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Consolidation-Driven Defect Generation in Thick Composite Parts

Fiber waviness is one of the most significant defects that occurs in composites due to the severe knockdown in mechanical properties that it causes. This paper investigates the mechanisms for the generation of fiber path defects during processing of composites prepreg materials and proposes new predictive numerical models. A key focus of the work was on thick sections, where consolidation of the ply stack leads to out of plane ply movement. This deformation can either directly lead to fiber waviness or can cause excess fiber length in a ply that in turn leads to the formation of wrinkles. The novel predictive model, built on extensive characterization of prepregs in small-scale compaction tests, was implemented in the finite element software ABAQUS as a bespoke user-defined material. A number of industrially relevant case studies were investigated to demonstrate the formation of defects in typical component features. The validated numerical model was used to extend the understanding gained from manufacturing trials to isolate the influence of various material, geometric, and process parameters on defect formation.

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Keywords: composite manufacturing simulation, wrinkles, excess length, toughened prepreg, variable thickness

1 Introduction

The increased use of composite materials has been at the forefront of the tremendous advance in weight efficiency of commercial aircraft in the last few decades. One of the main advantages in using composite materials is that their mechanical properties can be tailored to meet the design requirements of specific applications. However, along with this benefit comes a significant manufacturing challenge. Due to their compliance, the uncured composite prepreg materials have a tendency to form defects in the course of the manufacturing process. This is particularly relevant for autoclave molding, which is commonly used for aerospace applications. In this process, components are fabricated by laying up multiple sheets (plies) of unidirectional (UD) carbon or glass fibers, which have been pre-impregnated with resin (hence the abbreviation to prepreg). The stacks of prepreg are then iteratively consolidated under vacuum pressure to eliminate voids. The final step involves curing of the part under a predefined heat and pressure program in an autoclave during which further consolidation takes place. In these processing steps, the resin undergoes a number of major changes to its physical and mechanical properties, which contribute to the change in thickness. This evolution of material state and geometrical dimensions of the plies can cause the formation of fiber path defects (often referred to as wrinkles), which are triggered by ply movement during the consolidation phase. These defects, even when their size does not exceed a few mm, can be responsible for major knockdown of the laminated composite’s mechanical properties [1–3] and thus the wrinkle formation mechanisms need to be better understood in order to reduce their occurrence in the manufacturing process. Currently, producing parts with reduced levels of fiber path defects relies heavily on the designers’ experience and trial-and-error methods. Manufacturing trials are very costly and the number of process parameters is large. Hence, predictive modeling and greater understanding of the physical phenomena involved has become of paramount importance for the industrial and scientific community.

Potter et al. [1] proposed a taxonomy of defects, which highlights the large variation in causes and the complexity of the mechanisms leading to the generation of fiber path defects in the course of manufacture. They also showed that defect formation can occur at every stage of the manufacturing process. Over the years, composite process simulations have helped the industry to save large amounts of money by reducing the number of manufacturing trials. Simulation tools for forming [4–9], resin flow/infusion [10–17], or cure simulation and residual stress prediction [18–22] are now an integrated part of the toolbox available to the composite designers. These, used together, allow accurate prediction for the occurrence of a large range of defect types.
1.1 Problem Statement. One well-known wrinkle formation mechanism, which primarily occurs in thick composite parts and remains difficult to predict, is the creation of an excess length of material in the course the consolidation of the uncured laminate. The mechanism is a combination of the effect of the thickness reduction of the part, the inability of the prepreg plies to slip with respect to another, and the material's near inextensibility in the direction of the fibers. As illustrated in Fig. 1, a consequence of the part thickness reduction upon consolidation (under the applied pressure $P$) is that the length of the part surface reduces (from $l_{\text{before}}$ to $l_{\text{after}}$), thus creating an excess length ($l = l_{\text{before}} - l_{\text{after}}$). If the composite plies are compliant enough and/or the plies ends are unconstrained and free to move with respect to another, $l$ can be accommodated through in-plane compression and/or the so-called “book-end” effect. In the case where ply movement is prevented (e.g., through constrained boundary conditions or high interply frictional forces) and where some of the plies are oriented in such a way that $l$ is applied along the (inextensible) fiber direction, the only way to accommodate the excess length is through buckling of the plies (either out-of-plane, as illustrated in Fig. 1, in-plane, or a combination of both).

In order to produce a clear experimental illustration of the phenomenon, pipe sections of radius $15 \text{ mm}$ and cured thickness $4$ and $6 \text{ mm}$ were manufactured using autoclave curing of a continuous IMA-M21 ply (this same material is used throughout the paper). A single sheet of prepreg was roll-wrapped by hand around a nylon 6 mandrel to achieve the required thickness. The samples were then debulked, bagged, and cured in autoclave following the manufacturer’s recommended cure cycle [23]. Although this is a simple case, this allowed to replicate the conditions on the $90 \text{ deg}$ bend shown in Fig. 1, with perfectly constrained ends. Figure 2(a) shows the pipe sections post cure. The pipes were sectioned, hand polished, and micrographs were produced (Figs. 2(b) and 2(c)). This shows clear evidence of extensive out-of-plane wrinkling. Where the plies in the wrinkle peak at maximum amplitude, there is also evidence of in-plane wrinkling. It also shows clear correlation between thickness reduction due to consolidation and wrinkle severity with wrinkles in the $6\text{-mm}$-thick samples being significantly more severe.

1.2 State of Knowledge. Several investigations involving the use of L-sections have added to the understanding of the mechanism of excess length generation and have shown that other phenomena can also come into play and affect the severity of consolidation-induced wrinkles. L-sections are commonly used due to their relatively simple geometry and industrial relevance. Hubert and Poursartip [24] have shown how the presence of a bleeder (i.e., a layer of nonwoven synthetic fiber material which is add on top of the laminate in order to provide a continuous air path which favors the evacuation of entrapped air and residuals out of the laminate during consolidation and cure; it also absorbs the excess resin bleeding out of the component) can affect the compressibility of the prepreg stack by changing the mechanism by which the viscous resin flows through the fiber network, which in turn controls the defects’ severity. Lightfoot et al. [25] explained how thermal expansion can also come into play by impacting the way plies move relatively to each other. Other examples include Battley et al. [26] who studied the importance of the adhesion between the plies by showing how different debulking techniques can greatly affect wrinkling patterns. Very recently, Farnand et al. [27] studied the effect of forming parameters on wrinkle severities in L-shaped laminates.

