Dynamic micromechanical modeling of textile composite strength under impact and multi-axial loading

Ryan L. Karkkainen*
University of Miami, Department of Mechanical and Aerospace Engineering, Coral Gables, FL 33146, USA

1. Introduction

For inhomogeneous materials such as woven and textile composites with relatively complex microgeometries, finite element micromechanical analysis tools can be employed in highly detailed simulations of material failure behaviors. Investigation of non-uniform through-volume stress distributions and subsequent micromechanical failure analyses can shed great insights which can be used to optimize or tailor the mechanical response of a material system. In dynamic loading cases, further complexities arise due to non-periodicity of loading, material rate effects, as well as impedance mismatching of stress wave propagation. Failure modeling must account for dynamic loading effects and consideration of multiple failure modes to accurately simulate structural or impact mechanical performance.

Two-dimensional (2D) and three-dimensional (3D) textile composites can offer advantages over laminated composites and often greatly alleviate the delamination failure mode, especially in the case of thick-section composites. Such microgeometries are of critical importance to delamination resistance, an otherwise common weak point of thick composite structures. The out-of-plane undulation of a 2D or the direct through-thickness tows of a 3D woven composite both provide delamination resistance and generally increase through-thickness properties [1,2]. However, there is an unavoidable trade-off, as this also leads to loss of in-plane properties, as the weaving leads to interruption of in-plane fiber tows less in-plane aligned reinforcement.

The increased microstructural complexity of textile composites also leads to increased complexity of characterization and analysis, as well as a need for non-traditional analysis methods. Composite laminate theories and simple approximations will no longer apply. An accurate model must accommodate the fact that 3D woven composites exhibit multiple potential failure mechanisms [3], which depend upon the loading conditions and particulars of the layup and materials. 3D weaves show improved damage resistance, as well as more capacity to absorb multiple strikes before perforation and show less damage localization [4] in comparison to 2D weaves. This can be of particular interest in armor applications, and related modeling has addressed issues surrounding the computational needs required to reflect the energy absorption modes and damage progression [5].
Textile composite structures are often designed based upon traditional well-known phenomenological failure criterion, such as the maximum stress criterion, maximum strain criterion, and quadratic interaction criterion such as the Tsai-Hill and Tsai-Wu failure theories [6,7]. Other failure theories for orthotropic composites include the strain invariant failure theory (SIFT) [8,9] and failure mode concept (FMC) [10] approach.

Dedicated analysis of 2D and 3D textile composites has been effectively approached before [11–15], but strength prediction is still a topic of current research and ongoing development. FEM micromechanical methods for strength modeling of textile composites have been explored in previous works by the authors [16–18]. Therein, it was shown that many common assumptions and traditional micromechanical analysis techniques break down, due to the size and complexity of a textile representative volume element (RVE) in comparison to a unidirectional composite RVE, thus improvements to these techniques have been presented. Further work developed additional consideration of interface failure effects [19] such as tow pullout, and extension of these efforts into the dynamic regime [20].

The current work involves application of FEM micromechanics to 3D orthogonal weave and 2D plain weave textile architectures to provide detailed strength analysis for failure prediction. Loading cases and failure envelopes developed in Ref. [20] have been expanded to include some multi-axial cases. Further, a great deal of scrutiny has been applied to investigate differences between the applied nominal strain-rate actuated through boundary conditions and the local strain rates calculated (by rate of deformation tensor within a finite element formulation) within an RVE in an inherently inhomogeneous response. Explicit dynamic finite element modeling has been employed to investigate failure of 2D and 3D textile composite RVEs. Parametric investigation includes: 2D plain weave vs. 3D orthogonal textile microgeometry, two different loading rates (1000 and 10,000 strain/s), and both tensile and compressive loading, including some multi-axial cases.

