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Modeling the Initiation and Growth of Delaminations in Composite Structures

E. D. Reedy, Jr., F. J. Mello, T. R. Guess

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Modeling the Initiation and Growth of Delaminations in Composite Structures

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Abstract

A method for modeling the initiation and growth of discrete delaminations in shell-like composite structures is presented. The laminate is divided into two or more sublaminates, with each sublaminate modeled with 4-noded quadrilateral shell elements. A special, 8-noded hex constraint element connects the sublaminates and makes them act as a single laminate until a prescribed failure criterion is attained. When the failure criterion is reached, the connection is broken, and a discrete delamination is initiated or grows. This approach has been implemented in a three-dimensional, finite element code. This code uses explicit time integration, and can analyze shell-like structures subjected to large deformations and complex contact conditions. Tensile, compressive, and shear laminate failures are also modeled. This paper describes the 8-noded hex constraint element used to model the initiation and growth of a delamination, and discusses associated implementation issues. In addition, calculated results for double cantilever beam and end notched flexure specimens are presented and compared to measured data to assess the ability of the present approach to reproduce observed behavior. Results are also presented for a diametrically compressed ring to demonstrate the capacity to analyze progressive failure in a highly deformed composite structure.

Table of Contents

List of Figures	5
Introduction	6
Approach for Modeling Discrete Delaminations	7
Implementation Issues	8
Examples	11
Summary	13
References	15
Appendix I: Fabric Reinforced Composite Constitutive Model	16

List of Figures

Figure 1.	Approach used to connect shells by penalizing normal (mode I) displacement across the connection plane.....	18
Figure 2.	Approach used to connect shells by penalizing tangential (mode II) displacement across the connection plane.....	19
Figure 3.	The reduction of the area weighted penalty parameter $\bar{K}_n$ during failure is defined by the constraint force per nodal area F_n/A vs. the penalized displacement ΔU_j relation.	20
Figure 4.	Double cantilevered beam analysis (all dimensions in mm).	21
Figure 5.	Comparison of calculated double cantilevered beam load vs. load point displacement relation with test data ($U_{crit} = 0.004$ mm, $U_{max} = 0.15$ mm).	22
Figure 6.	Comparison of calculated double cantilevered beam load point displacement vs. delamination length relation with test data ($U_{crit} = 0.004$ mm, $U_{max} = 0.15$ mm)..	23
Figure 7.	Comparison of double cantilevered beam load point displacement vs. delamination length relations calculated using the "DLAM with damping" and the "DLAM with damage" approaches to control the failure process.	24
Figure 8.	Comparison of calculated double cantilevered beam load vs. load point displacement relation with test data ($U_{crit} = 0.002$ mm, $U_{max} = 0.30$ mm).	25
Figure 9.	Comparison of calculated double cantilevered beam load point displacement vs. delamination length relation with test data.....	26
Figure 10.	End notched flexure geometry, sublaminates 3.06 mm thick (all dimensions in mm).....	27
Figure 11.	Comparison of calculated end notched flexure load vs. load point displacement relation with test data.	28
Figure 12.	Diametral ring test analysis (all dimensions in mm).	29
Figure 13.	Calculated load vs. load point displacement relations for a diametrically compressed ring that either include or suppress the delamination failure mode.	30
Figure A1.	Fabric reinforced composite stress-strain relation for yarn-directed extension. .	31
Figure A2.	Fabric reinforced composite shear stress-strain relation.	32

Introduction

This work focuses on modeling the highly nonlinear, large deformation response of shell-like composite structures as they progressively fail. Our goal is to develop a reasonably accurate and computationally efficient approach for modeling the entire failure process. This type of analysis is applicable to crashworthiness studies, or whenever there is a need to estimate peak loads, energy absorption, or the final shape of a highly deformed structure. A shell-based finite element analysis is most appropriate for this class of problems. Since it is not presently feasible to model every aspect of the failure process, the aim is to include all the primary failure modes in as simple a way as possible. Failure modes of interest include 1) fiber dominated tensile and compressive failures, 2) large scale shear nonlinearity associated with matrix cracking, and 3) the initiation and growth of discrete delaminations.