There are, however, only a few numerical tools available for the prediction of consolidation-induced wrinkles in composite parts. Aiming at producing computationally efficient design tools, Dodwell et al., came up with a range of simplified one-dimensional [28] and two-dimensional [29] numerical models for the prediction of wrinkle formation in L-shape laminates. The efficiency was achieved by treating heterogeneous layered materials as a continuum. However, some essential mechanisms controlling the deformability of prepreg sheets and stacks had to be ignored. In particular, the model ignores the viscoelastic nature of the laminate’s behavior [30]. It is also limited in its ability to consider more complex geometries or to capture in-plane wrinkling. A more general hyper-viscoelastic model for consolidation of toughened prepregs has been developed by Belnoue et al. [31] and implemented in a finite element (FE) framework. This model forms the basis of the predictive analyses presented in this paper.

1.3 Paper Aims and Overview. This contribution examines how the material properties of prepregs and their constituents influence consolidation-induced wrinkle formation throughout the manufacturing process of composite materials. Several parts with industrially relevant features were manufactured with the aim of understanding how consolidation and the resulting accumulation of excess length affects wrinkle generation. A combined experimental and modeling approach is taken and it is shown how the modeling can be used to complement the experiments to gain useful insight into the key parameters driving wrinkle formation and the sequence of events that leads to the final state of the part. In
the first section of the paper, the importance of resin flow modes on the laminate through-thickness response under compressive loading is reviewed. Particular attention is given to the recent work by the authors of the present contribution [31]. The modeling framework has already been presented and validated elsewhere [31–33] and so is only briefly summarized. In the second section, greater attention is given to L- and C-sections with a specific focus on the effects of boundary conditions. The model is used to vary some of the key material properties (i.e., resin viscosity, bending stiffness in the fiber direction, and interply friction coefficient) in order to quantify their relative influence on wrinkle severity. The final section focuses on wrinkling in a severely tapered laminate, consolidated using hard tooling. The necessity for a better control on the material variability is highlighted and the model is used to understand the sequence of events leading to the final wrinkling pattern observed in the cured part. The paper concludes with general observations, suggesting some possible routes for wrinkling mitigation.

2 The Mechanics of Toughened Prepreg Stacks Under Processing Conditions

2.1 Competition Between Squeezing and Bleeding Flow. The wrinkling in the pipe sections highlights the importance of the relationship between thickness reduction and excess length generation. Understanding the physics that controls the laminates’ thickness reduction is, therefore, of prime importance in order to build predictive capability of the phenomenon. A compliant network of fibers and viscous resin exhibits various flow and deformation mechanisms, depending on applied conditions and the nature of the fibers and resin involved. Two major factors in consolidation [34] are squeezing flow (mostly observed in consolidation of thermoplastic based systems) and bleeding flow (which is typical for low-viscosity thermoset resins). Squeeze flow systems deform as an incompressible, highly viscous fluid where the fibers and the resin move together. This is characterized by non-uniform deformation through-thickness in certain conditions [35], strong size effects (i.e., thick and narrow unidirectional tapes are more deformable in through-thickness compression than thin and wide ones). In bleeding flow mode, on the other hand, the pressure gradient causes resin flow relative to the stationary fibers. The through-thickness compaction is limited and is determined by the ratio of initial fiber volume fraction (typically 50–55% after debulking) to the maximum fiber volume fraction achievable in compression (typically 60–65%).

Modern thermoplastic prepreg systems (such as Hexcel’s IMA-M21) are reinforced with toughening agents, which improve their resistance to delamination failure, particularly under impact loading and post-impact deformation. This is achieved by introducing thermoplastic particles into the prepreg resin, which increases the complexity of the system and results in a substantial increase of the resin viscosity range (up to five-six orders of magnitude [36]) in the spectrum of temperatures used in the course of composite manufacturing. In a recent study, Nixon-Pearson et al. [37] confirmed earlier observations of Ref. [24] that the two flow modes coexist during the processing of toughened prepreg. This was shown to have some important consequences for the ability of the prepreg stacks to deform. A hybrid behavior with features typical of a thermoplastic based prepreg (e.g., the variation of the material response with the tape width and thickness) and what is usually seen for systems using thermosets (e.g., the existence of a compaction limit) was observed. There was a difference of up to 15% in through-thickness strain between specimens tested at 30 °C and those tested at 90 °C, which is representative of high and low viscosity states, respectively. Moreover, 8% difference in the through-thickness strain was observed between samples of the same initial thickness, subjected to the same cycle of pressure and temperature but with different in-plane dimensions and stacking sequence. Practically, this means that, as long as lateral spreading is unconstrained, two laminates which are originally 20 mm thick but which have different stacking sequences can have up to 1 mm difference in thickness in their consolidated state. Therefore, the layup can affect the extent of consolidation considerably and thus will also affect the severity of wrinkling.

2.2 Uncured Prepreg Model. Building on the experimental work described earlier, Belnoue et al. [31] have recently proposed a consolidation model for toughened prepreg plies under processing conditions. Only the main model assumptions and equations are summarized here, therefore, the reader is referred to the original publication where the model is described at length for further details. One of the main features of the model is its ability to account for both squeezing and bleeding flow mechanisms. This allows for accurate description of the through-thickness strains under a determined cycle of pressure, pressure rate, and temperature. The model is implemented (in FORTRAN) into a user material subroutine for the FE package ABAQUS/STANDARD.

The model uses the thermodynamical framework proposed by Limbert and Middleton [38] for hyper-viscoelastic transversely isotropic solids. Although the framework was first derived for the description of human muscles, it is well adapted for the description of uncured prepreg as both materials are highly viscous and present one strong direction of anisotropy. The general thermodynamic potential, $\psi$, is assumed to be additively decomposed into an elastic part ($\psi^e$) related to the deformation of fibers and a viscous part ($\psi^v$) controlling the flow of resin through the fiber network. The second Piola–Kirchhoff stress tensor is expressed as

$$S = S^e + S^v = 2 \left( \frac{\partial \psi^e}{\partial C} + \frac{\partial \psi^v}{\partial C} \right)$$

where $C$ is the right Cauchy–Green deformation tensor and $\dot{C}$ denotes its derivative with respect to the time.

The expression of the elastic potential follows from Bonet and Burton [39]

$$\psi^e = \frac{1}{2} \mu_T (I_1 - 3) - \mu_T \ln(J) + \frac{1}{2} \lambda (J - 1)^2$$

$$+ \left[ \frac{\mu_T}{4} + \frac{\zeta}{8} \ln(J) \right] \left[ \frac{I_4}{I_3} - 1 \right] (I_4 - 1)$$

$$+ \frac{1}{2} \left( \frac{\mu_T}{\mu_v} \right) (I_5 - 1)$$

where $I_1$ is the second-order unit tensor, $I_1 = 1 : C$, $J^2 = \det(C)$ = $I_3$, $I_4$ = $N_0 : C$, and $I_5$ = $N_0 : C^2$. The material parameters $\zeta$, $\mu_T$, and $\mu_v$ are correlated to the Young’s modulus, shear modulus, and Poisson’s ratio of the prepreg.
\( \beta, \lambda, \mu_2, \) and \( \mu_L \) in Eq. (2) can all be expressed as a function of the engineering constants. \( N_0 \) is the structural tensor which characterizes the local directional properties of the material and is defined as \( N_0 = n_0 \otimes n_0 \) (where \( n_0 \) is the fiber orientation).