2. Methods

2.1. Textile composite RVE formulation and analysis

A representative volume element (RVE) is the smallest geometrical unit which represents the textile microstructure when arrayed or repeated, and the analysis of which can accurately determine the material continuum behavior. Figs. 1 and 2 depict both a 2D plain weave and 3D orthogonal RVE as implemented into a finite element mesh. Corresponding dimensions and material properties are listed in Table 1 through 4.

Effort was taken to investigate RVE geometries with some reasonable parity of dimensions. Note that the fiber tows themselves have a roughly 65% volume fraction and thus total volume fraction is less than 50% given that interstitial matrix is present between tows. In the case of the plain weave tow which exhibits some out of plane undulation (around 13.5° from horizontal at maximum), material properties are assigned to multiple local coordinate systems which follow the undulation angle.

To implement the interface failure model, each fiber tow has been surrounded with cohesive elements which essentially comprise a “sleeve” that binds the tow phase to the interstitial matrix. The 2D plain weave mesh (Fig. 2) consists of 248,351 total elements. Of these 43,813 are linear hexahedral elements of type C3D8R which comprise the fiber tows, while 186,212 linear tetrahedral elements represent the matrix (the meshed region is sufficiently complex that meshing algorithms require tetra elements) region, and 18,326 cohesive COH3D8 elements are used for the interface regions. A cohesive element is not indifferently symmetric and must be oriented such that its integration points lie along the bondline, thus defining inward and outward normals to the potentially debonded surface. Failure to do so will result in very inaccurate results. Note that for the 2D plain weave geometry, automatic meshing routines were not adequate to proper definition of the cohesive mesh regions without significant time consuming user adjustments. These meshing issues were not present in the case of the 3D orthogonal mesh, which was geometrically simpler (orthogonal, symmetric, non-undulating) in composition. The 3D orthogonal mesh consists of 221,190 total elements. Of these 38,406 are COH3D8 cohesive elements representing the interfaces, and 182,784 are C3D8R elements representing the fiber tows and interstitial matrix regions. Mesh density was chosen to be comprehensively dense for proper representation of inhomogeneous distributions and gradients in stress, strain, and strain rate.

![Fig. 1. Representative volume element for a 2D plain weave composite.](image1.png)

![Fig. 2. Representative volume element for a 3D orthogonal woven composite.](image2.png)

<table>
<thead>
<tr>
<th>Dimension</th>
<th>a</th>
<th>b</th>
<th>h</th>
<th>w₁</th>
<th>w₂</th>
</tr>
</thead>
<tbody>
<tr>
<td>Description</td>
<td>RVE width</td>
<td>RVE depth</td>
<td>RVE height</td>
<td>tow width</td>
<td>tow spacing</td>
</tr>
<tr>
<td>Value (mm)</td>
<td>6.0</td>
<td>6.0</td>
<td>1.38</td>
<td>2.11</td>
<td>4.60</td>
</tr>
</tbody>
</table>

Table 1

Plain weave RVE dimensions (mm).
stress levels for each failure mode, including axial stress state within the strength represents both the different failure modes. As per Table 4 (in this case the matrix properties and are shown in Tables 5 and 6. In some cases presented herein, a cohesive zone interface are investigated, as seen here. A model employing cohesive elements was previously developed to include rate dependency and mode effects on failure strain at the interface [19]. Thin cohesive elements were used to represent the interface region between the fiber tows and interstitial matrix, using a 3D continuum constitutive law. Interface failure is based on maximum strain employing a Johnson–Cook formulation to adapt the maximum allowable strain based on both strain rate and fracture mode mixity. The failure expression of Equation (1) is suitable for use to describe bulk matrix failure and interface failure, both of which are isotropic in nature. The failure parameters can be adapted based on the matrix properties and are shown in Tables 5 and 6. In some cases presented herein, a “weak” and “strong” interface are investigated, as seen here.

\[\varepsilon_{\text{max}} = [d_1 + d_2 \exp(-d_3 \eta)] \left[ 1 + d_4 \ln \left( \frac{t}{t_0} \right) \right]\]  

where \(d_i\) represent coefficients which quantify rate (\(d_4\)) or mode (\(d_2\) and \(d_3\)) effects, is the reference strain rate, and \(\eta\) is the triaxiality of the stress state.

