Simulations and experiments for automated fiber placement of prepreg slit tape: Wrinkle formation and fundamental observations

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ABSTRACT

Simulations and experiments for automated fiber placement (AFP) of prepreg slit tape (tow) on a flat surface with different radii of curvature are presented, with emphasis on characterization of wrinkle formation. Bonding of the slit tape to the substrate surface is modelled through a sticky contact definition in Abaqus. Subsequent delamination and wrinkling are predicted through incorporation of measured mixed mode cohesive TSL for contact and bonding of the slit tape and substrate. Comparisons of predicted wrinkle shape, amplitude and wavelength to experimental measurements show excellent agreement for a 6.35 mm wide IM7/8552-1 prepreg tow placed on a flat surface using four different radii of curvature. In addition, simulations are demonstrated to be capable of capturing the mechanisms of wrinkle formation using a generally accepted damage model. Careful inspection of the stress and deformation conditions at the tow-substrate interface under the compaction roller reveals a combination of Mode II and Mode III tractions, with significant damage predicted under the roller due to the Mixed Mode II/III traction conditions. After passage of the roller, nearly instantaneous initiation and growth of wrinkles occur with relatively consistent spacing under predominantly Mode I condition at the interface.

1. Introduction

Significant improvements in weight reduction, higher corrosion resistance and lower maintenance costs are some of the advantages offered by composite materials. Though advanced composite materials with superior mechanical properties have existed for more than six decades, their use has been limited to advanced military aircraft, primarily due to higher manufacturing costs associated with composite materials. Recently, the commercial aircraft industry has developed a generation of advanced composite aero structures to replace conventional aluminum components. Major roadblocks to rapid expansion and deployment of composite-based structures are associated with difficulties in manufacturing of high quality, complex-shaped parts using existing manufacturing technology. Some of the earliest manufacturing approaches, such as conventional hand layup methods, are painstakingly slow and thus unsuitable for high production rate demands. However, the recent introduction of computer aided prepreg placement processes, such as automated tape layup and automated fiber placement, have resolved many of the difficulties in manufacturing of high-quality composite material parts at faster production rates. These developments have led to a shift in interest for some manufacturers in automotive, sports and particularly commercial aircraft industry to exploit many of the superior mechanical properties offered by composite materials. In recent years, the commercial aircraft industry has seen the largest increase in use of composite materials for aircraft structures. For example, the Boeing 787 incorporates advanced composites for more than 50% of the weight of the aircraft [1], resulting in a 20% weight reduction compared to conventional aluminum airframes.

The AFP lay-up process for thermoset composite tows consists of a computer controlled robotic arm to place and adhere bands of tows (8–32 bands) having widths ranging from 3 mm to 12 mm (0.125 in to 0.5 in) along predefined paths [2,3]. The tows are heated while they are being placed on the mold and the placement head applies pressure to ensure proper adherence of tows to the substrate surface. The AFP process has the flexibility to cut and restart individual tows, and independently control feed rate, reducing wastage of materials. Since the

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1 Heating of the tow is performed by an infrared source, laser or gas heater, depending on the application (thermoset/thermoplastic tow).
AFP lay-up process can individually control the feed rate of tows, differential payout of adjacent tows can be performed, allowing the robotic arm to move along curvilinear paths to obtain advantageous variable stiffness properties. In fact, the ability to steer tows at preferred orientation opens a wide range of design options for tailoring properties of composite laminates. For example, orienting fibers within each lamina along optimal paths can result in favorable stress distributions and improved performance of a laminate for specific applications [4,5] without addition of material, with further weight reduction possible. In this regard, buckling analysis performed by Chen et al. [6] and Wu et al. [7] for variable angle tow composites positioned along optimal tow paths has shown that buckling resistance can be significantly improved by optimizing the tow path.

However, as the curvature of the tow path increases beyond a critical value, various defects can occur in the as-placed tows [8]. One of the major defects observed in the AFP placed tow is out-of-plane wrinkling of the tow. Fig. 1 shows an as-placed tow that has experienced out-of-plane wrinkling during AFP processing. Parameters affecting wrinkle defect formation during AFP are process variables such as radius of curvature of the layup path and tow properties such as prepreg tack and elastic moduli. Since the uncured epoxy within a typical thermoset tow is pressure-sensitive, studies have shown prepreg tackiness is a function of process variables including contact pressure, layup temperature and layup speed. For example, an increase in compaction pressure and temperature can result in more intimate substrate-tow contact, increasing tow tackiness. Conversely, increasing layup speed results in lower tackiness due to reduced contact time of the tow with the substrate.

1.1. Previous modeling work

Beakou et al. [9] and Matveev et al. [10] used an orthotropic plate-on-elastic-foundation model to predict tow wrinkling. Beakou et al. [9] developed an approximate theoretical solution assuming simply supported boundary conditions on three edges and a free boundary on the edge where wrinkling occurs to determine the critical buckling load and minimum steering radius. Matveev et al. [10] modified the boundary conditions of Beakou et al. [9] and derived a closed form solution to model out-of-plane wrinkling in steered tows with different radii of curvature. Neither model included the effect of compaction pressure on tackiness or the initiation and growth of damage during the placement process. Bakhshi [11] incorporated viscoelastic properties of the interface region in a plate-on-foundation model for predicting time dependent growth of wrinkles after initiation.

Bakhshi and Hojjati [12] used a finite element model along with a cohesive tow-substrate interface to predict the evolution of defects during the AFP process, with the opening mode cohesive traction-separation law (TSL) parameters determined experimentally using a probe-tack test; the authors assumed the same TSL for both the normal and in-plane shear directions. Simulations performed by the authors indicated that out-of-plane wrinkling and localized delamination of the tow (blister) would occur, though there was no quantitative comparison with experimental data. In our previous work [13], the authors’ experiments clearly showed that the Mode II TSL is significantly different from the Mode I TSL. Though several research publications have focused on modeling wrinkle formation during thermo-forming processing of thermoplastic prepregs [14–16], there are only a few focused on understanding the deformation mechanisms and subsequent occurrence of wrinkle formation during AFP placement of uncured thermoset tows. In this work, a finite element-based tow placement and adhesion model is developed to simulate the AFP process along curvilinear paths for thermoset tows. The simulations incorporate experimental results for Mode I and Mode II TSL in the cohesive interaction property of the tow-substrate interface to predict out-of-plane wrinkling during a typical AFP process. A complete description of the model, along with material properties employed in the simulations, is highlighted in Section 2. Results and discussion are shown in Section 3 and Conclusions are presented in Section 4.

2. Material and methods

IM7/8552-1 prepreg tows the authors used in a series of AFP experiments are selected for simulations. Material properties of the tows required for the simulations include (a) metrics for tackiness of the tow during processing and (b) the elastic properties of the orthotropic tow. The tackiness metrics used in the simulations are the measured traction separation laws for the tows. Details of the authors’ experimental program to determine the Mode I and Mode II TSL are described in recent published work using a rigid double cantilever beam specimen with prepreg tows bonded using conditions similar to those present during AFP placement of the tow [13].

2.1. Traction-separation relations for cohesive interaction

The tackiness of a tow when adhered to a substrate is modelled in our simulations using cohesive surface interaction of the contacting surfaces. The cohesive interaction is quantified by defining a TSL relating the relative opening/sliding of the contacting surfaces to the associated surface traction.