The expression for \( \psi' \) is derived from the work by Rogers [35] and Kelly [40]. Based on experimental results, the existence of a transition mechanism between squeezing and bleeding flow is postulated. This transition mechanism is thought to be related to what researchers have described in the past as locking [41], which corresponds to the moment when the fiber bed reaches a configuration that is such that it cannot deform in-plane (transverse to the loading direction) anymore. It is further assumed that after locking, a change of direction of the resin flow between the fibers takes place from transverse squeezing to longitudinal bleeding. To ensure a smooth transition between the two mechanisms, squeezing flow theories were used in both cases. In other words, bleeding was mathematically represented as squeezing along the fibers, i.e., both the models were described mathematically by Stokes flow at the microscale.

In addition, as opposed to traditional flow models for thermosetting prepreg, which use Darcy’s flow [42], it was assumed that the apparent viscosity of a piece of prepreg subjected to pure compressive loading could be multiplicatively decomposed into the product of a strain rate dependent term (assumed to behave as power law fluid) and a strain dependent term [40].

To capture the deformation of prepreg with an anisotropic Stokes flow, the presented model would have to be defined at sub-parallelity and a fine mesh through thickness of the plies would have to be used. This would make the computations for full scale components very time-consuming. To overcome this difficulty, a multiscale approach was used. As a result, the strain-dependent term was further multiplicatively decomposed into a component pertaining to the macroscale deformation of the tape and a term (at the microscale) expressing the evolution the inter-fiber channels. All this development finally led to the formulation of two expressions for \( \psi' \). Prior to locking, the transverse behavior of the material was controlled by the viscous potential given in Eq. (3). After locking, \( \psi' \) was expressed as in Eq. (4). In both these equations, \( J_2 \) is the strain rate invariant defined as

\[
J_2 = \frac{1}{2} (1 + C^2)
\]

\[
\psi' = \frac{\psi_0}{k_0} \frac{4 k \sigma_0^2}{\mu_2} \left( \frac{1}{P_1^f} \right)^2 \left( \frac{k}{\sqrt{P_1^f - \frac{1}{\mu_2}}} \right)^2 + 3 \mu_L
\]

\[
\times \left( \frac{1}{2 \left( (L_1 - 1)^2 + 2 \ell_3 \right)} \right)^{\frac{1}{2} \frac{\ell_3^2}{J_2^2}}
\]

(3)

where

\[
P_1^f = \frac{1}{2} \left( (L_1 - 1)^2 - \sqrt{(L_1 - 1)^2 - 4 \ell_3} \right)
\]

\[
\psi' = \frac{d \psi_0}{d h_0} \frac{4 k \sigma_0^2}{\mu_2} \left( \frac{1}{P_1^f} \right)^2 \left( \frac{k}{\sqrt{P_1^f - \frac{1}{\mu_2}}} \right)^2 \left( \frac{1}{2 \ell_3^2} \right)^{\frac{1}{2} \frac{\ell_3^2}{J_2^2}}
\]

(4)

where \( P_1^h = L_1 - (1 + \chi_f) \).

Most of the terms in Eqs. (3) and (4) are known a priori as they describe the geometry of the tape and the unit cell. \( h_0, \psi_0, \) and \( h_0 \) are the initial tape length (i.e., along the fiber direction), width, and thickness, respectively. While \( \chi_f \) and \( \ell_3 \) are the aspect ratios of a unit cell at locking and at the compaction limit, respectively, and \( d \) is the size of the fibers in the plane perpendicular to the fiber direction. Therefore, it can describe the transverse behavior of prepreg under processing conditions at a constant temperature using just three parameters. The parameters \( a \) and \( b \) are two power law parameters controlling the behavior of the rate-dependent term of the apparent viscosity, while the parameter \( k \) controls the size of the interfiber channels at the microscale. These parameters can be easily determined by fitting straight lines through experimental data on a double log scale obtained from simple compaction tests [31].

2.3 Material Parameters’ Determination. The deformation modes that need to be captured are the material response to transverse compression, the incompressibility upon squeezing, the response of the fiber direction to in-plane compressive loading, and the prepreg bending behavior along the fiber direction (or equivalently the longitudinal shear deformation). The material response to transverse compression is captured using the procedure described earlier and the parameter values used can be found in Ref. [31]. The material incompressibility and the response of the fiber direction to in-plane compressive loading are controlled by \( I_1 \) and \( I_3 \), which are physically based invariants [43–45] controlling, respectively, the local change of volume during deformation and the stretch along the fiber direction. Finally, longitudinal shear deformation is controlled through the longitudinal shear modulus \( \mu_L \) in Eq. (2).

The bending behavior of the prepreg is particularly important for predicting the magnitude and intensity of wrinkling. The mechanical behavior of uncured prepreg in bending shows many similarities with that of dry fiber yarns [46,47]. Unlike what is seen in a cured composite, the viscous resin is not rigid enough to prevent the sliding of fibers relative to each other within the ply, and hence, the resin behavior has a strong influence on the bending response in the fiber direction. In addition, in an unloaded prepreg sheet, the fibers will naturally show some undulations, which makes the contribution of the resin to bending stiffness of a ply even stronger. Two material characteristics used in the model: Young’s modulus \( E_L \) (which controls the compressibility of the sheet under loading along the fiber direction) and \( \mu_L \) should, therefore, be in agreement with the real bending response of the prepreg. The most pragmatic way to achieve this is to derive these characteristics from a bending test on a strip of prepreg. The drop in the resin viscosity with temperature is responsible for cohesion between the fibers, which result in the variation in rigidity of the prepreg. Therefore, there is a requirement for the bending stiffness \( (D_L) \) to be measured at different temperatures. An adaptation of the cantilever test [48,49] proposed by Liang et al. [50] for thermoplastic prepreg was used. A prepreg strip was heated up to the test temperature in an oven. Two thermocouples placed on each extremity of the strip allowed to ensure a homogeneous distribution of temperature. Once the testing temperature was attained, the oven was briefly opened and a 170-mm-long section of the strip was left to deform over an edge, due to self-weight, from a horizontal surface. As viscous effects are neglected here, the strip was left to reach equilibrium for 30 min. The deflection was then optically measured and the corresponding bending stiffness derived using Peirce’s equation: 