Fiber tow failure in both weaves is treated as orthotropic failure as per Table 4 (in this case the “T” subscript indicating transverse strength represents both the y and z direction failure value in three dimensions) and implemented via an anisotropic yield model. The stress state within the fiber tow is compared against allowable stress levels for each failure mode, including axial fiber breakage or transverse tow pull-apart, or shear failure generally corresponding to matrix failure. Once failure occurs in an element, the element is degraded completely and removed from the analysis. A form of the Hill yield criterion is used as shown in Equation Set 2, which is written in terms of applied stresses compared to failure stresses, where the latter are indicated with a “\(Y\)” superscript.

\[F(S_y - \sigma_y)^2 + G(S_z - \sigma_z)^2 + H(S_x - \sigma_x)^2 + 2Lr_{yz}^2 + 2Mr_{zx}^2 + 2Nr_{xy}^2 = 1\]

\[F = \frac{1}{2} \left[ \frac{1}{{\sigma_y}^2} \left( \frac{1}{{\sigma_y}^2} + \frac{1}{{\sigma_x}^2} + \frac{1}{{\sigma_z}^2} \right)^2 \right]^{\frac{1}{2}} \]

\[G = \frac{1}{2} \left[ \frac{1}{{\sigma_x}^2} \left( \frac{1}{{\sigma_x}^2} + \frac{1}{{\sigma_y}^2} + \frac{1}{{\sigma_z}^2} \right)^2 \right]^{\frac{1}{2}} \]

\[H = \frac{1}{2} \left[ \frac{1}{{\sigma_z}^2} \left( \frac{1}{{\sigma_z}^2} + \frac{1}{{\sigma_y}^2} + \frac{1}{{\sigma_x}^2} \right)^2 \right]^{\frac{1}{2}} \]

\[L = \frac{1}{2} \left( \frac{r_{yz}^2}{{r_{yz}^2}} + \frac{1}{{r_{zx}^2}} + \frac{1}{{r_{xy}^2}} \right) \]

Failure occurs when the value of Equation (2) exceeds unity. In two-dimensional stress space, the failure envelope is of an elliptical nature which is an appropriate formulation to fit the observed failure behavior of orthotropic composites.

Abaqus™ dynamic explicit analysis is performed, and periodic boundary conditions are applied [18] to ensure maintenance of the response of the RVE as a repeatable element. However, it must be noted that dynamic loading is not periodic. Infinite elements (CIN3D8) are positioned at the RVE bound to prevent spurious wave reflections at the artificial RVE edges. Thus, the current analysis represents the response of the material at a given point to a given strain rate passing as a dynamic stress wave field. Because time-position-history of the stress field is not indifferent, results cannot be superposed and different loading conditions must be analyzed explicitly.

Loading is applied through a velocity (\(V\)) boundary condition which essentially sets the applied strain rate. In the current analysis, applied strain rates of 10,000 strain/s and 1000 strain/s have been investigated. For a velocity boundary condition, the total macro-level strain applied to the RVE can be found by integrating the velocity boundary condition. For constant acceleration, this is

**Table 2**

Orthogonal weave RVE dimensions (mm).

<table>
<thead>
<tr>
<th>Dimension</th>
<th>a (mm)</th>
<th>b (mm)</th>
<th>h (mm)</th>
<th>(t_1) (mm)</th>
<th>(t_2) (mm)</th>
<th>(w_1) (mm)</th>
<th>(w_2) (mm)</th>
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</thead>
<tbody>
<tr>
<td>Description Value (mm)</td>
<td>RVE width</td>
<td>RVE depth</td>
<td>RVE height</td>
<td>warp (half) thickness</td>
<td>weft thickness</td>
<td>warp width</td>
<td>weft width</td>
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<td>---</td>
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</tr>
<tr>
<td>7.25</td>
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<td>1.68</td>
<td>0.49</td>
<td>0.70</td>
<td>6.62</td>
<td>4.75</td>
<td></td>
</tr>
</tbody>
</table>

**Table 3**

Fiber tow and matrix material properties.