Flexural failures often dominate when shell-like structures are subjected to transverse loads. Flexural failure can be quite complex, and will involve one or more of the aforementioned failure modes (e.g., fiber dominated compressive failure and delamination). Even a single delamination can have a dramatic effect on flexural stiffness. For example, if a homogeneous beam is split in half, the flexural stiffness is reduced to 25% of its original value. If instead the beam were initially split into 3 beams, the stiffness is reduced to 11% of its original value. Accordingly, an analysis that includes the initiation and growth of just one or two discrete delaminations, located at a depth where they would have the greatest impact on flexural stiffness, is expected to provide a reasonable representation of composite response. This is the approach taken in the present work. The laminate is broken up into two or more sublaminates. Each sublaminate is modeled with 4-noded, quadrilateral, layered shell elements. The shell elements use composite material models that include the in-plane failure modes of interest. Special, eight-noded hex constraint elements connect adjacent sublaminates, and supply the nodal forces and moments needed to make the sublaminates act as a single laminate until a prescribed failure criterion is attained. When this failure criterion is reached, the connection is broken creating (or growing) a discrete delamination.

The efficacy of an analysis that uses a partially bonded assemblage of plate-like sublaminates to model a laminate with a fixed, preexisting delamination is well established. [1-4]. There have also been several efforts aimed at extending this type of analysis to growing delaminations. For example, Sankar and Hu [5] connect linear elastic beam elements with extensional and torsional springs that fail when the energy release rate reaches a critical value. They analyzed impact induced growth of a preexisting delamination embedded within a cantilevered beam. Farley and Jones [6] model delamination by disconnecting "zero length" extensional and torsional springs between coincident nodes of adjacent shell elements when the energy release rate reaches a critical value. Their large deformation analysis uses a nonlinear elastic idealization with a maximum strain failure criterion for the in-plane material model. A small section of a composite tube was modeled to estimate sustained crushing stress. Song and Waas [7] use a linear elastic beam on a nonlinear spring foundation to model delamination growth in a double cantilever beam fracture specimen. A spring fails when the total strain energy in the spring reaches a critical value.

Notable features of the present analysis include 1) a three-dimensional, large deformation capability, 2) modeling the initiation and growth of one or more discrete delaminations, 3)

modeling in-plane failure modes, 4) compatibility with existing shell element formulations, in-plane material models, and mesh generation tools, and 5) implementation in a finite element code that uses explicit time integration to advance the equations of motion from the initial state. Note that explicit finite element codes, sometimes referred to as a "wave codes", are used to solve transient dynamic problems that include highly nonlinear material response and large deformations [8]. They are also used for quasi-static loadings when the model contains a large number of elements, complex contact conditions, geometric instabilities, or material softening. In the following, the approach for modeling discrete delaminations is reviewed, implementation issues are discussed, and illustrative examples are presented.

Approach for Modeling Discrete Delaminations

The basic idea is easily visualized by considering a 2D representation. The laminate is broken into two shells (sublaminates) of thickness h , with nodes located along each shell's mid-surface. Focus attention on the two nodes on the left hand side of Fig. 1a. The relative normal (mode I) displacement across the connection plane (the location of a potential delamination) is simply equal to the relative nodal displacement in the normal direction (the shells are inextensible in the thickness direction). The relative tangential (mode II) displacement across the connection plane is based on the displacement at the bottom of the top shell and that on the top of the bottom shell. As indicated in Fig. 2a, these displacements can be defined in terms of the tangential nodal displacement and nodal rotation defined at the shell's mid-surface. To make the two shells act as one, the relative displacements across the connection plane (ΔU_j and ΔV_j) must be constrained or penalized to enforce an acceptable level of displacement continuity. The penalty parameter (i.e., K_n or K_t) can be thought of as a spring constant that is applied to the relative displacement, and the stiffer the spring the more precisely the constraint is enforced. The associated forces act on the connection plane and correspond to the internal forces that hold the sublaminates together prior to delamination (Figs. 1b,2b). These forces are then replaced by equivalent nodal forces and moments (Figs. 1c,2c). Note that the corresponding nodal degrees of freedom on opposite sides of the delamination plane are not simply tied together. There are no torsional springs, nor are there concentrated moments on the delamination plane. The present approach produces the correct bending stress distribution in an intact laminate prior to delamination.