2.1.1. Mode I response

The Mode I TSL extracted from rigid cantilever beam experiments by the authors is shown in Fig. 2(a). The softening part of the TSL is nonlinear, indicating ductile behavior. In the finite element model for tow placement, the Mode I TSL can be mathematically represented by Eqs. (1) and (2).

\[
\sigma_n = K_n \delta_n \quad (1)
\]

\[
K_n = \begin{cases} 
K_{n0} \delta_n \leq \delta_0 \\
K_{n0}(1 - D_h), \delta_n > \delta_0 \\
0, \quad \delta_n \geq \delta_{se}
\end{cases} \quad (2)
\]

where \( K_n \) is the stiffness of the Mode I TSL which is a function of a damage variable \( (D_h) \) and the opening displacement \( \delta_n \); \( \delta_0 \) is the maximum separation up to which elastic behavior exists, with damage accumulating when separation exceeds \( \delta_0 \); and \( \delta_{se} \) is the separation at which the cohesive element is completely damaged and no longer able to...
resist tensile traction.

The TSL parameters $K_{id}$, $\delta_{0}$ and $\delta_{tc}$ have single values. The damage variable ($D_n$) is a function of the effective separation ($\delta_n - \delta_{0}$) which is valid for $\delta_n > \delta_{0}$. These parameters are directly obtained from the experimentally obtained TSL ($\sigma_n - \delta_n$). Parameters of the TSL are shown in Table 1. The damage variable is also calculated from the experimentally obtained TSL using Eq. (3).

$$D_n = 1 - \frac{\sigma_n}{\sigma_{0}} \left[ 1 + \frac{1}{\delta_{tc}} (\delta_n - \delta_{tc}) \right]^{-1}$$  \hspace{1cm} (3)

where $\sigma_{0}$ is the traction at separation of $\delta_{0}$.

In order to show the importance of modeling the complete shape of the TSL in the finite element model, the experimental load versus displacement response of the RDCB specimen is compared with finite element result incorporating TSL with (a) a linear softening part and (b) non-linear softening that matches the experimentally determined shape. As shown in Fig. 2(a), the elastic part of the TSL is the same for both models. The maximum separation in the linear softening model is estimated from the experimentally measured critical energy release rate ($\delta_{tc} = 2G_{IC}/\sigma_{0}$ where $G_{IC}$ is the Mode I critical energy release rate). For the non-linear softening model, the shape of the softening curve is defined using a tabular representation of $D_n$ as a function of effective separation ($\delta_n - \delta_{tc}$) calculated from Eq. (3). Fig. 2(b) presents load versus displacement results for the RDCB specimen as predicted by each model. As shown in Fig. 2(b), the shape of the TSL strongly affects the ability to match the global load-displacement data. As such, the shape of the TSL is important when modeling separation processes in soft adhesives with cohesive zone sizes that are oftentimes much larger than characteristic dimensions of the specimen.

### 2.1.2. Mode II response

The Mode II TSL extracted from a rigid cantilever beam experiment [13] is shown in Fig. 3(a). The Mode II TSL is represented in the FE model using Eqs. (4) and (5).

$$\sigma_t = K_t \delta_t$$ \hspace{1cm} (4)

where $K_t$ is the stiffness of the Mode II TSL, which is a function of damage variable ($D_t$) and tangential separation $\delta_t$, $\delta_{tc}$ is the separation up to which elastic behavior exists and damage accumulates when the separation exceeds $\delta_{tc}$, and $\delta_{tc}$ is the separation at which the cohesive element is completely damaged. Parameters of the TSL are shown in Table 1. The magnitude of the Mode II traction is assumed to be same in the positive and negative shear directions. The shear damage variable is calculated from the experimental TSL ($\sigma_t - \delta_t$) relationship using Eq. (6).

$$D_t = 1 - \frac{\sigma_t}{\sigma_{tc}} \left[ 1 + \frac{1}{\delta_{tc}} (\delta_t - \delta_{tc}) \right]^{-1}$$ \hspace{1cm} (5)

where $\sigma_{tc}$ is the traction at separation of $\delta_{tc}$.

Fig. 3(b) shows the FE predicted load versus displacement results incorporating the shape of the experimentally obtained Mode II TSL using the shear damage variable (Eq. (6)) expressed as a function of effective shear displacement ($\delta_t - \delta_{tc}$) in tabular form. The nonlinear softening law for Mode II TSL provides load versus displacement predictions that are in excellent agreement with experimental results.

In addition, the authors noted that when the cohesive zone size is small compared to the characteristic dimensions of the sample, the maximum traction and energy release rate are the only important parameters necessary to accurately predict the global load-displacement data. Due to the expected large size of the active CZM for the soft adhesive used in this work, the shape of the softening part of the TSL is exactly replicated in the CZM of the tow-substrate interaction to ensure accurate modeling of the separation processes. The cohesive zone size effects on global response were similarly observed in previous studies [18].

### 2.1.3. Mixed mode response

For calculating the mixed mode TSL, the pure Mode III and Mode II TSL are assumed to be the same. The TSL employed in this work to model

### Table 1. Parameters of the TSL for mode I and mode II.

<table>
<thead>
<tr>
<th>Fracture Mode</th>
<th>Elastic stiffness (N/m²)</th>
<th>Separation at damage initiation (mm)</th>
<th>Maximum separation (mm)</th>
<th>Traction at damage initiation (MPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Mode I</td>
<td>$K_{id} = 36.86$</td>
<td>$\delta_{0} = 0.0137$</td>
<td>$\delta_c = 2.833$</td>
<td>$\sigma_{0} - K_{id}\delta_{0} = 0.506$</td>
</tr>
<tr>
<td>Mode II</td>
<td>$K_{id} = 6.98$</td>
<td>$\delta_{0} = 0.0067$</td>
<td>$\delta_c = 1.253$</td>
<td>$\sigma_{0} - K_{id}\delta_{0} = 0.047$</td>
</tr>
</tbody>
</table>
mixed mode loading is expressed in Eq. (7).

\[
\begin{bmatrix}
\sigma_x \\
\sigma_y \\
\sigma_z
\end{bmatrix} =
\begin{bmatrix}
K_x & 0 & 0 \\
0 & K_y & 0 \\
0 & 0 & K_z
\end{bmatrix}
\begin{bmatrix}
\delta_x \\
\delta_y \\
\delta_z
\end{bmatrix}
\]

(7)

where \(K_x, \sigma_x\) and \(\delta_x\) are the elastic stiffness \((K_x = K_y)\), traction and separation in the second shear direction (Mode III), respectively. The coupling terms in the stiffness matrix in Eq. (7) are assumed to be zero.

### 2.1.3.1. Damage initiation

In this study, a quadratic traction criterion as expressed in Eq. (8) is employed in the FE model for predicting initiation of damage.

\[
\frac{(\sigma_x)}{(\sigma_{\text{initiation}})}^2 + \left( \frac{\tau}{(\sigma_{\text{initiation}})} \right)^2 = 1
\]

(8)

where \(\tau = \sqrt{(\sigma_x^2 + (\sigma_y)^2)}\) is the resultant shear traction and \(\sigma_{\text{initiation}}\) is nonzero only for positive values of \(\sigma_{\text{initiation}}\).