<table>
<thead>
<tr>
<th>Material</th>
<th>(E_1) (GPa)</th>
<th>(E_2) (GPa)</th>
<th>(G_{12}) (GPa)</th>
<th>(\nu_{12})</th>
</tr>
</thead>
<tbody>
<tr>
<td>S2 Glass Composite (65% Fiber Volume)</td>
<td>71.1</td>
<td>16.2</td>
<td>6.46</td>
<td>0.25</td>
</tr>
<tr>
<td>Neat BMI Resin</td>
<td>4.60</td>
<td>4.60</td>
<td>1.76</td>
<td>0.31</td>
</tr>
</tbody>
</table>

**Table 4**

Fiber tow and matrix strength properties (MPa).

<table>
<thead>
<tr>
<th>Material</th>
<th>(S_{11})</th>
<th>(S_{12})</th>
<th>(S_{22})</th>
<th>(S_{33})</th>
<th>(S_{44})</th>
</tr>
</thead>
<tbody>
<tr>
<td>S2 Glass Composite</td>
<td>1705</td>
<td>550</td>
<td>45</td>
<td>150</td>
<td>50</td>
</tr>
<tr>
<td>Neat BMI Resin</td>
<td>103</td>
<td>103</td>
<td>103</td>
<td>103</td>
<td>-</td>
</tr>
</tbody>
</table>

Running times were not overly demanding, and depending on the particular loading rate or mesh would typically require 16 to 32 CPH (cpu-hours) on an SGI Altix 4700 shared memory system with 512 cores per node. Each core is a 1.6 GHz processor and has access to 4 GB of system memory.

**Table 5**

Interface strength properties and coefficients used in textile impact simulations.

| \(\gamma_{\text{max}}\) | 0.042 | 0.045 | 0.1 | -0.0550 | -0.0531 | 0.110 | 0.50 s^{-1} |
|\(\gamma_{\text{max}}\) | 0.010 | 0.012 | 0.1 | -0.0880 | -0.0225 | 0.110 | 0.50 s^{-1} |

**Table 6**

Strength properties and rate/mode effect coefficients used for BMI polymer failure.

<table>
<thead>
<tr>
<th>(\gamma_{\text{max}})</th>
<th>0.022</th>
<th>0.025</th>
<th>0.1</th>
<th>-0.0750</th>
<th>-0.0390</th>
<th>0.110</th>
<th>0.50 s^{-1}</th>
</tr>
</thead>
<tbody>
<tr>
<td>(\gamma_{\text{max}})</td>
<td>0.010</td>
<td>0.012</td>
<td>0.1</td>
<td>-0.0880</td>
<td>-0.0225</td>
<td>0.110</td>
<td>0.50 s^{-1}</td>
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<table>
<thead>
<tr>
<th>(d_1)</th>
<th>(d_2)</th>
<th>(d_3)</th>
<th>(d_4)</th>
<th>(i_0)</th>
<th>0.50 s^{-1}</th>
<th>0.50 s^{-1}</th>
</tr>
</thead>
</table>
\[ V = \left( \frac{V_f - V_i}{t_f - t_i} \right) t + V_i \]  

And for zero initial conditions this is

\[ V = \left( \frac{V_f}{t_f} \right) t \]  

So displacement is then

\[ \delta = \int V dt = \frac{1}{2} \left( \frac{V_f}{t_f} \right) t^2 \]  