A discrete delamination between sublaminates is created (or grows) when the constraint condition is removed. The constraint condition is removed by reducing the corresponding penalty parameter to zero. There are two obvious choices for the delamination failure criterion. Either a critical energy release rate or a critical stress over a fixed area could be used. The energy release rate criterion has the advantage of being mesh-size independent. Unfortunately, in many of the nonlinear, large deformation problems of interest, the applicability of a linear elastic fracture mechanics-based approach is questionable. In such problems, multiple energy absorbing failure modes occur simultaneously. For example, fiber failure and delamination growth occur at the same time when a ring is crushed [9]. The use of a critical stress over a fixed area is most suitable when the sublaminates are made of a material with a well defined, macroscopic, characteristic length scale. Clearly such an approach has limitations, but seems reasonable for composite materials reinforced with a coarsely woven fabric. In the examples presented below a woven roving reinforced composite (rovings are ~ 5 mm wide) is analyzed, and the "critical stress over a

fixed area" criterion is used. Note that this criterion can also be expressed in terms of a critical value of the relative displacement across the connection plane (ΔU_j and ΔV_j are directly related to the connection plane forces F_n and F_t ; see Figs. 1 and 2). With this interpretation, mode I failure occurs at a critical value of ΔU_j , and mode II failure occurs at a critical value of ΔV_j . Observed differences in mode I and II delamination resistances are modeled by specifying different critical values for mode I and II failure. Though not considered in the present study, a mixed-mode delamination criterion that is a function of both ΔU_j and ΔV_j could be defined. Regardless of the delamination criterion used, failure in mode I implies a complete loss of resistance to a mode II loading, and vice versa. Interpenetration of the sublaminates is prevented by reapplying the transverse stiffness K_n whenever the delamination closes (i.e., acts like a gap contact element). A mode II delamination can grow while the delamination is closed in mode I.

Explicit finite element codes are conditionally stable. The minimum time step that can be used to stably integrate the governing equations is associated with the highest eigenvalue (vibrational mode) in the mesh. The penalty parameter (i.e. spring constant) must be chosen in a judicial manner to avoid adversely impacting the stable time increment. For this reason, the smallest, physically reasonable value for the penalty parameters is used. Recall that K_n penalizes normal displacement across the connection plane. Since the shell elements are transversely inextensible, the estimate for K_n is based on the transverse stiffness of the material between the nodes defining the upper and lower sublaminates.

$$K_n = A\bar{K}_n \quad \text{where} \quad \bar{K}_n = \frac{E}{h} \quad (1)$$

where E is the sublaminate's transverse Young's modulus, A is the portion of the connection plane area assigned to the node, and h is the sublaminate thickness. Prior to delamination, the spring deformation is associated with through-the-thickness laminate deformation. When a shear deformable shell is used, the K_t estimate is based on the shear stiffness of a thin, resin-rich layer between sublaminates.

$$K_t = A\bar{K}_t \quad \text{where} \quad \bar{K}_t = \frac{G^*}{h^*} \quad (2)$$

where G^* is the shear stiffness that corresponds to a resin-rich layer (e.g., epoxy shear modulus), and h^* corresponds the thickness of a resin-rich layer (e.g., one quarter of a lamina thickness). Previous work has shown that these choices for penalty parameters lead to an energy release rate, compliance, and stress distribution ahead of the delamination that are in good agreement with detailed finite element results [10]. Note that it is convenient to use area weighted penalty parameters $\bar{K}_n$ and $\bar{K}_t$ as input quantities since they do not depend on element size.

Implementation Issues

The delamination modeling technique described in the previous section has been implemented in a three dimensional, transient dynamics, finite element code (PRONTO3D [8]). A special, eight-

noded hex element was created to enforce the constraints penalizing the relative displacement across the connection plane between sublaminates. This element is referred to as the DLAM element. The DLAM element looks like, and is meshed like a standard hex element, but it has no mass. It does not use shape functions. It shares nodes with the shells defining the sublaminates and uses these nodal displacements and rotations to determine the nodal forces and moments that are needed to make the sublaminates act as a single laminate until the connection is broken (see Figs 1 and 2). Implementing the constraints within the context of a hex element provides a natural way for simultaneously accessing the displacement and rotational degrees of freedom of both the upper and lower sublaminate shells. It also automatically includes the constraint forces when nodal forces are assembled. Other advantages of this approach include: 1) consistency with vector processing of element/material blocks, 2) ease of definition using existing mesh generators; the DLAM hex fits "in between" the sublaminate shell elements and shares the same nodes, and 3) a capability for modeling laminates containing multiple delaminations with sublaminates of differing thicknesses (e.g. shell-DLAM-shell-DLAM-shell). The DLAM element can be used with any type of four node quadrilateral shell element as long as the shell element is defined in terms of the usual displacement and rotational degrees of freedom. Layered shells can be used to model the sublaminates, and there are no restrictions on the type of material model used to model sublaminate response. In particular, material models that include damage and failure can be used.