\[
D_L = \frac{(\cos(\theta/2)/\tan(\theta))^2 \rho L}{(\rho d^3/8) \theta},
\]

where \( \theta \) is the angular deflection of the cantilever end, \( \rho \) is the mass per unit area multiplied by the acceleration due to gravity (i.e., \( 9.81\text{m} / \text{s}^2 \)), and \( d \) is the cantilever strip length. Figure 3 presents the average values (from three samples) of \( D_L \) at all the tested temperatures. The observed behavior shows a sharp reduction of \( D_L \) with increasing temperature, followed by a plateau at higher temperatures. This is very similar to the consolidation response studied in Ref. [37] and the evolution of the viscosity with temperature measured by Lukaszewicz [36]. This shows the considerable influence of the resin on the prepreg apparent behavior. A consequence of the existence of the plateau is that the error bars at the higher temperatures are very small. On the other hand, at lower temperatures as the resin viscosity sharply decreases, the magnitude of the error bars becomes fairly sizeable. Hence, for the measurement accuracy, it is absolutely key to control the temperature well on this side of the spectrum.

Beam theory is then used to derive, from the value of \( D_L \) experimentally measured, the effective Young’s modulus \( (E_L) \) that will
give the proper bending behavior of a ply. This is what is given as an input to the user material. \( E_L \) is thus expressed as \( E_L = (12 \times D_L / h^3) \) (where \( h \) is the ply thickness). Moreover, longitudinal shear modulus is \( G_L = (E_L / 2) \) (i.e., it is assumed that there is no spreading along the fiber direction). It is worth noting that cases where there is high fiber tension such that the fibers have straightened would, therefore, not be captured very well. However, it is assumed that this is not the case in the examples treated here.

In order to ensure the material incompressibility upon squeezing, a penalty method is used. A value for \( \lambda \) (in Eq. (2)), which controls the material bulk modulus, needs to be set so that it is “large enough” to enforce incompressibility while not infinitely large to avoid numerical problems. The near inextensibility condition sets another requirement on \( \lambda \) which has to be “low” in comparison to \( E_L \). A good compromise between both constraints was “large enough” to ensure incompressibility while not infinitely large to avoid numerical problems. The near inextensibility condition sets another requirement on \( \lambda \) which has to be “low” in comparison to \( E_L \). A good compromise between both constraints was

\[
\lambda = (E_L / 1000).
\]

Upon locking, \( \lambda \) is ramped down to zero allowing the fiber volume fraction to increase due to bleeding. All the other elastic constants are set to a very small value (it is best that they are nonzero to avoid numerical instabilities in the code).

An improvement of the work in Belnoue et al. [31], whereby a full cure cycle is modeled through the coupling of the consolidation model with cure simulation models, was presented in Ref. [32]. However, in this paper, the model uses an isothermal process to reduce the computational cost. Unless otherwise stated, all the models are run with the parameter values as extracted for 90 °C (see Table 1). As shown in Ref. [31], past 70 °C, the resin viscosity is so low that locking is reached very quickly. There is no evolution of the parameters \( a, b, \) and \( k \) beyond this point. In a thick laminate made from IMA-M21, it takes two and a half hours for the resin to gel [51]. This means that all of the laminate’s flow and deformation up to vitrification occurs very early in the cure cycle and that it is very likely that a cure simulation model is not required. Should this assumption be inaccurate, the model would be conservative (i.e., on vitrification in the curing composite, the whole composite architecture is “frozen” and no more deformation is possible).

### 3 Consolidation Over an External Radius

A geometry feature that is known in the industry for being prone to wrinkling defect formation is the consolidation of a laminate over an external radius. Unlike the pipe sections, wrinkles are only formed if the plies are sufficiently constrained from sliding with respect to one another (either by end condition or a high friction force resulting from the long arm span of the section) and, hence, the excess length cannot be dissipated [28]. In order to mitigate these wrinkles, there is a great need to understand what “sufficiently constrained” means in terms of geometrical and material parameters such as bending stiffness and frictional constraints.

#### 3.1 The Mechanics of Wrinkle Formation

Four manufacturing trials to demonstrate the impact of processing parameters were conducted. Two thicknesses (\( t = 4 \) mm and \( t = 6 \) mm) and two end conditions were investigated. In the first case, cork dams were used all around the laminate edges (this was referred to as the “standard” case); in the second case, the end conditions were altered by taping every fourth ply onto the tool with silicone adhesive flash tape, to restrict sliding and maximize any excess length generation (this case will be referred to as “constrained”). For all the demonstrators, 300 mm × 80 mm 0 deg unidirectional plies were laid up over a male tool with a radius of 15 mm and debulked after every four plies laid down. The L-brackets were then autoclave cured using Hexcel’s recommended cure cycle for thin IMA-M21 monolithic parts [23]. In order to ensure repeatability, two specimens were produced for each configuration. The choice behind using UD laminates fabricated without off-axis plies was to eliminate factors associated with the lay-up. It also represents a “worst case scenario” since the bulk factor (compressibility) of a laminate with unconstrained width is maximum for unidirectional lay-ups [37].

Figure 4 shows photographs of 4-mm- and 6-mm-thick L-sections for both constrained and standard end conditions. It shows clear evidence of out-of-plane wrinkling for the 6-mm-thick specimen with constrained ends. Each of the other configurations showed no clear evidence of out-of-plane wrinkling. However, a micrograph (see Fig. 5(a)) of the constrained section for \( t = 4 \) mm shows that there is also a moderate extent of localized in-plane waviness in each of the plies, revealed by pronounced ellipticity of the fiber cross section. Hence, the excess length tended to be accommodated by in-plane waviness. A very significant level of in-plane waviness in each of the plies is also observed in the case where \( t = 6 \) mm (see Fig. 5(b)). The micrographs (where no changes in the fiber angle of the 0 deg plies in the arms, and extensive in-plane waviness at the radius is observed) show that similar to Lightfoot et al. [25] observations, in-plane wrinkling is particularly pronounced at the maximum amplitude points of the out-of-plane wrinkle.