Or, in the case of constant applied velocity employed herein, displacement is simply the product of velocity and time. Average uniaxial macro-level strain applied by the boundary condition is approximated as

\[ \varepsilon = \delta \frac{1}{L} \]  

It then follows that macro-level strain rate across the RVE is approximated as

\[ \dot{\varepsilon} = \frac{V}{L} \]  

However, some important distinctions must be made as to how strain rate is calculated within a finite element software package using an explicit solver scheme. This is calculated as the rate of deformation tensor

\[ D_{km} = \frac{1}{2} \left( \frac{\partial V_k}{\partial x_m} + \frac{\partial V_m}{\partial x_k} \right) \]  

which for small deformations does not differ appreciably from the time derivative of the strain tensor. In an explicit solver, element acceleration is basically calculated as the difference in external and internal forces divided by the mass, and from this, a velocity \( V \) field is integrated. The spatial gradient of the velocity field is then calculated to determine strain rates from the rate of deformation tensor of Equation (8). A finite element formulation must make calculations based on local quantities, and there is no computational method by which an overall RVE length can be accommodated in analog to Equations (6) and (7). The idea presented here is that a macro-level continuum failure code which includes strain-rate dependent behavior (rate-dependent stiffness and/or strength) must accurately accommodate the fact that there will be differences in a macro-level strain rate and the element-level strain rate at which failure phenomena occur. Thus, given a material with inhomogeneities and disparate stress wave speeds and the methods by which finite element solvers determine strain rate, one should expect variation between a boundary-applied strain rate and the internal strain rate. They should not be expected to be the same.

3. Results

Strain rate distributions across the RVE are shown for uniaxial load cases involving 2D and 3D microgeometries at 1000 and 10,000 strain/s nominal applied strain rates. Contours are shown just before initial failure occurs, to illustrate the strain rates in effect which drive the failure process. Please refer to [20] for further details on the stress response and failure behavior of these loading cases, which is too lengthy to repeat here. Multi-axial loading cases which expand on the results of [20] are shown herein, as well as failure envelopes which define the failure behavior of the textile architectures.

3.1. Textile RVE strain rate distributions

Explicit dynamic impact analysis at two strain rates has been performed, resulting in detailed strain rate contours by which one can evaluate the validity of an assumed uniform applied strain rate. Note again that complementary stress contours at the point of failure initiation and the point of ultimate failure are shown in Ref. [20]. As will be detailed, for the 2D plain weave, the strain rate response was very non-uniform and significantly more complicated than the 3D weave, owing largely to the tow undulations. Calculated internal strain rates diverged appreciably from the macro-level boundary-applied strain rate. In contrast, for the 3D orthogonal weave, the results were fairly uniform and relatively closer to the boundary-applied strain rate. It has been observed that non-uniformity of stresses is guaranteed, and uniformity of strain-rate is not guaranteed (but possible).

3.1.1. 2D plain weave strain rate distributions

3.1.1.1. 1000 strain/s compressive. Fig. 3 shows the strain rate distribution in the fiber tows of the plain weave RVE at a time point just prior to failure initiation. Similarly, Fig. 4 shows the strain rate distribution for the interstitial matrix material. As can be seen, the
strain rates are not uniform. Further, the maximum strain rate seen in the tows was as high as 2500 strain/s at the maximum inflection points in the curvature. Bending and buckling effects led to local strain rates much larger than that applied at the bounds. Further, also due to bending effects, local strain rate could even be seen to be tensile in small regions. Strain rates seen in the matrix were generally 20% higher in comparison to the fiber tows, with increased strain rates mostly concentrated near the phase interface with the fiber tows. This is counterintuitive given the slower stress wave speed in the more compliant phase, but it must be noted that a 2D weave is actually a deceptively complicated microstructure. If one observes a profile or cross-section of a 2D weave, it will be seen that a linear stress wave passing through the RVE will pass through regions of both materials alternatingly. Thus, not only will wave speed will fluctuate, but minor wave reflections will occur each time a phase boundary is encountered. The net effect is that even at the level of a single RVE at a relatively low impact level, the strain rate distribution is far from simple.