The penalized relative displacements and the associated constraint forces and moments must be defined with respect to a suitable connection plane coordinate system. Note that the initial shape of a DLAM hex element is not necessarily rectangular, and regardless of its initial shape, the DLAM hex can be deformed in an arbitrary manner. It is convenient to define a local coordinate system for each of the four DLAM element edges that connect the nodes on the upper and lower sublaminates. Consider one of these four DLAM element edges. The first coordinate axis is directed along this DLAM edge. This is the direction of normal relative displacement across the connection plane (i.e., ΔU_j in Fig. 1). The other two local coordinate axes lie within a plane perpendicular to the first axis and are defined with respect to the DLAM element edges that lie in the plane of the sublaminate shells. This coordinate system is continually updated as the DLAM hex deforms. The normal constraint force is always directed along a line connecting upper and lower nodes. This is necessary to avoid artificial stiffening when the laminate is bent.

At present, only the "critical stress over a fixed area" delamination failure criterion has been implemented. As discussed in the previous section, this criterion can be expressed in terms of a critical value of the relative displacement across the connection plane (e.g., ΔU_j in Fig. 1). Once the failure criterion is satisfied, the corresponding penalty parameter is reduced to zero. Rate independent failure models are inherently unstable. The failure process must be suitably controlled, or the connection forces will be removed in just a few time steps. This is undesirable when simulating a quasistatic failure process since unloading occurs too quickly and high frequency stress waves are generated. This triggers a rapid sequence of delamination failures and "stick-slip" delamination growth.

Two different approaches for controlling the failure process have been implemented. In the first approach the penalty parameter is reduced gradually to zero following a failure path that looks like that of an elastic-damaging material (referred to below as "DLAM with damage"). This reduction in the penalty parameter is defined by the secant slope of the associated stress versus

displacement relation. Specifically, the area weighted mode I penalty parameter $\bar{K}_n = K_n/A$ is defined as the secant slope of the curve that specifies the variation of the connection force per area F_n/A with the penalized displacement ΔU_j (see Fig 3; where A is one fourth of the connection plane area). The curve's initial slope is set equal to the $\bar{K}_n$ value defined by Eq. 1. The value of $\bar{K}_n$ begins to decrease once $\Delta U_j > U_{crit}$, and continues to decrease until $\Delta U_j = U_{max}$. Although an arbitrary, piece-wise linear definition of the softening has been implemented, a simple linear reduction is a reasonable choice. The value of U_{max} must be sufficiently large to prevent failure from occurring too rapidly, but not so large that constraint force is still present after the delamination front moves forward (unless crack bridging forces acting behind the delamination front are being modeled). If the sublaminates are linear elastic and the delamination advances quasi-statically, the area under the unloading path, (Fig. 3), equals the energy release rate for crack extension. The penalty parameter K_t is defined in an analogous way. In the present implementation, there is no distinction between mode II and mode III delamination growth, and failure is based on the vector magnitude of the penalized tangential displacement components.

The second approach for controlling the failure process applies a velocity dependent damping force opposing relative motion across the connection plane (referred to below as "DLAM with damping"). First consider the relative motion normal to the connection plane. A damping force opposing this relative motion is applied to the connected nodes (Fig. 1a). The magnitude of the damping force F_d is

$$F_d = C_t m_e \Delta \dot{U}_j / \Delta t \quad \text{where} \quad m_e = \frac{m_t m_b}{m_t + m_b} \quad (3)$$

where C_t is the damping coefficient for relative translational velocity across the connection plane, m_e is the effective mass of the connected upper and lower nodes of mass m_t and m_b , respectively, $\Delta \dot{U}_j$ is the relative normal velocity across the connection plane, and Δt is the length of the current time step. The damping force opposing relative motion tangential to the connection plane is defined in an analogous manner. In this case, however, the relative tangential velocity at the connection plane depends on both the relative translational velocity and the relative rotational velocity of the nodes (Fig. 2a). Each of these relative velocity terms must be damped separately to insure a stable solution. Consequently, a second damping coefficient is defined; C_r is the damping coefficient for relative rotational velocity across the connection plane. Test calculations show $C_t = C_r = 0.05$ is effective in damping DLAM failure.