Finite element models for the four manufacturing conditions described earlier were set up in ABAQUS/STANDARD. A ply-by-ply modeling strategy was used. As in all the rest of the paper, linear, reduced integration solid elements (C3DR8) with enhanced hourglass control were used and a dynamic implicit analysis was performed. A custom written MATLAB tool allowed the automatic generation of a biased mesh, refined in the corner area (i.e., 0.5 mm × 10 mm × 0.2 mm elements) and coarser at the arms’ ends (i.e., 10 mm × 10 mm × 0.2 mm elements). This resulted in a total of 28,000 elements for the model of the 4-mm-thick sample and 41,000 elements for the model of the thicker specimen. Interply friction was modeled through a simple penalty contact with Coulomb friction. Larberg and Akermo [52] measured the friction coefficient for four different aerospace grade carbon/epoxy prepreg systems. The only difference between the T700-M21 prepreg that they tested and the IMA-M21 system used here is the fiber type. In particular, the same toughening strategy with

### Table 1 Material parameters controlling IMA-M21 prepreg viscous behavior at 90 °C, as derived in Ref. [31]

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<th>( K )</th>
<th>( a )</th>
<th>( b ) (squeezing)</th>
<th>( b ) (bleeding)</th>
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<td>0.79</td>
<td>-0.864</td>
<td>-14.58</td>
<td>-31.31</td>
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Fig. 3 Plot of the bending stiffness of an uncured ply of IMA-M21 in the fiber direction against temperature. The error bars are plot using the standard deviation.
thermoplastic particles distributed on the prepreg surface is used. Various conditions of temperature, pressure, and pressure rate were considered and the friction coefficient was consistently found to lie between $\eta = 0.15$ and $\eta = 0.2$. Unless otherwise stated, an approximate value of $\eta = 0.2$ was, therefore, prescribed to the models.

The manufacturing process was simulated in two steps. To start with, a forming phase was set up. The prepreg plies were laid flat on top of each other. The stack was formed onto the rigid L-shaped tool using two rotating plates (a video illustrating this is available at the website link). As in reality, the plies will be laid-up one at a time onto the tool, no friction between the plies and between the tool and the plies was considered. The second step consisted of the modeling of the actual autoclave cycle. Constrained ends were modeled by preventing the nodes at both ends of the laminate from moving along the tool arms. As detailed in Sec. 2.3, temperature effects were neglected on the basis that most of the deformation is likely to happen at elevated temperature, before the resin cures and that the parameters controlling the prepreg through thickness response do not change beyond 70°C. The laminate was thus only subjected to the autoclave pressure (i.e., ramp to the 7 bar in 600 s and then dwell at 7 bar), with properties representative of 90°C being used (see Table 1).

Figures 6 and 7 show the good predictive capability of the model, which was able to reproduce the out-of-plane wrinkling (and its absence in most cases) in all the samples. Figure 7, however, shows that in the 6-mm-thick samples with constrained ends, the model seems to somewhat overestimate the wrinkle localization, and hence, its severity. This is attributed to the fact that the fixed end constraints applied in the model do not perfectly replicate the experimental conditions. Even with ends taped, some ply movement can occur and some excess length can be lost. As highlighted in Fig. 6, the model was also able to capture some of the in-plane

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<td><img src="image1" alt="Image" /></td>
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<tr>
<td>$t = 6$ mm</td>
<td><img src="image3" alt="Image" /></td>
<td><img src="image4" alt="Image" /></td>
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Fig. 4 Photographs representative of each tested L-bracket configurations

Fig. 5 Micrograph showing the wrinkle of the 4 mm (a) and 6 mm (b) thick L-bracket under constrained conditions

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2https://data.bris.ac.uk/data/dataset/36wbtsbo09a72c2elgocw5qkh
wrinkling observed experimentally. This is particularly interesting in the case of the 4-mm-thick sample with constrained ends. The combined effect of the constrained ends and the thickness reduction will necessarily create excess length, which needs to be accommodated somehow. The model’s ability to predict in-plane wrinkling, with no facility for this embedded in the model beyond the material constitutive relationships, highlights its accuracy with regard to the observed physical phenomena.

The next set of case studies explores the effect of friction in constraining the prepreg from sliding and triggering the formation of wrinkles. The phenomenon is illustrated by the FE analysis of a 6-mm-thick C-section with unconstrained ends. To that purpose, the same FE mesh as in the 6-mm-thick L-shaped laminate was used but a different set of boundary conditions was assigned. In the forming phase, the nodes at one end of the laminate were constrained in such a way that ply slippage was not possible. In the step simulating autoclave curing, the same end was prescribed symmetry boundary conditions along the arm direction. The result of the analysis is presented in Fig. 8. A similar level of wrinkling as in the L-shaped laminate with constrained ends (see Fig. 7) is observed. This is due to the symmetry boundary conditions, which essentially double the length of the arm and result in a rise of the frictional force. The other repercussion of shifting the axis of symmetry is that unlike in the L-shaped sample with fully constrained ends, the main wrinkle is not formed in the middle of the corner region but at the point where the arm with a free end meets the corner region.

### 3.2 Quantification of the Influence of the Main Material Parameters on Wrinkles Severity

In a prepreg sheet, all the material parameters are linked to the resin viscosity in some way and cannot be varied independently. This is illustrated for example in Fig. 3 which shows how the bending stiffness in the fibers direction varies with temperature (hence with viscosity). It is, therefore, difficult to experimentally quantify how the different physical mechanisms involved individually affect the formation of wrinkles upon consolidation of the part. In this section, the model is used to quantify the relative influence on wrinkle severity of:

- the thickness reduction of the stack,
- the interply friction, and
- the bending stiffness of the plies.

The model of the C-section described in Sec. 3.1, which shows evidence of wrinkles without any “artificial” end constraints, is used. The material parameters are varied independently one at a time, with all the other material constants fixed to their reference value indicated earlier in the paper.

Assessing the wrinkling severity through one single number is not self-evident. Depending on the bending stiffness and the interply friction, the same amount of excess length can be accommodated in different ways. For example, if that excess length is high enough and the bending stiffness reasonably low, two or more wrinkles instead of one could be formed. Whether it is worse for the part integrity to have two wrinkles of lower angles or only one “more severe” defect is difficult to tell and may depend on the location of defect and loading of the part. Therefore, in order to give as complete picture as possible, it is chosen to characterize the wrinkling severity in two different ways: both the excess length generated through consolidation and the amplitude of the most severe wrinkle are extracted from the models.