3.1.1.2. 1000 strain/s tensile. Figs. 5 and 6 show the strain rate distribution in the tows and matrix of the plain weave RVE at a time point just prior to failure initiation for a boundary-applied rate of 1000 strain/s tensile. A cross-sectional view is applied to illustrate the distribution across a tow.

3.1.1.3. 10,000 strain/s compressive. Figs. 7 and 8 show the strain rate distribution in the tows and matrix of the plain weave RVE at a time point just prior to failure initiation under boundary-applied 10,000 strain/s compressive loading. Note that at the higher impact levels, initial damage occurs before the stress wave has propagated across the RVE. Here, the greatest portion of the RVE remains at reference state. The strain rate towards the leading edge of the tows varies from roughly 8700 to 11,800 strain/s. As before, the matrix exhibits a higher strain rate than that seen in the fiber tows was around 1700 strain/s at the leading edge and maximum inflection points in the curvature. Bending effects are still encountered, essentially due to “straightening” of the tows as they are pulled in tension. As seen previously, small regions of compressive strain rate could be seen due to bending. Strain rates observed in the matrix were again generally 20% higher in comparison to the fiber tows, mostly concentrated near the phase interface with the fiber tows. Fig. 5 is displayed with a cross-section to further illustrate the strain rate distribution across the thickness of the tow. Further note that six elements through thickness here is capable of accurately capturing large gradients in any field quantity, such as stress, strain, or strain rate. A great deal of non-uniformity is seen, even over this relatively small spatial dimension, including transition from positive to negative quantity.
tows, in this case approximately 30% higher. In general, as seen in Refs. [20], damage is a much more localized phenomenon at progressively higher loading rates.

3.1.1.4. 10,000 strain/s tensile. Figs. 9 and 10 show the strain rate distribution in the tows and matrix of the plain weave RVE at a time point just prior to failure initiation under boundary-applied 10,000 strain/s tensile loading. As with the boundary-applied 10,000 strain/s compressive loading case, initial damage occurs before the stress wave has propagated across the RVE. Also note that, as with the previous lower-rate tensile loading case, matrix failure in the leading edges occurs before failure in the load-carrying fiber tows, thus some failed matrix elements are seen in Fig. 10. Note that region beyond the contour front (in tows and matrix) is at zero strain rate. The strain rate towards the leading edge of the tows varies from roughly 5000 to 11,800 strain/s. As before, the matrix exhibits a higher strain rate than that seen in the fiber tows, in this case approximately 35% higher.

3.1.2. 3D orthogonal weave strain rate distributions

3.1.2.1. 1000 strain/s compressive. Fig. 11 shows the strain rate distribution in the 3D orthogonal weave microgeometry, with tow, matrix, and through-thickness tows shown simultaneously. In the more uniform 3DO structure, and at low rate, just prior to failure, the strain rate is largely uniform and fully distributed across the RVE. Further, the strain rate through the axially-aligned warp tows is reasonably close to the 1000 strain/s applied condition, with little variation. The strain rate in the weft tows was seen to be about 30% lower. Though the correlation is not one-to-one in quantity, this is primarily due to the lower wave speed in the more compliant phase, as the axial stiffness of the weft tows is mostly matrix-driven. The distinct and uniform behaviors seen in the warp and weft tows are also reasonable given the lack of undulation or phase intermingling between tow and matrix (which is unlike the 2D plain weave). In general, the behavior of the 3D weave is closer to a laminate cross-ply architecture, and it lacks the undulation of the 2D plain weave. Failure for the orthogonal weave [20] occurs much more catastrophically than for the 2D plain weave; this is analogous to the commonly observed failure pattern of a uniaxial laminated composite. In this case, the material fails ubiquitously as the stress state and material composition are both relatively uniform. As will be seen, the observations noted here were very consistent across all analysis cases herein.