Damage and failure within the sublaminates can affect the composite's resistance to delamination. Since material/element blocks are processed as vectors, passing element data between sublaminates shells and DLAM hex elements is very inefficient. For this reason, sublaminates membrane strains are approximated by the DLAM element using translational degree of freedom data and lengths determined from element coordinates. These strains can be used to estimate the level of damage within a sublaminates, and delamination resistance can be changed accordingly. When the sublaminates membrane strains reach a critical value, the penalty parameters are modified to simulate the coupling between sublaminates damage and delamination. (This feature was not used in the analyses presented below in the Example section).

The minimum time step used for the explicit time integration of the governing equations depends on the highest eigenvalue (vibrational mode) in the mesh. The presence of DLAM elements in a mesh can introduce or modify high frequency vibrational modes. For this reason, formulae have been derived to estimate the frequency of those vibrational modes that might impact the choice of the stable time increment. These estimates are based on simple idealizations that capture the main features of the mode of interest. A through-the-thickness vibrational mode exists when two sublaminae are connected together by springs restraining relative normal displacements. A two mass, one spring system is used to estimate this frequency. The frequency depends on the mode I penalty parameter (stiffness) K_n . The highest extensional (membrane) vibrational frequency of sublaminae joined by a DLAM element is greater than that of the individual sublaminae. When the sublaminae are vibrating 180° out of phase, the mode II connection (with stiffness K_s) is stretched. This effectively increases the sublaminae extensional stiffness; more force is required to extend the sublaminate. The frequency estimate is based on a bar's natural frequency using a stiffness that includes the effect of the mode II connection. The highest vibrational frequency associated with sublaminate transverse shear stiffness is also increased when a DLAM element connects the sublaminae. A satisfactory estimate for a shell's highest shear frequency is the eigenvalue of the lumped mass-shear stiffness matrix for a linear beam element [11]. When a DLAM element is present, the shear stiffness matrix must include the rotation-induced contribution to moment generated by the mode II DLAM connection.

Examples

Calculated results for double cantilever beam (DCB; mode I) and end notched flexure (ENF, mode II) specimens are presented. These results are compared to measured data to assess the ability of the present approach to reproduce observed behavior. Particular attention is placed on examining the possible role of crack bridging forces. Results are also presented for a diametrically compressed ring to demonstrate the capacity to analyze progressive failure in a highly deformed composite structure.

The E-glass/polyester composite fracture specimens analyzed are reinforced with a basket weave fabric that is woven from rovings that are ~ 5 mm wide. Since the composite contains a clearly defined characteristic length scale, finite element meshes with 5 mm square elements are used in all calculations. This approach is consistent with the "critical stress over a fixed area" delamination criterion. A phenomenological model is used to define the material's response to in-plane loadings. The model is based upon measured tensile, compressive, and shear stress-strain data, and includes fiber dominated tensile and compressive failures, the effect of tensile cracking on stiffness, and large scale shear nonlinearity. See Appendix I for a full description of this constitutive model and the definition of the material analyzed. The laminates analyzed here are roughly 6 mm thick and contain 12 layers of the fabric reinforcement. For this reason, six, through-the-thickness shell integration points are used in each of the laminate's two sublaminae. Note that reported experimental results are for a quasistatic loading condition. A much faster loading rate (500 mm/s) is used in the analysis to reduce the number of time steps required to complete the calculation. Nevertheless, as will be shown below, inertia effects are quite limited at this loading rate, and the calculated results are applicable to a quasistatic loading. Finally note that the initial values for the area weighted penalty parameters are based on Eqs. 1 and 2, and $\bar{K}_n=2$

kN/mm³ while $\bar{K}_t=10$ kN/mm³.

The DCB specimen geometry is defined in Figure 4a. Calculations were carried out for a range of U_{crit} values, and the calculated results are in good agreement with experiment when $U_{crit}=0.004$ mm and $U_{max}=0.15$ mm. This value of U_{max} is not so large that a constraint force is still present after the delamination front moves forward (there is no crack bridging force). The analysis predicts that the delamination grows stably with increasing load point displacement (Fig. 4); a result consistent with experimental observation. Figure 5 compares the calculated load vs. load point displacement relation with results from three DCB tests. As is common practice in a DCB test, the specimen was loaded until the delamination extended about 10 mm, and then unloaded to measure the specimen compliance. This procedure was repeated until the delamination was roughly 100 mm long. For clarity, the unloading curves are removed from the plotted test data, and the remaining portions are associated with a propagating delamination. Test results for the initial peak load show considerable variation between the three tests. The delamination growth was initiated from an artificially induced delamination. The variability in the measured peak load suggests variability in the character of the inserted flaw and in the initiation of delamination growth from this flaw. After the first unloading, the measured data for the three tests are quite consistent, and the calculated result is in excellent agreement. The calculated load vs. crack length relation also agrees with the test data (Fig. 6). Calculated results are shown for a DCB with its arms pulled apart at a velocity of 500 mm/s and also at 250 mm/s. These results are nearly identical. Inertia effects are quite limited at this loading rate, and the calculated results are applicable to a quasistatic loading (although not plotted, the calculated load vs. load point displacement relations for the two applied velocities are also nearly identical).