#### 3.2.1 Resin Viscosity

The influence of the different flow modes on the ability of the prepreg stacks to deform under consolidation were reviewed in Sec. 2 of the paper. In order to study how this affects the severity of the wrinkles, the parameters \(a\) and \(b\) controlling the viscous part of the transverse response of the material (Eqs. (3) and (4)) were varied by controlling the temperature. In order to decouple as much as possible, the influence that different parameters have on one other, it was assumed that neither the bending stiffness nor the interply friction varies with temperature. A summary of the results obtained from the different analyses is presented in Fig. 9. A clear correlation between the resin viscosity and the amount of excess length generated emerges from the figure, showing that the lower the viscosity, the greater the excess length. As much as 1.5mm difference in excess length exists between the samples processed at 30°C and those processed at 90°C. A similar trend is observed for the wrinkle amplitude but instead of plateauing at the lower viscosities as expected, it slightly decreases. This can be explained by the coupling between the interply friction and the through-thickness behavior. For example, the locking strains
at 50°C and 70°C are very similar (i.e., 0.11 and 0.12, respectively). As the thicknesses are small, this results in very similar levels for the final thicknesses (hence of the excess length). However, as the material is incompressible upon squeezing, the same strain difference is also applied in the width direction (i.e., on a much larger length) and this leads to sizable (≈0.8 mm) difference of the material transverse expansion. As the applied pressure is the same, the increased transverse dimension (at 70°C compared to 50°C) is responsible for an increase of the normal force. As Coulomb friction assumes that the normal and transverse forces are proportional, this results in the rise of the frictional forces and an inclination to accommodate excess length with more wrinkles (see Sec. 3.2.2).

3.2.2 Interply Friction. The influence of the friction coefficient was also investigated. Larberg and Akermo [52] and Erland et al. [53] have shown that different but chemically similar prepreg systems can exhibit very dissimilar frictional behavior depending on the toughening strategy used (i.e., whether the thermoplastic particles are dispersed on the prepreg surface or not) and the processing conditions. Friction coefficients varying from \( \eta \approx 0 \) up to \( \eta = 0.2 \) have been measured in the past. In Sec. 3.1, it was shown that friction can act as a constraint and trigger the formation of consolidation-induced wrinkles. Here, the way the friction coefficient influences the wrinkling pattern is further studied. A slightly bigger range (i.e., \( 0 \leq \eta \leq 0.4 \)) than the one measured experimentally is explored.

The results of the analyses are presented in Fig. 10. They revealed the existence of a competition between two mechanisms:

- In the absence of frictional constraints, all the excess length generated from the laminate consolidation process is dispersed at the sample ends through the so-called book-end behavior (i.e., plies slipping with respect to one another).
- For the higher values of \( \eta \), the frictional forces act against the laminate transverse expansion, which results from squeezing flow. This reduces the laminate’s ability to compact by forcing an earlier transition to bleeding flow. As a result, less excess length is generated. This is reminiscent of the observations by Nixon-Pearson et al. [37] who have shown that the material system IMA-M21 which is known to have a higher interply friction coefficient [52] than the more widely used IM7-8552 is also less prone to squeezing flow even though the plies are thicker.

The other main learning from the data presented in Fig. 10 is that for the higher values of \( \eta \), the wrinkle amplitude decreases at an even faster rate than the excess length. The higher friction coefficients make it harder for the material to move along the tool surface. The excess length tends to accommodate in a greater number of more moderate wrinkles instead of accumulating at one single location.

3.2.3 Bending Stiffness. Finally, the influence of the plies’ bending stiffness along the fibers direction \( D_L \), which controls the behavior of the prepreg sheets under bending and uniaxial compressive loading in the fiber direction (see Sec. 2.3), is assessed. The results of the numerical analyses performed are shown in Fig. 11. The analyses ran with the lower values of the bending stiffness highlight the importance of the fiber inextensibility in the mechanism leading to wrinkle generation. If \( D_L \) is low, some of the excess length created upon consolidation is lost through compression of the plies along the fiber direction. As \( D_L \) increases, the excessive length progressively plateaus as fiber compression becomes more difficult. Meanwhile, the evolution of the wrinkle amplitude with \( D_L \) first follows the evolution of the excess length. When the excess length reaches the plateau, it becomes harder for the increasingly stiff plies to conform to the rounded shape of the radius of the tool. The excess length is thus accommodated through sharp buckles.

Fig. 9 Influence of temperature on both resin viscosity and wrinkle severity. Resin viscosity measurements refer to those presented in Ref. [36].
4 Severely Tapered Laminate

A common feature in the design of composite parts is a tapered region where the structure transitions from a thick to a thinner section. They are used to match specific stiffness and loading requirements [54] thus allowing considerable weight savings and helping to reduce material waste. Typical structures that include tapered features comprise engine fan or wind turbine blades, helicopter yoke, aircraft wings, etc. Following a similar mechanism to the corner radius (i.e., the consolidation of the plies causing a change in part thickness, and hence, the change to the geometry of the plies, resulting in the generation of excess length), tapered structures are also prone to defect formation. The aim of the present section is to illustrate how variability of the composite prepregs (i.e., prepreg manufacturers generally guarantee a cured ply thickness within $\pm 5\%$ of the nominal value), or variation in the ply placement within the tooling, may trigger wrinkling in the finished part. It also provides an opportunity to showcase the model’s ability to give insights on the mechanisms of wrinkle formation and to thus give leads for possible defect mitigation.

4.1 Samples Design and Manufacture

4.1.1 Samples Design. Section 3 illustrated the physical mechanisms leading to the generation of excess length during the consolidation phase of composite parts’ manufacture. In order to separate the layup effects, a unidirectional layup (which is not frequent in the industry) was used. The study presented here offers the opportunity to show how the same phenomenon also occurs with a different geometry and where industry standards for design and manufacturing were respected. A cross ply laminate with alternating 0 deg and 90 deg plies at the thick and thin sections was chosen. A double taper configuration (see Fig. 12) was adopted. The laminate was $300 \times 300$ mm in-plane. In the industrial cases cited earlier, the taper angles can vary between 20 deg and 35 deg. A taper angle of 26 deg was adopted to stay within this range and the tapered length, $w$, was set at 37.38 mm. The taper design, which consists in calculating the adequate ply lengths to achieve the desired angle and to separate the ply orientations through the taper thickness to maximize the part’s resistance to delamination, was performed using the procedure described in Ref. [55]. The baseline plybook was created using the manufacturer’s quoted cured ply thickness of 0.184 mm [23]. Two extra samples were also designed, with the thicker section number of plies increased by, respectively, 5% and 10%, with the ply drops along the tapered region adjusted accordingly. This results in slightly different taper angles compared to the rigid upper tool, see Table 2. This mimics the quite plausible scenario of variable prepreg thickness, misplaced plies, or out of tolerance ply lengths (causing a change in taper angle) or a misplaced...
preform. The aim here was to quantify the resulting level of tool stand-off (the occurrence of which was expected) and to explore its repercussion on wrinkle severity. The laminates’ ply counts are summarized in Table 2. As has been the case throughout the rest of this paper, the material system IMA-M21 from Hexcel was used.