### 2.1.3.2. Damage evolution

The TSL for a mixed mode case is described using the resultant traction \((\sigma = \sqrt{(\sigma_x^2 + (\tau)^2)})\) and resultant separation \((\delta = \sqrt{(\delta_x^2 + (\delta_y)^2 + (\delta_z)^2)})\). Damage evolution for the mixed mode case is described based on the damage variable shown in Eq. (9), which is a generalization of the Mode I and Mode II formulae given in Eqs. (3) and (6).

\[
D = 1 - \frac{\sigma}{\sigma_0} \left[ 1 + \frac{1}{\delta_0} (\delta - \delta_0) \right]^{-1}
\]

(9)

where \(\sigma_0\) is the traction at initiation of damage (based on Eq. (8)) and \(\delta_0\) is the separation at initiation. The softening part is determined by assuming a mixed mode energy criterion shown in Eq. (10).

\[
\frac{G_I}{G_{IC}} + \frac{G_{II}}{G_{IIC}} + \frac{G_{III}}{G_{IIC}} = 1
\]

(10)

In Eq. (10), \(G_{IC}, G_{IIC}, G_{IIC}\) are the critical energy release rates for the local Mode I, Mode II and Mode III loading conditions \((G_{IC} = G_{IIC})\), respectively, and \(G_I, G_{II}, G_{III}\) are the local applied energy release rates at any time for the three modes, respectively. The T-S relationship satisfying Eq. (10) for different Mixed Mode I and II ratios is shown in Fig. 4.

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Fig. 3. (a) Mode II traction-separation data obtained using RDCB experiment and CZM traction-separation model response using non-linear softening law (b) Load versus displacement data from Mode II RDCB experiment and model predictions using non-linear softening law.

Fig. 4. Traction separation relationship for different mode mixity ratios satisfying the energy ratio criterion given in Eq. (10).

### 2.1.4. Rate dependency

With regard to wrinkle initiation and growth, experimental studies and preliminary simulation results indicate that wrinkle initiation occurs immediately after initial bonding under the AFP roller. Based upon recent viscoelastic measurements for the IM7/8552-1 slit tape [19], the separation process is expected to occur at much faster rate than the viscoelastic characteristic time of the material. For this reason, viscoelastic effects are not modelled in this work. However, for predicting the longer-term time-history of wrinkle growth, a fully time dependent TSL and viscoelastic modulus of the tow material should be considered.

#### 2.2. Elastic properties of tow: baseline experiments and simulation parameter values

The elastic constitutive properties of uncured IM7/8552-1 tow material is assumed to be orthotropic. Due to the extremely soft matrix material in the uncured tow and limited literature data for this material system, the authors performed a number of experiments to estimate several of the elastic properties.

The elastic property in the fiber direction, \(E_f\), is obtained by tensile experiments. The tensile test samples are shown in Fig. 5 and Fig. 6 shows the complete test setup. A Tinius Olsen 5000 uniaxial test frame with a load cell capacity of 22.2 KN (5000 pounds) is used in the experiments. The strain fields on the tow surface are measured using a
StereoDIC system. The StereoDIC system consists of two 5 MP Point Grey cameras with CMOS image sensors. The stereo camera images are synchronized with the load-displacement data acquisition system to capture both strain and load data during the deformation process. A LED light source with light intensity of 5000 lumens is used for illuminating the sample. The sample used in this experiment is a 6.35 mm wide slit tape (IM7/8552-1) with a gauge length of 165 mm. Glass fiber woven composite tabs with a thickness of 1 mm are bonded on both ends of the slit tape to allow it to be placed inside the grips of the tensile test machine without causing damages to the fibers from gripping pressure. A speckle pattern is applied on the surface using an airbrush with an average speckle size is 0.2 mm. The Stereo-DIC baseline displacement variability is $\approx 40$ nm. Three uniaxial test samples are evaluated to obtain an average stress-strain response at a displacement-controlled loading rate of 2.5 mm per minute$^3$. All three samples gave consistent results, with sample-to-sample variation less than 5%. The average stress versus strain data and a linear fit to the data are shown in Fig. 7$^4$.

In order to obtain an estimate of the modulus in the matrix direction and the corresponding Poisson’s ratio, a separate set of carefully controlled experiments were performed using a Bose ElectroForce 3200.

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$^3$ The tow modulus in the fiber direction measured from a tensile experiment is independent of loading rates for the range of strain rates experienced during the AFP process.

$^4$ Lateral compressive strains in the tensile specimens were observed to induce transverse buckling of the tow, introducing deformations that were not solely due to Poisson effects. Thus, the measured lateral strains during tensile loading were not be used to extract Poisson’s ratio.
Glass fiber woven composite tabs are attached to the sides of the specimen; the tabs are then placed within the grips of the test frame, as shown in Fig. 8. Since out-of-plane motion of the specimen is minimal due to low thickness and high compliance of the specimen, a single camera was employed with 2D DIC image analysis to analyze deformed speckle images of the tensile specimen and calculate strain. The camera used is the same type employed for the axial tensile experiments, with a Nikon 85 mm lens having a working distance of 30 mm for acquiring high magnification images. The tow surface is again speckled using an airbrush, with an average speckle size of 50 μm. The complete experimental setup for tensile testing in the transverse direction is shown in Fig. 6.

The average stress versus average strain in the matrix direction for loading rate of 25 mm per minute is shown in Fig. 9. The modulus in the matrix direction is determined by measuring slope of the initial part of the stress strain curve where strain is lower than 0.01%. Due to high stiffness of the carbon fibers, strain in the fiber direction is negligible (much lower than the measurement accuracy). Regarding the remaining elastic parameters, a computational parametric study showed the following:

- wrinkle formation mechanism is unaffected for values of Poisson’s ratio, \( \nu_1 \), in the range 0.1 \( \leq \nu_1 \leq 1 \)
- values of the in-plane and out-of-plane shear moduli, \( G_{12} \) and \( G_{13} \), in the range 500 MPa \( \leq \left( G_{12}, G_{13} \right) \leq 5000 \) MPa gave similar wrinkle formation.

Hence it is assumed in this work that \( \nu_1 = 0.24 \) and all shear moduli \( G_{12} = G_{13} = G_{23} = 5000 \) MPa. All relevant material properties and geometric dimensions of the tow model are shown in Table 2. Only the short-term response of the tow is modelled, as wrinkles initiate almost instantaneously after passage of the compaction roller.

As a final note regarding material characterization for the AFP simulations, bending stiffness of the tow has been shown to be a major parameter influencing wrinkle formation during the AFP process [20]. Due to a lack of experimental data for bending stiffness of uncured IM7/8552-1 slit tape at the process temperature of 40 °C, a series of three-point bend creep experiments were performed by the authors using a TA dynamic mechanical analysis system [19,21]. The creep-compliance master curve shown in Fig. 10 summarizes the experimental effort and provides the relevant viscoelastic response of an uncured IM7/8552-1 tow at 40 °C. A brief development describing how the DMA measurements of compliance vs time are used to ensure that the continuum shell elements used to model the tow have the same bending stiffness is presented in the Appendix.