Note that the results presented in Figs. 4 to 6 were calculated using the "DLAM with damage" method to control the failure process. A U_{max} value of 0.15 mm was used (Fig. 3). Calculations were also carried out using the "DLAM with damping" method to control the failure process with $C_t = C_r=0.05$. As shown in Fig. 7, both methods for controlling the failure process yield similar results.

Visual observations made during the DCB tests suggest the presence of partially failed material behind the delamination front that spans the delamination plane [9]. For this reason, calculations that simulate the presence of crack bridging forces were also performed. The value of U_{crit} was arbitrarily set to 0.002 mm, and U_{max} was varied. When $U_{max}=0.30$ mm, the calculated load vs. load point displacement relation is in good agreement with the test data (Fig. 8). Indeed, this fit is arguably better than that attained without bridging, since the analysis with bridging shows the same sort of flattened peak load as the experimental data (compare Figs. 5 and 8). With this choice of U_{max} , crack bridging constraint forces span the delamination at a distance one element length (5 mm) behind the advancing front. The calculated load point displacement vs. delamination length relation is also in good agreement with the test data, and the match with the data is comparable to that achieved by the analysis with no bridging (Fig. 9). These results highlight the difficulty in identifying a unique set of DLAM failure parameters using the sort of data typically measured during DCB tests. An independent measurement of the bridging length may be required.

Figure 10 defines the ENF specimen analyzed. The lower supports are fixed, and the upper load ram is displaced downward at 500 mm/s. Frictionless contact between the supports, load ram, and

specimen are maintained using PRONTO3D's general capabilities to enforce interpenetrability conditions between specified materials [12]. As in the DCB analysis, calculations were carried out for DLAM parameters chosen so that no crack bridging forces are present and also for parameters such that bridging forces are present. When no bridging forces are present, results for a V_{crit} value of 0.003 mm are in good agreement with the measured peak load (Fig. 11; the "DLAM with damping" method was used to control the failure process). The analysis indicates that once initiated, the delamination propagates rapidly until it approaches the central loading ram and then slows. This type of response is frequently observed in ENF tests of unidirectionally reinforced composites, and is expected in ENF specimens since the energy release rate increases as the delamination begins to grow [13]. Note, however, delamination growth did not occur in an abrupt, unstable way in the tested fabric reinforced composites. Measured load vs. load point displacement relations show that a roughly constant load is maintained once delamination growth is initiated. Calculated results that include crack bridging forces do display the observed behavior. When V_{crit} is set to 0.00125 mm, a V_{max} value of 0.25 mm produces calculated results that are in good agreement with the test data (Fig. 11). This result suggests the possible importance of including fiber bridging effects when modeling delamination growth in fabric reinforced composites.

The final example presented here is that of a diametrically compressed ring (Fig 12a). The ring is compressed between two, rigid, frictionless platens, with the top platen displaced downward at 500 mm/s. The calculation uses the same DLAM failure parameters as used in the previously discussed DCB and ENF analyses that included bridging forces. This example is presented to illustrate the capacity of the present approach to analyze a highly deformed composite structure that fails progressively with multiple failure modes. The deformed geometry is shown in Fig 12, and the calculated load vs. load point displacement is plotted in Fig 13 (solid line). The initial load plateau is associated with the initiation and growth of delaminations within the ring. Delaminations first appear in the regions between the 0° , 90° , 180° , and 270° ring positions (0° is at the top of the ring), and the ring is completely delaminated by the time that the load point displacement equals 12 mm. The first abrupt load drop occurs as the ring undergoes a partial flexural failure (outer layers fail in compression, inner layers fail in tension with one or more layers still intact) at the 0° and 180° positions. The second abrupt load drop occurs as the ring undergoes a partial flexural failure at the 90° and 270° positions. The deformed ring then takes on a peanut shape, and contains four hinge-like regions. Further deformation continues at a nearly constant applied load. A similar calculation, but with the delamination failure mode suppressed, was also carried out. As shown in Fig. 13, the inclusion on the delamination failure mode can have a significant effect on calculated results.