4.1.2 Sample Manufacturing. In some cases, hard tooling is preferred in the aerospace industry as it allows a better surface finish and dimensional tolerances. To better study the defect formation in the present parts, a nonconventional manufacturing technique allowing good control of the pressure and tool displacements, as well as a homogeneous distribution of temperature through the part, was used. The setup (see Fig. 13(a)) consisted of aluminum male and flat tools to which heater plates were attached. These were bolted into a die-set that ran on linear bearings to ensure the top and bottom tools remained parallel. The fixture was then placed into the bed of an Instron 600DX servo-hydraulic test machine with a load cell rated for 600 kN. The pressure cycle recommended by the manufacturer was applied, and the male tool and the flat plate were heated also according to Hexcel’s recommended cure cycle for a monolithic 15–48 mm thick part made of IMA-M21 (Hexcel®, Stamford, CT, 2012). The computer-aided design of the setup is shown in Fig. 13(b)). The temperature was controlled using K-type probe thermocouples embedded in the tool. Wire thermocouples were inserted at the surface as well as halfway through the layup of the thick sections for temperature measurements of the laminate (see Fig. 12). The temperature of the tools was controlled and measured using a bespoke program created in LabVIEW. Once the laminates were cured, a small section comprising a single taper with a 40 mm length of the thin region was cut off from the main part and mounted in Prime 20 LV resin for polishing and optical microscopy. The thicknesses were measured over several points using digital Vernier callipers in both the thick section and the thin section and averaged.

4.2 The Effect of Variability on the Formation of Wrinkles. The micrographs of the samples (see Figs. 14 and 15) revealed a substantial amount of wrinkling in the two components where the ply count at $t_2$ was increased. A clear correlation between the wrinkles’ severity and the increased value of $t_2$ could be made. Perhaps more surprisingly, some wrinkling was also observed in the baseline specimen (see Fig. 14). As in the L-brackets presented in Sec. 3, some of the excess length created during the sample consolidation was accommodated through in-plane wrinkling. This form of waviness can be seen most clearly in the 0 deg plies via fluctuations in the dimensions of the ellipses in the micrographs as the fibers snake in and out of the cut section, in-plane (see Figs. 14 and 15). This confirms the observation made in Sec. 3 and in Ref. [25] that wrinkling in laminates is not a two-dimensional phenomenon, but rather a combination of both in- and out-of-plane distortions. In addition, the micrographs also allow to highlight a notable amount of distortion in the 90 deg (characteristic of squeezing flow) and some porosity (characteristic of a lack of consolidation) in the thinner section of the tapers. The specimen designed with $t_2$ being 10% over-thick compared to the baseline case exhibits wrinkles of the severity shown in Fig. 15 (i.e., ~45 deg!). This specimen was designed mostly for illustration purposes and is not representative of any real manufacturing case. However, wrinkles with as severe an angle as ~20 deg were still shown in the 5% over-thick specimen. These wrinkles observed are significantly more severe than anything that would be judged acceptable by an industrial standard. It is also significant that the baseline specimen, which was designed using the nominal cured ply thickness provided by the manufacturer such that the cured laminate should perfectly match the shape of the tool, showed a non-negligible amount of waviness (~10 deg) and that this is, by no means, prevented by the use of hard tooling.

4.3 Identification of Wrinkles Formation Mechanism. Preventing the formation of wrinkles during the manufacture of thick tapered laminates requires a better understanding of the mechanisms by which these wrinkles are formed. Similarly, to what was observed in the L-shaped laminates in Sec. 3, the thickness variation of the laminate upon consolidation is responsible for the generation of an excess length. The micrographs of the samples (see Figs. 14 and 15) also suggested that wrinkling is favored by the inability of the mold to close onto the thin section of the part. This is supported by the existence of porosity in the thin section, which suggests a lack of applied pressure. As in situ observation of the laminate consolidation and cure process is still at an early stage [56] and only allows to look at very small volumes of material, it is difficult to experimentally determine the
exact mechanisms by which wrinkles form. The manufacture of the three different specimen designs was, therefore, simulated. It was assumed that good prediction for the internal ply geometry of the cured part implies that all the events leading to it were properly captured.

Again, linear, reduced integration solid elements (C3D8R) with enhanced hourglass control were used and a dynamic implicit analysis was performed. The MATLAB tool allowed the automatic generation of a refined mesh in the tapered areas (i.e., the smallest elements in the model were 0.5 mm × 50 mm × 0.2 mm elements and the largest elements, at the specimen’s extremity, were 7.5 mm × 50 mm × 0.2 mm). This resulted in a total of 47,200 elements for the model of the baseline specimen and 50,000 and 52,100 elements for the models of the \( t_2 + 5\% \) and \( t_2 + 10\% \) specimen, respectively. As with the previous models, a two-step procedure was used. The “as laid-up” geometry was generated by bringing together the originally flat plies through a male rigid tool conforming to the shape of the unconsolidated laminate and progressively coming down until all the plies forming the thin section were in contact. The bottom ply of the taper was kept fixed and the application of a gravity load helped to maximize the contacts between the plies. In the second phase, the as laid-up geometry was compacted using a rigid tool to model the mold surfaces. The pressure cycle recommended by the manufacturer was applied. The aluminum dams around the edges of the laminate were modeled by preventing any horizontal movement of the plies sides. The vertical movement of the ply at the bottom of the taper was constrained to represent the bottom flat tool. The symmetry of the problem was used and only half of the taper was modeled. To validate the model, predictions for the final thicknesses of the taper thick and thin sections were compared to those measured on the real samples. The results are reported in Fig. 16 which show a very good agreement. Extra validation was provided by comparing the predicted internal ply geometry with the samples’ micrographs. Figure 17 provides these comparisons for the half of the thin section that was modeled. Relatively good agreement was observed but the excess length in the models seem to be accommodated through wider and less steep wrinkles than in the experiments. There are several possible explanations for this. As is consistent with other work in the field [57], the model does not take into account the viscoelastic nature of the material response to loading in the direction of the fibers [58,59]. Only the time-independent part of the response to bending of the prepreg is taken into consideration and this may result in an overly stiff behavior. Second, the model neglects the effect of the adhesion between the plies [60], which may have an impact on the shape of the wrinkles [61]. Also, once the resin has bled out of the fiber bed, it is not accounted for anymore in the model. In the real sample, the resin, which is bled out, fills up the space between the mold and the taper in the thin section and could interact with the plies influencing the wrinkles’ shape. Finally, in order to reduce the computation time, the sample symmetry was used to model only half of the taper. It is not clear how the applied symmetry boundary conditions impact the wrinkles’ shape, as in the experimental case the wrinkles were not precisely symmetrical, even though the part external geometry was.