2.2.1. AFP Simulations

Simulation of the AFP process requires that several components involved in the AFP process, including the tool, compaction roller, tow and tow guidance mechanism, are modelled. In this work, all simulations are performed using Abaqus Explicit. The complete model of the AFP process is shown in Fig. 11. As shown in Table 3, from a computational standpoint the stable simulation time increment for the AFP process is controlled by the size of the tow element. The AFP simulations in this work identified an optimum tow element size of 0.5 mm \( \times \) 0.5 mm, which is the largest element size for which wrinkle spacing and amplitude are consistently repeated. Further decreases in the size of the element significantly increase the computational time. For the optimal tow element size, \( 5.2 \times 10^3 \) iterations are required to complete each simulation. In addition to defining an optimal tow element size, mass scaling of each tow element is employed to increase the stable time increment to \( 1 \times 10^{-5} \) s without significantly affecting the computational results. Here, mass scaling of only the tow by a factor of 6.75 was employed. With the selected mass scaling, computational time for a 12 node 1.4 GHz cluster is about 4 hours which can be reduced to less than an hour by using a 32 node 2.5 GHz cluster.

In the following sections, models of the individual parts of the AFP process are described.

2.3. AFP substrate - tool

Since the authors previous experimental measurements of wrinkle shape and size using StereoDIC were performed on a flat tool where the tow was placed along different radii of curvature (\( R = 305 \) mm, 1270 mm and 2540 mm), all simulations in this study included a rigid flat panel model for the substrate where the tow layup process is performed. Fig. 11 shows the tool model along with a model of the roller and tow placement path. Since the tool is a thick steel plate, finite element discretization of the tool/substrate is performed using rigid shell elements (R3D4 elements in Abaqus). The size of the tool is 500 mm \( \times \) 500 mm and the size of each element is 2 mm \( \times \) 2 mm. Also shown in Fig. 11 is a cylindrical datum coordinate system defined on the axis of the roller at a radial distance, \( R \), from the global system, where \( R \) is equal to the required radius of the curved tow paths. All boundary conditions are defined based on this coordinate system. The translational and rotational degrees of freedom of the tool are constrained at the rigid body reference point, \( O \), of the tool. For defining the deformations and stresses on the deformed shape of the tow, a local orthonormal coordinate system (\( t, b, n \)) is defined and shown in Fig. 11. The direction ‘t’ is tangent to the tow path, ‘b’ is in the transverse or width direction of the tow and ‘n’ is normal to the surface of the tow facing the tool surface. The origin of the local coordinate axes ‘t’ and ‘b’ is located at the intersection of the mid-planes through the thickness and width direction of the tow. The material properties of the tow are defined based on local coordinates.

2.4. Compaction roller

The compaction roller in the AFP system consists of a rigid inner cylinder with a compliant material on the outer layer (usually a silicon rubber layer in an AFP system). The soft outer layer of the compaction roller maintains a more uniform pressure distribution across the tow width to reduce damage in the tow when it is placed on the substrate. The roller FE mesh along with inner cylinder and pin are shown in Fig. 12. The outer rubber material is discretized using three-dimensional, linear hexahedral elements with incompressibility mode (C3D38I element in Abaqus). The outer layer of material is modelled as an isotropic linear elastic material with Young’s modulus of 50 MPa and Poisson’s ratio of 0.45. The roller has an outer diameter of 70 mm, inner diameter of 30 mm and width of 100 mm. All degrees of freedom of the inner surface of the roller are tied to a rigid hub that is modelled using rigid shell elements. A rigid pin in the center of the roller also is modelled using rigid shell elements (see Fig. 12). Frictionless contact
interaction is defined between the hub and the pin to allow the hub to rotate freely about the pin axis. Motion of the hub along the radial coordinate direction is constrained so that there is no relative slip between the hub and the pin along the pin axis. Motion of the compaction roller is controlled by specifying boundary conditions for the rigid body reference point of the pin defined at the origin of the coordinate system shown in Fig. 11.

Simulations are performed in two steps. In the first step, a downward displacement (z direction) of 2 mm is specified for the pin, constraining all other degrees of freedom. The downward displacement is incremented from zero to 2 mm in 0.02 s along a smooth curve such that downward velocity of the roller at the end of the first step is zero with rebounding of the roller from the tool eliminated. The deformed shape of the compaction roller after the first step is shown in Fig. 13. The 2 mm downward displacement results in the same contact length (12 mm) as in the validation AFP experiment.

In the second step, the pin is rotated about the z-axis for a specified radius is calculated by requiring the total length of the path of the roller about the center of the axis to be equal for all radii of curvature (1.2 radians for R = 305 mm). The angle θ is increased from zero to 1.2 radians at a constant angular velocity which corresponds to a layup speed of 1.83 m/s.

### 2.5. Tow guidance system

Since the uncured tow is held together in the transverse direction by a weak matrix and is susceptible to buckling of the stiff carbon fibers under compressive load, the AFP head consists of a series of rollers for controlling the feed rate and slots for controlling the path of the tow. One of the roles of the guidance system is to eliminate unwanted deformation of the tow before it enters the compaction roller. Since any form of buckling of the tow during tow feeding can have a detrimental effect on the quality of the tow placement, slight pretension (19 N) is applied to the tow to prevent compressive stresses in the tow and possible buckling within the tow feeding mechanism. In this regard, the authors have observed defects such as overlaps, gaps and twist in the as-placed tow for low values of tow tension. Another potential buckling concern is shown in Fig. 14. Here, buckling of the tow immediately ahead of the roller is observed in the simulations when the tow is not properly guided ahead of the roller, resulting in deviation of the tow from the specified path that induces local wrinkles just ahead of the roller. The tow guidance system shown in Fig. 14 consists of a rigid slot for the tow passage and constrains the tow only up to the point of contact of the tow with the roller. Compressive stress at the inner radius of the tow ahead of...
the roller induces buckling of the tow, resulting in unwanted contact of the tow with the substrate before application of roller compaction pressure; early contact can lead to deviations from the as-specified path.

The photograph in Fig. 15 (a) shows the compaction roller and tow guidance system in our previous AFP experimental work [8]. The tow guidance system maintains a small clearance between the tow-guide and tool to prevent possible interference between the two. Simulation results including the modified tow guidance system are presented in Fig. 16 (a), showing no sign of buckling or unintended contact of the tow with the tool. Hence the tow-guide shown in Fig. 15(a) is shown to be effective in preventing pre-compaction buckling of the tow, while maintaining the tow path along the predefined radius. In these simulations, the FE model of the tow-guide is discretized using rigid shell elements and motion of the tow-guide is kinematically coupled to the roller pin so that the compaction roller, pin and the tow-guide moves as a rigid body 8.

In summary, the tow-guide has two parts, one representing tow guidance close to the roller and a second one for simulating tow constraint in the tow feeding mechanism. The latter consists of a rigid

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8 The authors have not modelled or included a complete description of the complex mechanism used for tow feeding, which involves multiple rollers within an actual AFP head, as their presence was not judged to be important for accurate prediction of wrinkle formation.
A slot for tow passage where the tow is constrained to translate longitudinally (fiber direction). Pretension is applied to the tow model by specifying a distributed shell edge load of 3 N/mm to the far end of the tow. The tow guide system close to the roller described previously allows the tow to be in contact with the roller, providing better representation of the real-world AFP process shown in Fig. 15 (a). Furthermore, the tow-guide constraints out-of-plane buckling of the tow towards the tool so that unintended contact with the tool is prevented. In this work, tangential contact of the tow with the roller is modelled with a penalty friction coefficient of 0.2. Possible stickiness of the tow with the roller can be included using a cohesive surface interaction criterion; this effect was not included in the AFP model considered in this work.