Summary

A method for modeling the initiation and growth of discrete delaminations in shell-like composite structures has been developed and implemented in a three-dimensional, finite element code that uses explicit time integration to advance the equations of motion. This approach uses a special, 8-noded hex constraint element to connect shell elements (sublaminates) and makes them act as a single shell (laminate) until a prescribed failure criterion is attained. When the failure criterion is reached, the connection is broken, and a discrete delamination is initiated or grows. This method

of analysis has been applied to double cantilever beam and end notched flexure fracture specimens. Calculated results were compared to measured data, and the present approach reproduced the observed behavior. A diametrically compressed ring was also analyzed. This calculation demonstrated the capacity of the present approach to analyze a highly deformed composite structure that fails progressively with several failure modes operating simultaneously.

Although this work demonstrates the potential usefulness of the DLAM constraint element, a number of important issues deserve additional attention. In the present study, a "critical stress over a fixed area" delamination criterion was used. This criterion is most appropriate when an obvious, macroscopic characteristic length scale exists, and all elements conform to this length scale. Clearly this approach has limitations, including mesh dependence when element size is allowed to vary. Mesh dependence problems can be avoided if an energy release rate fracture criterion is used. However, the applicability of a linear elastic fracture mechanics approach when other energy absorbing failure modes are also present is questionable. The need to identify the most appropriate delamination criterion is obvious. There is also a need to develop a better understanding of the influence of in-plane laminate stress states and damage levels on delamination initiation and growth. For example, one expects that in-plane compressive stress will effect delamination failure levels in fabric reinforced composites that contain significant yarn crimp. Perhaps one of the biggest challenges is to identify practical test configurations that can identify and define such effects. Finally, note that the present approach for modeling composite delamination can be readily extended to adhesively bonded joints and bonded reinforcements (e.g. doublers and patches). The adhesive bond is modeled with DLAM elements, and the adherends are modeled with shell elements.

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Appendix I: Fabric Reinforced Composite Constitutive Model

The material model for the in-plane response of the fabric reinforced composite laminate analyzed in the Example section is discussed here. This model is consistent with the needs of a crashworthiness analysis. It includes all the primary failure modes in as simple a way as possible, and defines stress-strain response to ultimate failure and material separation. The model is based upon measured tensile, compressive, and shear stress-strain data. It includes fiber dominated tensile and compressive failures, the effect of tensile cracking on stiffness, and large scale shear nonlinearity. The model allows the material to load, unload, reload, etc. prior to ultimate failure since load reversals can occur in crashworthiness analyses.

Stress is resolved into a material coordinate system whose axes are aligned with the two orthogonal yarn directions. The Poisson's ratio for a typical plane weave fabric is relatively low (~ 0.10). Based on this observation and the need for simplicity, it seems reasonable to neglect the coupling between the two orthogonal fiber directions. Furthermore, it is assumed that shear response is independent of yarn extension. This idealization greatly simplifies the material model; the model only needs to reproduce the measured yarn-aligned and pure shear stress-strain relations.

First consider stress associated with yarn-directed extension. Figure A1 shows a piece-wise linear representation of the measured response for the composite analyzed in this paper (see [9] for more information on test methods and experimental data). This material has equivalent properties in the two yarn directions. The stress-strain model is similar to that of an elastic-damaging material. The material is initially linear elastic, with the same modulus in tension and compression. In compression, the material remains linear elastic until failure initiates. After peak load, the composite maintains a constant crushing strength. In tension, matrix cracking begins to reduce the modulus once the stress level exceeds $\sim 10\%$ of the tensile strength. Test data indicates that this composite's tensile strength is 40% greater than its compressive strength. Beyond the linear elastic regime, the modulus is degraded, and equals the slope of the secant from the origin to the current stress-strain value. Any unloading (i.e., a stress reversal after the material is damaged) follows this secant path. Note that tension and compressive stress-strain relations are defined independently (except for a common linear elastic modulus), and degraded tensile and compressive modulus are also defined independently. For example, if a composite is loaded in tension to a stress level that degrades its modulus, and it is then unloaded into compression, its compressive modulus equals the initial linear elastic modulus. This response is consistent with tensile cracks closing in compression. Once the composite completely fails in tension (degraded modulus equals zero), it can no longer carry any compressive stress, and vice versa.