Analysis of Figs. 16 and 17 helps to understand the mechanisms at play. The “targeted” thicknesses in Fig. 16 used the manufacturer’s nominal cured ply thickness. The good agreement (see Fig. 16(a)) between the targeted, measured, and predicted values of \( t_2 \) for the baseline case suggest that the thick section of the laminate is consolidated adequately. In contrast, the thicknesses of the thick sections of the \( t_2 + 5\% \) and \( t_2 + 10\% \) cases are below the percentage increase of the extra plies (closer to \( t_2 + 1.6\% \) and \( t_2 + 2.75\% \), respectively). This indicates a level of “over consolidation” and suggests that the thick section consolidation is higher than planned. Hence, it can be assumed that the top of the laminate and the tool were not in contact everywhere leading to an increased pressure (with respect to the 7 bar applied on the top of the tool) on the specimens. On the other hand, Fig. 16(b) shows that the measured and predicted values for \( t_1 \) are significantly higher than the targeted baseline value. As expected, the extra plies added to the specimen thick section lead to tool stand-off in the specimen thin section and higher measured values of \( t_1 \). It also suggests that in the baseline specimen, where a thicker than expected value of \( t_1 \) is also observed, the contact between the tool
and the thin section is established only at a later stage, which results in under consolidation. In this case, the predicted and measured thicknesses in the thin section are, respectively, 6% and 12.5% off the targeted values. This level of difference between the measured and predicted values may be the result of the variability of the ply thickness, which would lead to an imperfect match between the mold and the specimen. This assumption is supported by the examination of the model of the baseline case (Fig. 17) which shows that in the case where the preform and the tool match perfectly, there is no detectable ply waviness in the thin section, unlike what is observed in the micrographs of the corresponding sample.

The hypotheses made above on the wrinkle formation mechanism are confirmed by an observation of the model predictions for the sequence of events leading to the final internal ply geometry. Figure 18 shows the $t_2 + 5\%$ case as an example. At the start of the process, the uncured prepreg preform does not match the mold in the tapered region. First contact with the tool is made on the angled section of the taper. As the load increases, the part of the sample in contact with the tool is progressively forced to adopt the shape of the mold. As the edges of the specimen are constrained by the aluminum dams, the reduction of thickness of the areas in contact with the tool triggers the formation of an excess length, which is accommodated through the generation of wrinkles on both sides of the contact region. As the tool comes down further, the gaps between the tool and the flat parts of the taper are reduced and then the tool contacts the thick section. By this point, all the excess length generated has been transferred to the thin part, forced down the taper into regions of the specimen not yet in contact with the tool. Upon further consolidation of the thick section, the wrinkle progressively travels toward the center of the part and its amplitude increases until it meets the mold. As the consolidation continues, more excess length is generated, while further increase of the wrinkle amplitude is prevented by the
mold. This eventually leads the excess length to be accommodated in the form of two wrinkles instead of one. It was shown in Sec. 2 that an important aspect of the mechanics of prepreg stack consolidation is the existence of a compaction limit (see the graph of the evolution of the tool displacement with time in Fig. 18). When the thick section locks, no more deformation of the sample is possible. If the mold has not shut onto the thin section by this point, it is never able to do so. In the real specimen, the gap left between the sample and the tool will be progressively filled up with resin, which will eventually cure. The measured thickness will then appear to be much higher than the targeted value. It is interesting to note that this mechanism holds many similarities with the observations by Dangora et al. [62] when performing double compaction tests on dry fiber fabric strips.

**4.4 Possibilities for Wrinkle Mitigation.** Section 4.3 has demonstrated how the bulk factor of the laminate and the lack of pressure applied to the areas of the top surface during the processing cycle are responsible for the formation of wrinkles during the manufacture of tapered laminates under hard tooling. The importance of the applied pressure is reinforced by further analysis (see Fig. 19), which shows that if processed in an autoclave using single-sided tooling and a vacuum bag, the $t_2 + 5\%$ demonstrator would not exhibit any substantial wrinkles. The obtained shape will, however, not meet the tight tolerances used in the aerospace industry (in particular for aerodynamic components).

In order to suppress defects of low severity (i.e., such as in the baseline specimen), slight modifications of the processing cycle aiming at favoring the mold closure onto the part’s thin section may be possible. This could, for example, consist in increasing the pressure slightly or in delaying the resin cure by amending the temperature cycle. More geometrically based solutions could include slight over-fill of the thin section or adapting the top tool closure path. These solutions would help ensuring that locking is reached in the thin section. To prevent wrinkling of greater severity, more “radical” solutions would have to be invented. One path
to explore could include preventing squeezing flow (which controls the high bulk factor of the prepreg), but this would necessitate either changing the chemical composition of the prepreg or drastically modifying the processing conditions. For industrial end users, it is likely to be more practical to investigate novel tooling solutions, which allow combining the advantages of load control (soft tooling) and displacement control (hard tooling).

5 Discussion and Conclusions

In this paper, a combined experimental and modeling approach was used to elucidate the mechanisms responsible for the formation of defects formed in the course of the manufacture of fiber-reinforced polymer composites. Two geometries, inspired by real industrial cases, were investigated: the consolidation of a laminate over an external radius and a tapered laminate consolidated under hard tooling. The study showed that in both these cases, the main drivers for wrinkle formation are the laminate bulk factor, the incompressibility of prepreg material in the direction of the fibers, and the (in)ability of the prepreg sheets to slip with respect to another due to the friction coefficient or the boundary conditions applied to the laminate. Upon the combined effect of the three phenomena listed above, an excess length is created and accommodated through out-of-plane (and also in-plane) buckling of the composite plies.

The numerical tools presented have proven their ability to capture effectively the appearance of consolidation-induced wrinkles that form during the manufacturing processes of composite materials. The scheme used here is quite versatile and can be applied to a wide range of potential part geometries. It provides a good basis for defect mitigation through the optimization of the process parameters and part geometry (including the ply stacking sequence). The numerical tools presented here hold great potential to help reduce part development cost, wastage, and scrap through the early introduction of the manufacturing constraints at the design stage of the production of the parts. That said, the current ply-by-ply modeling approach is computationally expensive and is not able to give predictions for components as large as an aircraft wing for example. One of the key research challenges for the future, to take advantage of this new understanding gained here, would be to modify the current framework in order to improve its numerical efficiency. There is also a need for a more accurate representation of some of the physical aspects behind the formation of wrinkles. For example, the viscoelastic behavior of prepreg in the direction of the fibers, the adhesion between the plies and possible interaction between the laminate and the resin, which bleeds out of the system, are currently not taken into account.

The work presented here also illustrates the importance of control of the variability of both the prepreg material and preform dimensions. It was shown how a difference of only 5% in the thickness of a tapered laminate can lead to drastically different levels of wrinkle severity. A potential route to follow is a better control of the processing parameters [63] but alternative materials that are less likely to deform through squeezing flow could also help greatly.

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References