2.6. Prepreg tow model

The tow is modelled using quadratic shell elements with orthotropic lamina properties. The experimental program for measurement of constitutive properties of the lamina is described in Section 2.2 and the Appendix. Tow adhesion to the substrate is modelled using the cohesive contact interaction criteria available in the Abaqus framework. The constitutive behavior of the cohesive contact is fully defined using the mixed mode TSL in Section 2.1. Cohesive contact interaction is found to be more efficient than using cohesive elements for modeling tow adhesion. The sticky contact condition is invoked when any of the nodes of the tow experience contact with the tool surface.

3. Results and discussion

Simulations of the AFP process, including incorporation of the measured TSL for tow-to-tow bonding during placement, are performed for four different circular paths, with radii of curvature $R = 305$ mm, $R = 635$ mm, $R = 1270$ mm and $R = 2540$ mm. Results for all four simulations are reported in this section. For mixed mode TSL, the damage parameter $D$ is determined based on the change in slope of the traction-separation response with increasing $\delta$ [17]. It is noted that experimental data for wrinkle formation during AFP placement of the tows is available from the authors previous work for three of the selected radii of curvature [8]. Simulation predictions are compared to the experimental results for $R = 305$ mm, $R = 1270$ mm and $R = 2540$ mm, the three radii of curvature where data exists. Consistent with experimental observations, our simulations (a) show no evidence of wrinkles for radii of curvature of 1270 mm and 2540 mm and (b) predict wrinkle formation occurs for $R = 305$ mm. Simulations also predict wrinkle formation for $R = 635$ mm, but with lower frequency than for $R = 305$ mm.

Fig. 17(a) and (b) present the predicted wrinkle shape and wrinkle amplitude for $R = 305$ mm and $R = 635$ mm, respectively. Inspection of Fig. 17(a) and (b) shows that free edge conditions near the start and end of the tow path cause deviations in wrinkle characteristics; these wrinkles are not included in Fig. 17(c) and (d). The horizontal coordinate of the graphs in Fig. 17(c) and (d) is the arc length along the inner edge and

### Table 3

Summary of the element type, minimum size of the element, number of elements and stable time increment for different components of the FE model.

<table>
<thead>
<tr>
<th>Component</th>
<th>Element type</th>
<th>Minimum element size</th>
<th>Total number of elements</th>
<th>Stable time increment</th>
</tr>
</thead>
<tbody>
<tr>
<td>Tow</td>
<td>Four node doubly curved shell element with reduced integration (S4R) and hourglass control</td>
<td>$0.5 \times 0.5$ mm</td>
<td>11,128</td>
<td>$3.85 \times 10^{-8}$ s</td>
</tr>
<tr>
<td>Compaction roller rubber layer</td>
<td>Eight node linear brick element with incompatible modes (C3D8I)</td>
<td>$1.88 \times 1.93 \times 3.07$ mm</td>
<td>16,218</td>
<td>$2.96 \times 10^{-8}$ s</td>
</tr>
<tr>
<td>Tool</td>
<td>Four node 3-D bilinear rigid quadrilateral element (R3D4)</td>
<td>$2 \times 2$ mm</td>
<td>62,500</td>
<td>–</td>
</tr>
<tr>
<td>Tow guide</td>
<td>Four node 3-D bilinear rigid quadrilateral element (R3D4)</td>
<td>$0.25 \times 1$ mm</td>
<td>8646</td>
<td>–</td>
</tr>
<tr>
<td>Pin</td>
<td>Four node 3-D bilinear rigid quadrilateral element (R3D4)</td>
<td>$2 \times 2$ mm</td>
<td>2576</td>
<td>–</td>
</tr>
<tr>
<td>Hub</td>
<td>Four node 3-D bilinear rigid quadrilateral element (R3D4)</td>
<td>$2 \times 2$ mm</td>
<td>2444</td>
<td>–</td>
</tr>
</tbody>
</table>
is offset to zero at the beginning of the 5th wrinkle along the tow path. 

Fig. 17(c) directly compares the simulation and experimental measurements for wrinkle spacing and height along the inner edge of the tow for \( R = 305 \) mm. As shown in Fig. 17(c), simulations predict that wrinkles form at quite regular intervals with a consistent amplitude.

Table 4 summarizes and compares FEA predictions and experimental measurements of wrinkle spacing and amplitude for all path radii of curvature. As shown in Table 4, the simulation results for \( R = 2540 \) mm, \( 1270 \) mm and \( 305 \) mm are in very good agreement with experimental results. Table 4 confirms that the predicted mean values for amplitude and spacing/wavelength are in very good agreement with experimental measurements. Consistent with expectations, the experimental

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**Fig. 14.** Buckling of the tow ahead of the roller due to the absence of tow guidance ahead of the roller; inset showing magnified view of the tow bucking and contact of the tow with the tool before application of compaction pressure.

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**Fig. 15.** (a). Photograph of the roller showing the position of the tow guidance system ahead of the roller and (b) schematic of the tow placement process.

---

**Fig. 16.** (a). Finite element modeling of tow guidance system, Inset: magnified view of the tow ahead of the roller; for better visibility the tow, the guidance system is removed from the view and (b) magnified view of the tow guidance system close to the roller; the roller is removed from the view for better visibility of the tow guidance system and tow interaction ahead of the roller.
measurements have much larger variability in the wrinkle amplitude and wavelength. One of the reasons for larger variability in the experimental measurements is the presence of substrate surface defects, such as roughness, overlaps and gaps on the tow layup substrate, as reported in recently published work [8]. For example, the negative values in the amplitude of wrinkles seen in experimental data are due to the presence of gaps in the substrate. Substrate surface imperfections are not modelled in the present work.

The spacing and amplitude of the predicted wrinkles along the inner edge of the tow for a radius of curvature of 635 mm are shown in Fig. 17(d). Consistent with the discussion for $R = 305$ mm, four wrinkles near the start and end of the tow path are not included in Fig. 17(d). As expected, the predicted number of wrinkles per unit length is found to be less (higher wavelength) than for $R = 305$ mm. In addition, the maximum predicted amplitude of wrinkles decreases relative to the predictions for $R = 305$ mm. For the larger radii of curvature ($R = 1270$ mm and 2540 mm), no wrinkles were observed experimentally and are not predicted to occur in the simulations.

Taken together, the agreement between the experimental measurements and simulation predictions indicate that the simulation process developed in this work is an accurate model for predicting wrinkling or similar defects that occur during the AFP process. Thus, for a given tow material system (e.g. for mixed mode TSL defining the cohesive behavior of the tow), the simulation platform can be used to determine the range of path curvatures that can be accommodated without incurring wrinkle defects during the process. Furthermore, the effect of other parameters (e.g. roller composition, roller pressure, process temperature, lay-down speed) can be assessed using the AFP model and the appropriate TSL for the tow material, temperature and pressure. In addition to this important application of the author’s simulation platform for robust AFP

Table 4
Comparison of finite element and experimental results of wrinkle wavelength and amplitude for different radii of curvature.