3.1.2.2. 1000 strain/s tensile. Results for tensile loading were not appreciably different in trend, distribution, or magnitude in comparison to the compressive case, and the uniform response can be seen in Fig. 12.

3.1.2.3. 10,000 strain/s compressive. Some slight non-uniformity of the strain rate distribution is seen for the higher impact loading cases, as seen in Fig. 13. Though the response is still largely uniform, slightly higher rates are seen in some small areas of the tows around the through-thickness stitches and the leading edge matrix zones. This appears to be some combination of stress-concentration effects due to the planar interruption of the stitches and computational edge effects.

3.1.2.4. 10,000 strain/s tensile. Results for tensile loading were not appreciably different in trend, distribution, or magnitude in comparison to the compressive case, as can be seen in Fig. 14. Again, some slight non-uniformity of the strain rate distribution is seen for the higher impact loading cases, due to stress-concentration effects from the planar interruption of the stitches and some edge effects.
3.2. Strength effects of multi-axial loading for textile composite RVE

Multi-axial impact analysis has been performed on a plain weave microgeometry to observe the effects of such loading on macro-level failure envelope. At the RVE level, this amounts to failure analysis at a material point at which two stress waves have intersected. The loading is asymmetric and non-periodic, but this analysis provides valuable insight into the effects of biaxial loading on the material strength under dynamic loading. Biaxial failure is not necessarily distinct; herein, failure is defined as the point at which either warp or weft tows lose load bearing capacity, as opposed to loss of capacity in just one direction. Also, note that in dynamic loading, stress is not fully resolved across the RVE and must propagate over time, thus a given point (or element) in the RVE does not necessarily see a biaxial loading at any time before failure.

3.2.1. Failure behavior of 2D plain weave under Bi-Axial mixed tension/compression loading at 1000 strain/s

Fig. 15 shows the stress contours in the fiber tows at the point of ultimate failure. Von-Mises stress is plotted to give an overall impression of multi-axial stress state. Both axial failure of the fiber tows and matrix microcracking failure of weft tows is present at the point of loss of load bearing capacity. The axial strain \((\varepsilon_x = \varepsilon_y)\) at failure is 0.48% strain, which is roughly 20% lower than the allowable failure strain under uniaxial compression at the same loading rate. Failure is not uniform across the RVE, and the zone of greatest damage occurs around the corner adjacent to both planes of impact loading.

3.2.2. Failure behavior of 2D plain weave under Bi-Axial tensile loading at 1000 strain/s

Fig. 16 shows the stress contours in the fiber tows at the point of ultimate failure. Von-Mises stress is again plotted to give an overall impression of multi-axial stress state. The failure mode is predominantly axial failure of the fiber tows, which occurs nearly simultaneously in the warp and weft directions. In this case, the stress waves in each direction interact significantly and there was a considerable decrease in failure strain to 0.42%, which is roughly 50% lower than the allowable failure strain under uniaxial tension at the same loading rate. Note that in Fig. 16, some of the appearance of asymmetry in the stress contours across the RVE is due to leading edge elements that have failed and been removed from the analysis along the z-axis, versus separated leading edge elements near failure that retain some internal stress along the x-axis. Yet in
both cases, the fiber tows have failed completely across their cross-section and retain no load bearing capacity.

3.2.3. Failure behavior of 2D plain weave under multi-axial mixed tension/compression loading at 10,000 strain/s

Fig. 17 shows the stress in the fiber tows at the point of ultimate failure for mixed biaxial tension and compression. As with the uniaxial cases, damage is more localized at the higher impact rate of a nominal 10,000 strain/s loading, and failure occurs before stress waves have propagated across the RVE. For biaxial loading, the stress waves have little or no interaction at this rate, thus the failure strain is not appreciably changed in comparison to the uniaxial cases [20], although both warp and weft tows fail. Damage modes also mimic the uniaxial cases; along the compressively loaded axis, axial tow failure is seen on the leading edge, along with some transverse failure along the edge of the tow perpendicular to loading. Whereas the tow loaded in tension (x-axis) sees catastrophic localized failure as the end of the tow essentially snaps off under extension.

