C (kJ/m ²)	15.5	17.4	21.5	46.7	47.2	41.1	60.8

performed to evaluate the fracture toughness. Figure 11 shows the J - R curves for the 0.32 T and 1 TCT specimens at 77 K. The blunting line, the 0.2 mm offset line and the power law regression line are superimposed on the plot. The blunting line was determined in accordance with the following equation

$$J = 2F_{ty}\Delta a \quad (32)$$

The J - R curves showed significant specimen size dependence. For the 0.32 TCT specimens, final unstable fracture occurs prior to the determination of the required J values versus Δa data. For the 1 TCT specimens, the J values versus crack growth behavior is approximated with a best-fit power law relationship. The intersection of the power-law fit with the 0.2 mm offset line was taken as the J_Q value. The J -integral results are summarized in Table 4. Note that the J_C values for the 0.32 TCT specimens are calculated from the results of Ref. (6). It is evident from these results that increasing specimen size from 0.32 T to 1 T increase J_C . In the experiments with the 1 TCT specimens it was possible to evaluate the J_Q value at 77 K. The J_C value for 1 TCT specimen at 77 K is approximately 1.3 times as large as the J_Q value.

The size of predicted damage by fiber-dominated failure of 0.32 TCT specimen at 77 K as a function of the applied load is shown in Fig. 12. A significant change of slope in the damage size-load relation is observed at a load of P_C . The P_C value seems to be a critical load at which the crack (breaking of both fibers and matrix) initiation and propagation occurs. An apparent fracture toughness K_C was calculated for each specimen using the load P_C . The results of P_C and K_C of CT specimens with different notch lengths and thicknesses are given in Table 5. It

Fig. 12 Variation of damage zone size versus load

Table 5 P_C and K_C of G-11 at 77 K

Specimen size	0.32T			1T	
a_0/W	0.5	0.55	0.6	0.5	0.55
P_C (kN)	2.56	2.18	1.81	12.76	10.83
K_C (MPa√m)	21.6	21.6	21.6	21.9	21.8

is found that the K_C value is independent of notch length and specimen size.

6. Conclusions

Cryogenic fracture toughness tests were carried out with 0.32 T and 1 TCT specimens. An analytical model is also presented for the evaluation of the cryogenic damage and fracture behaviors of G-11 woven glass-epoxy laminates. Based on the study, the following conclusions can be made.

1. The predicted load-displacement curve deviated slightly from the test data.
2. The predicted damage by matrix cracking at 77 K was overestimated. The size of predicted damage by matrix cracking of the 1 TCT specimen was more extensive than that of the 0.32 TCT specimen.
3. There was significant geometry dependence of the J - R curves at 77 K.
4. The K_C value was nearly constant with notch length and specimen size.

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