<table>
<thead>
<tr>
<th>Radius of curvature, $R$ (mm)</th>
<th>Wrinkle wavelength, $\lambda$ (mm)</th>
<th>Wrinkle amplitude, $h$ (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Finite element</td>
<td>Experimental</td>
</tr>
<tr>
<td>305</td>
<td>14.65 ± 1.35</td>
<td>15.55 ± 7.14</td>
</tr>
<tr>
<td>635</td>
<td>26.29 ± 8.26</td>
<td>Data not available</td>
</tr>
<tr>
<td>1270</td>
<td>No wrinkles are observed</td>
<td>No wrinkles are observed</td>
</tr>
<tr>
<td>2540</td>
<td>No wrinkles are observed</td>
<td>No wrinkles are observed</td>
</tr>
</tbody>
</table>

Fig. 17. (a) Simulation predictions of wrinkle shape, wrinkle amplitude and wrinkle spacing for $R = 305$ mm, (b) $R = 635$ mm, (c) FE and experimental result of the spatial distribution of the wrinkle height along inner edge of the tow for $R = 305$ mm and (d) FE results for $R = 635$ mm. The origins for the “t” coordinate for (c) and (d) are shown in Fig. 17a and b, respectively and wrinkle height is measured from the tool surface to the bottom face ($n = d/2$) of the tow.
processing with minimal wrinkling, an equally important and interesting aspect of the simulations is to improve understanding of the fundamental mechanistic aspects that are present and govern the initiation and evolution of wrinkles in tows during placement. This aspect is the focus of the remainder of the discussion.

3.1. Steady state stresses on the tow in the wrinkle region

We begin by considering the “steady-state” longitudinal stress distribution on the top surface of a tow over a representative length (including at least three wrinkles) for the two shortest radii of curvature where wrinkles formed. The predicted stress distributions for \( R = 305 \text{ mm} \) and \( R = 635 \text{ mm} \) are shown in Fig. 18(a) and (b), respectively. The maximum tensile stresses due to out-of-plane buckling occurs at the location of each wrinkle. On both sides of the uplifted wrinkle are two clearly visible compressive regions. Thus, each wrinkle affects a finite-sized region surrounding the delaminated, uplifted tow wrinkle location.

Since physically observable wrinkling is initiated by stresses and deformations within the tow, we consider conditions that are predicted to be present at the mid-thickness of the tow along the inner edge of the tow where the maximum compressive conditions are expected to occur. Noting that conditions between wrinkles are essentially the same (see Fig. 18(a) and (b)), focus will be on the longitudinal stress between two specific wrinkles.

Fig. 19 presents the compressive longitudinal stress at mid-thickness along the inner edge\(^9\) between the 5th and 6th wrinkle for all four radii of curvature. For \( R = 2540 \text{ mm} \), the compressive stress along the length is constant at 100 MPa. As \( R \) decreases to 1270 mm, the compressive stress remains constant but has increased to 230 MPa without wrinkle formation. The rapid increase in longitudinal compression indicates that stresses are approaching values that can initiate wrinkling. For the two smaller radii of curvature, wrinkling has occurred, and the stress distributions are much different. In both cases, the minimum compressive stress of 20 MPa at mid-thickness is at the wrinkle location, with the stress rising to a maximum midway between the wrinkles. The maximum compressive stress for \( R = 635 \text{ mm} \) is 2× larger than the maximum stress for \( R = 305 \text{ mm} \). Taken together, these findings indicate that as the number of wrinkles per unit length of the tow increases (wavelength decreases), there is increased relaxation of the compressive stress along the inner edge between wrinkles due to separation of the tow in the wrinkle region altering the local boundary conditions. These results are in contrast with the nearly constant compressive stress distribution predicted for higher radii of curvature (\( R = 1270 \text{ mm} \) and 2540 mm). Precisely how these “steady-state” conditions are developed requires close investigation of the conditions within the tow and on the tow adhesion surface as the roller passes over the tow and moves away. This process is described in some detail in the next section.

3.2. Mechanisms for initiation and spacing of wrinkles

The mechanism of wrinkle formation can be studied by examining the evolution of stresses and deformations of the tow during the AFP placement process. The longitudinal stress distributions on the top surface of the tow (\( \sigma_t \) at \( n = -d/2 \)) at different times, from initiation of tow separation to complete separation\(^10\) and formation of wrinkles, are shown in Fig. 20 for the two shortest radii of curvature (\( R = 305 \text{ mm} \) and \( R = 635 \text{ mm} \)). The reference time (\( \tau \)) is based on the initiation time for the 6th wrinkle. The dotted red lines in the figure indicate the roller contact area during and immediately after initiation of the 6th wrinkle. Inspection of the compressive longitudinal stress along the inner edge of the tow immediately before initiation of the 6th wrinkle in Fig. 20 shows similar stress magnitude for \( R = 305 \text{ mm} \) and 635 mm. Line plots showing the temporal evolution of \( \sigma_t \) along the inner edge are shown as solid lines in Fig. 21(a) and (b) for \( R = 305 \text{ mm} \) and 635 mm, respectively. For both \( R = 305 \text{ mm} \) and 635 mm, the magnitude of \( \sigma_t \) along the inner edge increases as distance from 5th wrinkle increases, rapidly reaching a maximum just before initiation of the 6th wrinkle, with an elapsed time that is less than 1 ms after passage of the roller. As the wrinkle forms, compressive stresses along the inner surface rapidly decrease. Within 3 ms after wrinkle formation, the stresses are approaching the “steady-state” levels shown in Figs. 18 and 19.

Also shown in Fig. 21(a) and (b) are the evolutions of uplift displacement of the tow along the inner edge for \( R = 305 \text{ mm} \) and 635 mm, respectively. The wrinkle uplift occurs just as separation initiates at the tow-substrate surface. Additional insight regarding uplift displacement at the initiation of 6th wrinkle is provided in Fig. 19, which presents the compressive longitudinal stress at mid-thickness along the inner edge for both radii of curvature. The micro buckles are present during the placement process time before initiation of large wrinkles. Based on the stress distributions in the tow up to wrinkle initiation shown in Fig. 21(a) and (b), the micro-buckles allow partial relaxation of the compressive stresses on the inner edge, so that \( \sigma_t \) is only slightly different\(^11\) for \( R = 305 \text{ mm} \) and \( R = 635 \text{ mm} \).

Inspection of the trends in \( \sigma_t \) for \( \tau = 0 \) in Fig. 21(a) and (b) shows that \( \sigma_t \) reaches a plateau after a certain distance from the neighboring wrinkle (e.g., for \( R = 635 \text{ mm} \)). This indicates that, for the AFP tow placement process, a transition length exists beyond which the compressive stress distribution on the inner edge is constant up to the initiation of a new wrinkle. In this case, transition length is defined as the minimum distance between wrinkles for which the effect of a neighboring wrinkle does not influence the stress distribution prior to formation of the next wrinkle. The transition distance is \( \approx 14 \text{ mm} \) for the AFP process and tow material used in this study. The lower stress relaxation at the midpoint between the wrinkles for \( R = 635 \text{ mm} \) than that for \( R = 305 \text{ mm} \), as shown in Fig. 19, can be explained based on the wrinkle wavelength relative to the transition length. Since the midpoint between the 5th and 6th wrinkle for a tow path with \( R = 305 \text{ mm} \) is much closer (5 mm) to the neighboring 5th wrinkle than for \( R = 635 \text{ mm} \) (10.5 mm), \( \sigma_t \) is lower for \( R = 305 \text{ mm} \) at the midpoint. The effect of the transition length is also evident in Fig. 23, which shows the history of \( \sigma_t \) at the point of initiation of the 6th wrinkle and the mid-point between the 5th and 6th wrinkle along the inner edge.