3.2.4. Failure behavior of 2D plain weave under multi-axial tensile loading at 10,000 strain/s

Fig. 18 shows the stress in the fiber tows at the point of ultimate failure under biaxial tension. As with the uniaxial cases, damage is more localized at the higher impact rate of a nominal 10,000 strain/s loading, and failure occurs before stress waves have propagated across the RVE. For biaxial loading, the stress waves have little or no interaction at this rate, thus the failure strain is not appreciably changed in comparison to uniaxial tensile loading [20], although both warp and weft tows fail. Damage modes also mimic the uniaxial cases, but in this case catastrophic localized failure occurs at the ends of both warp and weft tows, which essentially snap off under extension.

3.2.5. Failure envelopes for 2D plain weave including axial and multi-axial dynamic loading

Fig. 19 shows a failure plot for the 2D plain weave in strain-space ($\varepsilon_x$ vs. $\varepsilon_y$). Failure points at 1000 and 10,000 strain/s are shown. Curves indicated as “fit” in the legend are also shown to indicate the expected form of the failure envelope. It is seen that at the higher 10,000 strain/s loading rate, the RVE fails in a manner consistent with the maximum strain failure theory, as there is little tow interaction and failure is highly localized. This is consistent with the failure envelope in Fig. 19, which takes a rectangular shape in strain-space. Results are less straightforward with the 1000 strain/s loading cases, but a simple “fit” has been applied to approximate the nature of the envelope in cases of mixed loading.

Future work will expand the results to include peak load and energy absorption results quantification. Furthermore, loading will be expanded to include bending and transverse shear loading.

4. Conclusions

Dynamic explicit analysis of a representative volume element (RVE) of 2D and 3D woven textile composites has been performed. Detailed analysis of strain rate distributions within each microgeometry for nominal boundary-applied strain rates of
1000 or 10,000 strain/s has been presented. It is shown that, just as the stress distribution will be very non-uniform, uniformity of the strain-rate distribution is not guaranteed. These effects were greater for the 2D plain weave, owing to the undulation of the fiber tows. Strain rate distributions were generally more uniform in the case of the 3D weaves, but significant differences were seen between warp and weft tows. It has been shown that strain rate distributions in a textile RVE are far from uniform and can diverge in magnitude from an applied macro-level boundary condition. Thus, great caution must be observed in the development of failure theories and the application of continuum level analysis techniques. Just as a stress distribution would be very non-uniform even for simple loading, there are many local effects and variations in strain rate. Given that a failure routine must consider strain rate effects that update stiffness and strength in an element, it should be realized that localized strain rate effects in the region of failure can be in play, meaning that assumptions of uniformity would lead to an inappropriate material law with a spurious assumed uniform strain rate. Homogenization must occur at some level for component/application level analyses to be practical, but suffice it to say that one should conduct sufficiently detailed micromechanical analyses in development of failure theories that attempt to describe textile composite materials.

Failure analysis has been performed for in-plane compressive and tensile loading cases, including multi-axial loading. Highly refined meshes have been employed to ensure convergence and accuracy in such load cases which exhibit large stress gradients across the textile RVE. It has been observed that high rate biaxial cases tend to resemble uniaxial strength levels, as failure occurs rapidly before stress waves have had time to interact and create biaxial stress states in an appreciable portion of the RVE. The effect of strain rate and phase interfacial strength have been included to biaxial stress states in an appreciable portion of the RVE. The effect of strain rate and phase interfacial strength have been included to develop macro-level material failure envelopes for a 2D plain weave and 3D orthogonal microgeometry.

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References