Tests results for the woven roving reinforced composite laminate show that it exhibits one unusual

failure mode. Delamination occurs under a purely tensile loading at a strain level of roughly 1%. Upon unloading, all the fabric plies are separated. This behavior is presumably associated with the straightening of yarn crimp. If subsequently loaded in compression, such delaminations will greatly reduce the composite's compressive stiffness and strength. For this reason, the compressive modulus is reduced by a fixed percentage (e.g. 10% of the value that would be otherwise be present) once the tensile delamination limit is exceeded. (This feature was not used in the analyses presented in the Example section since this type of response is not anticipated in those problems presented).

The composite's measured shear stress-strain is highly nonlinear. Figure A2 shows the idealization used for the composite analyzed in this paper. It is initially linear elastic, followed by power-law-like response with isotropic hardening. Shear strain levels are unlimited, and unloading follows the secant from the absolute value of the peak stress-strain value attained and its reflection through the origin.

This section concludes with a summary of the required input data to fully specify the fabric reinforced composite constitutive model. The yarn directed stress-strain relations are defined by four functions, and each function is constructed from piece-wise linear segments. In each yarn direction, one function defines the tensile response, and one function defines the compressive response. These functions are specified independently, except for a common linear elastic modulus. A different stress-strain relation can be defined for each yarn direction. This would be appropriate when warp and fill have different fiber volume fractions. In addition, a tensile delamination limit, and an associated compressive modulus reduction factor is specified for each yarn direction. Three material parameters define the shear response: shear modulus, an initial cracking strain, and an hardening exponent.

$$\Delta U_j = \Delta U$$

1a)

$$F_n = K_n \Delta U_j$$

1b)

1c)

Figure 1. Approach used to connect shells by penalizing normal (mode I) displacement across the connection plane. 1a) relative normal displacement across the connection plane, 1b) forces acting on the connection plane, 1c) equivalent nodal forces.

$$\Delta V_j = \Delta V - (h/2) \Delta \omega$$

2a)

$$F_t = K_t \Delta V_j$$

2b)

$$M = (h/2) F_t$$

2c)

Figure 2. Approach used to connect shells by penalizing tangential (mode II) displacement across the connection plane. 2a) relative tangential displacement across the connection plane, 2b) forces acting on the connection plane, 2c) equivalent nodal forces and moments.

Figure 3. The reduction of the area weighted penalty parameter $\bar{K}_n$ during failure is defined by the constraint force per nodal area F_n/A vs. the penalized displacement ΔU_j relation.

4a) DCB geometry, sublaminates 3.05 mm thick.

4b) Time = 0.0175 s, delamination length = 60 mm.

4c) Time = 0.0350 s, delamination length = 85 mm.

Figure 4. Double cantilevered beam analysis (all dimensions in mm).

Figure 5. Comparison of calculated double cantilevered beam load vs. load point displacement relation with test data ($U_{crit} = 0.004$ mm, $U_{max} = 0.15$ mm).

Figure 6. Comparison of calculated double cantilevered beam load point displacement vs. delamination length relation with test data ($U_{crit} = 0.004$ mm, $U_{max} = 0.15$ mm).

Figure 7. Comparison of double cantilevered beam load point displacement vs. delamination length relations calculated using the “DLAM with damping” and the “DLAM with damage” approaches to control the failure process.

Figure 8. Comparison of calculated double cantilevered beam load vs. load point displacement relation with test data ($U_{crit} = 0.002$ mm, $U_{max} = 0.30$ mm).

Figure 9. Comparison of calculated double cantilevered beam load point displacement vs. delamination length relation with test data.

Figure 10. End notched flexure geometry, sublaminates 3.06 mm thick (all dimensions in mm).

Figure 11. Comparison of calculated end notched flexure load vs. load point displacement relation with test data.

Fig. 12a) Diametral ring test geometry, sublaminates 3.2 mm thick.

Fig. 12b) Ring compressed 10 mm. Partial flexural failures are present at the 0° (top) and 180° positions and 75% of ring is delaminated.

Fig. 12c) Ring compressed 20 mm. Partial flexural failures also present at the 90° and 270° positions and the ring is fully delaminated.

Figure 12. Diametral ring test analysis (all dimensions in mm).

Figure 13. Calculated load vs. load point displacement relations for a diametrically compressed ring that either include or suppress the delamination failure mode.

Figure A1. Fabric reinforced composite stress-strain relation for yarn-directed extension.

Figure A2. Fabric reinforced composite shear stress-strain relation.

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