3.3. Cohesive interface damage under compaction roller

3.3.1. Cohesive interface damage evolution under the compaction roller

The processes occurring under the roller during tow placement are fundamental to the initiation and growth of wrinkles. The surface shear forces under the roller at positions where wrinkles form for \( R = 305 \text{ mm} \) and 635 mm are shown in Fig. 24(a) and (b), respectively. The compressive side of the tow experiences high shear forces (Mode II mostly, some Mode III) during placement along curvilinear paths due to roller rotation. These upper surface forces induce damage in the tow at the tow-substrate interface. Near mid-width of the tow, the shear forces are mostly Mode III but are much smaller than the Mode II forces at the outer edges of the tow. The temporal evolution of damage, the longitudinal compressive normal stress and the opening displacement at the boundary are shown in Figs. 27 and 28, respectively. For both \( R = 305 \text{ mm} \) and 635 mm, as shown in Figs. 20 and 21, the micro-buckles allow partial relaxation of the compressive stresses on the inner edge, so that \( \sigma_t \) is only slightly different\(^11\) for \( R = 305 \text{ mm} \) and \( R = 635 \text{ mm} \).

\(^{10}\) The section of the tow shown in Fig. 20 is a fixed length of the tow for a given radius of curvature between 5th under the roller and 6th wrinkle.

\(^{11}\) The peaks in compressive stress of Fig. 21(a) and (b) occur just behind the roller. Static analysis shows that the peak is due to transverse gradients in shear traction at the roller-tow boundary contributing to corresponding increases in compressive stress up to the roller location.
point of initiation on the compression edge of the 6th wrinkle for $R = 305$ mm are shown in Fig. 25. In addition, evolution of normal traction at the tow-substrate interface is shown in Fig. 26.

To improve understanding of how wrinkles form, there are four time intervals in Fig. 25,
- Region I: Immediately before contact with the roller (between $\tau_b$ and $\tau_a$),
- Region II: Roller compaction zone (between $\tau_c$ and $\tau_b$),
- Region III: After roller compaction and before initiation of wrinkle (between $\tau_d$ and $\tau_c$),
- Region IV: Initiation and growth of the wrinkle (between $\tau_e$ and $\tau_d$).

Region I correspond to conditions immediately ahead of the roller as it approaches the location where the 6th wrinkle forms. This region incurs relative slip (shear) between the tow and substrate, and small tensile opening stresses that are less than the maximum value. These conditions result in pre-compression damage before entering the compaction zone in Region II. As shown in Figs. 25 and 26, pre-compression damage can be up to 35% of the total damage required for separation. During the early stages of the Region II compaction zone, higher compressive stresses, up to 8 MPa, are present and no additional damage is predicted during the roller compaction process.

Towards the end of region II and beginning of region III, damage continues due to higher shear forces. In this region, the compressive longitudinal stresses reach a peak and begin to decline in concert with the presence of small tensile opening stresses on the interface. The higher shear forces shown in Fig. 24(a) induce tangential Mode II and Mode III slip displacements between tow and substrate, increasing damage. During this time, the compaction pressure has dissipated, and small tensile opening stresses occur. The combined set of tensile stresses and shear result in satisfaction of the mixed mode failure criterion and set the stage for opening of the separated interface. In Region IV, the longitudinal compressive stress decreases rapidly as the interface separates and wrinkle amplitude increases.

As shown in Fig. 25, the time from initial damage as the roller approaches the location where the 6th wrinkle will form to the beginning of wrinkle uplift is less than 1.5 ms, demonstrating that the predicted evolution of a wrinkle is remarkably fast. Finally, it is noted that the maximum wrinkle amplitude occurs towards the left side of the point of initiation of the 6th wrinkle, consistent with the evolution of the constraining effect of the roller as it moves along the tow path.

As noted earlier, the distribution of longitudinal compressive stress along the inner edge of the tow reveals that a transition length exists for given tow and interface properties. In these simulations, shorter radii of curvature are predicted to have smaller wrinkle wavelength. For the smaller radii of curvature, $R = 305$ mm and 635 mm, simulations show that the resulting wrinkle wavelength is less than the transition length so that the region between the wrinkles has lower maximum residual compressive stress. Since simulations and experiments both show that for $R \geq 1270$ mm, no wrinkles are formed, these results can be used to provide an estimate for the critical radius of curvature where the wrinkle occurs.

Fig. 18. (a). Steady-state longitudinal stress map on the top of the tow surface for $R = 305$ mm and (b) $R = 635$ mm.

Fig. 19. Steady state longitudinal stress along normalized inner edge distance ($t/\lambda$) of the tow about neutral plane (for $R = 305$ mm and 635 mm the stress distribution is of the region between the peaks of 5th and 6th wrinkle).
wavelength is larger than the transition length. For this material and AFP process, \( R_{\text{transition}} \approx 1000 \text{ mm}. \)

As discussed in the previous section and shown in Figs. 25 and 26, damage growth is predicted to occur immediately ahead of the roller due to relative slip between the tow and substrate and small tensile stresses. In addition, once an element of the tow approaches the maximum longitudinal compressive stress, significant damage is also predicted to occur primarily due to slip (Mode II & III) and the presence of small tensile stresses on the interface. As shown in Figs. 24(a) and 24(b), deformation of the tow under the roller causes sliding motion in the longitudinal (t) and transverse (b) directions, with the Mode II component much larger on the edges and lower at mid-width of the tow. As the...
Fig. 22. Presence of micro buckles along inner edge between 5th and 6th wrinkles at the initiation of the 6th wrinkle for $R = 305$ mm and 635 mm the dotted line shows the region of roller contact (negative opening displacement).

Fig. 23. Evolution of longitudinal compressive stress at two points on the inner edge of the tow at mid-thickness (neutral plane), one at the location of the initiation of 6th wrinkle and other at mid-point between 5th and 6th wrinkle for $R = 305$. The dotted lines show the region of roller contact (negative opening displacement).

Fig. 24. (a) Local shear forces on top surface of tow due to traction from roller for $R = 635$ mm and (b) $R = 635$ mm.
roller moves away from the region, relatively small tensile opening stresses are predicted to occur in the interface region, setting the stage for separation of the tow from the substrate.

The discussion given above regarding damage accumulation for various local modes of loading is provided for the specific TSL measured by the authors for the temperatures and pressures applied in their previous RDCB experiments. As the tow material changes, or the temperatures and pressures vary during tow layup, the TSL will necessarily change and thus the damage introduced by each local mode of loading may change. In fact, preliminary simulation studies by the authors for different TSL have shown that softer interface material with reduced maximum traction and increased maximum separation can result in (a) tow separation due to shear and tow sliding along surface, (b) separation of tow on tension side of tow and rollover of delaminated tow region and (c) transverse wrinkling of tow that would result in fiber separation and fiber bunching in transverse direction. Details regarding these observations will be reported in a forthcoming publication.

Finally, previous work by Harvey and Cebon [22] for bitumen fracture indicated that fracture toughness and the measured nominally Mode I TSL are a function of the rate of separation for slow rates of loading. Thus, additional experiments may be needed to assess the importance of rate effects during tow placement.

4. Conclusions

A simulation platform has been developed and used successfully to model the AFP process and tow wrinkling during placement along curvilinear paths. Using measured and estimated tow material properties of IM7/8552-1 and the measured TSL for Mode I and Mode II loading, no wrinkles were predicted to form for tow paths with radii of curvature $\geq 1270$ mm. These predictions for wrinkling, including wavelength and amplitude, are in very good agreement with experimental measurements for straight paths and paths with three radii of curvature.

For a roller speed of 1.83 m/s, simulation studies focusing on the mechanics and mechanisms of wrinkle formation have elucidated
critical aspects of the process that result in initial wrinkle formation within 1.5 ms, with the wrinkle predicted to grow to full size within 2 ms. Summarizing the discussion in the previous section, the process in the adhesive zone resulting in wrinkle formation during AFP of a tow is as follows:

- roller approach, with initial damage up to 35% due to local deformations and stresses
- compaction under roller, pressures up to 8 MPa, with no predicted additional damage
- high shear forces along tow edges during latter stages of compaction, with additional 20% damage
- passage of roller inducing small tensile stresses at tow-substrate interface resulting in fully damaged region, tow-substrate separation, wrinkle formation and uplift
- wrinkle growth to full size occurring at high rate after roller passage

Given the high rate of wrinkle formation during the AFP process, viscoelastic effects are not significant, as is evident from the early stage creep master curve for the tow (Fig. 10) showing minimal creep at high rate for the process temperature, $T = 40 \degree C$.

Results from these simulations confirm that the AFP model is capable of predicting wrinkling of tows during processing. The simulation platform currently is being extended to study a variety of issues including (a) how changes in tow material properties and process parameters (e.g. placement speed, temperature, roller pressure) affect the types of damage and tow defects that can occur, (b) how the presence of gaps and overlaps on the substrate surface affect tow wrinkling, (c) the effect of multi-ply curved surfaces on tow processing and tow defect formation and (d) how the wrinkle formation process occurs when placing tows on top of recently placed tows.

Appendix A

The conventional shell element constitutive relationship is defined using orthotropic lamina properties $E_x$, $E_y$, $G_{xy}$, $G_{xz}$ and $G_{yz}$ where the Young’s modulus $E_x$, $E_y$ are obtained using tensile test in the fiber and matrix directions, respectively. In conventional shell elements, the through-thickness normal strain is assumed to vary linearly along thickness direction with zero strain at the neutral axis, with the through thickness shear strain is neglected. However, it has been observed [23] that an uncurled prepreg incurs considerable shear deformation in the resin-rich region due to orders of magnitude difference between the moduli of the fiber and matrix. Hence the bending rigidity obtained by assuming zero through thickness shear strain will over-estimates the actual bending rigidity.

Considering the 3-point bend creep compliance data shown in Fig. 10, where the compliance ($S$) is defined as the inverse of the bending modulus, $S = 1/E_{b}$, is obtained from the load and deflection data from DMA measurements using a Euler Bernoulli beam theory approximation. From the creep compliance data in Fig. 1, the short-term bending modulus, $E_{b}$ at 0.1 ms can be obtained as $E_{b} = 10^{4.5}$ MPa (31.6 GPa). As expected, the bending modulus calculated from the Euler Bernoulli beam theory shows time dependency and its instantaneous value is less than the modulus in fiber direction, $E_{1}$, obtained from a tension experiment. The low value for $E_{b}$ is due to shear deformation of the resin in bending, whereas in pure tensile load the deformation is governed by the fiber modulus. Due to the complex, through thickness deformation of the tow, the six orthotropic lamina properties are not enough to define the deformation of the tow for a general loading. In this regard, Döbrich et al. [24] developed a finite element shell element for modeling of wrinkling during forming of textile preforms by decoupling bending and membrane deformation. This allowed specifying bending rigidity independent of the membrane stiffness. However, in reality, bending and membrane deformations are nonlinearly coupled, so that the through-thickness deformation predicted by the model may deviate from the true, through-thickness behavior. The through-thickness deformation is further complicated at large strain due to intra-ply sliding and friction. From the perspective of modeling wrinkled formation, the exact, though-thickness variation of stresses is of secondary importance to the bending rigidity, $D_{b}$, justifying the use of a simplified model using conventional shell elements for the tow subjected to the condition that $D_{b}$ used in the model matches the actual bending rigidity of the tow obtained from the DMA experiments. In this paper, this condition is achieved by defining an effective thickness of the tow in the model using the following procedure.

The bending rigidity ($D_{b}$) measured from the 3-point bend experiment is,

$$D_{b} = E_{b} \cdot I$$

(A-1)
The $D_b$ in the model is written using an effective moment of inertia as,

$$D_b = E_b \cdot I_{b}\text{eff}$$  \hspace{1cm} (A-2)\]

Equating A-1 and A-2 gives the following:

$$E_x \cdot I_{x}\text{eff} = E_b \cdot I_b$$  \hspace{1cm} (A-3a)

Or,

$$E_x \cdot d_{eff}^3 = E_b \cdot d^3$$  \hspace{1cm} (A-3b)

From Eq. A-3b,

$$d_{eff} = (E_b/E_x)^{1/3}$$  \hspace{1cm} (A-4)

Using Eq. A-4 and measured values of $E_b$ = 31.6 GPa, $E_x$ = 106 GPa and $d$ = 0.16 mm, the effective thickness, $d_{eff}$ = 0.107 mm, is used as the thickness of the Abaqus shell model.

References


Nomenclature

- $E_x$: Modulus in the fiber direction obtained from tensile test
- $E_b$: Modulus in the matrix (transverse) direction obtained from tensile test
- $\nu_{xy}$: In plane Poisson’s ratio
- $G_{xy}$: In plane shear modulus
- $G_{xz}$: Out-plane shear moduli in two orthogonal directions
- $G_{yz}$: Modulus in the fiber direction obtained from 3-point bend experiment in DMA
- $S$: Compliance in the fiber direction obtained from 3-point bend experiment
- $d$: Thickness of the tow
- $\delta_{eff}$: Effective thickness of the tow in the FE model
- $b$: Width of the tow
- $A_{eff}$: Effective cross section of the tow in X direction
- $A_{eff}$: Effective cross section of the tow in X direction in the FE model ($A_{eff} = b \cdot d_{eff}$)
- $L$: Second moment of area of the tow about y axis ($I = b \cdot d_{eff}^3/12$)
- $\delta_{eff}$: Effective second moment of area of the tow about y axis in the FE model ($I_{eff} = b \cdot d_{eff}^3/12$)
- $D_b$: Flexural rigidity of the tow for out-of-plane bending about y axis ($D_b = E_b \cdot I_{eff} - E_x \cdot I_x$)