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CONSOLIDATION-DRIVEN WRINKLING IN CARBON/EPOXY WOVEN FABRIC PREPREGS: AN EXPERIMENTAL AND NUMERICAL STUDY

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Abstract: An efficient macro-scale finite element approach is introduced to predict defects that arise from the consolidation of carbon fibre woven fabric prepregs. The model incorporates the through-thickness compaction and inter-ply shear behaviour of the plies in the form of a compliant penalty contact in an explicit finite element simulation. For describing the compressibility of uncured prepregs, a one-dimensional compaction model is implemented via a user-subroutine. The model parameters were identified from an experimental programme and validated against compaction and inter-ply shear experiments on a Carbon/Epoxy prepreg. Moreover, the in-plane and out-of-plane behaviour of the woven fabric is captured through a hybrid hypo-elastic membrane and shell modelling approach. The applicability of the model is further demonstrated through a number of industrially relevant case studies. The method is simple, efficient, and it can be applied to assess various scenarios for modelling textile forming and consolidation behaviour.

Keywords: A. Composites manufacturing simulation; B. Wrinkling; C. Excess length; D. Consolidation

1. Introduction

Over the last few decades, the number of applications using composites has increased considerably [1]. Driven by the desire for weight reduction while maintaining strength and rigidity in the aerospace and automotive sectors, structures made out of metals are gradually being replaced with lighter Carbon Fibre Reinforced Plastic (CFRP) structures [2]. Although composite materials out-perform their metallic counterparts materials in terms of mechanical properties, in their uncured state, they are often susceptible to manufacturing defects [3-5]. In general, insufficient control of critical process parameters during the early stages of the manufacturing process (i.e. pre-gelation) can lead to the formation of defects in composite parts. This includes defects such as porosity [6, 7], residual stresses [8], and dimensional changes [9]. Additionally, there exists a class of fibre-path defects such as out-of-plane wrinkling and in-plane waviness, which is very much dependent on the manufacturing process, frequently observed in the autoclave moulding process of thick composite parts [10-14]. The global and local process-induced defects arising from the soft and compliant nature of the curing thermosetting resin below the gelation point may have a significant impact on the structural performance which underlines the importance of the consolidation phase in the CFRP product development process [15, 16]. These defects often lead to interference during assembly and need to be addressed by costly repair operations or part rejection [17]. An increased

understanding of the onset, evolution, and detection of ply wrinkling will lead to cost reductions, lighter (not overly designed) parts, and increased manufacturing throughput.

Regarding ply wrinkling, attention has primarily been paid to understanding, characterising and simulating the formability and wrinkle formation in woven fabrics [18, 19]. For textile prepregs, a large body of research has been dedicated to the characterization and modelling of in-plane tensile, shear, and out-of-plane bending behaviour in the context of forming simulations [20, 21]. In contrast, consolidation-induced defects have remained relatively unexplored. With the objective of integrated product development for industrial textile composite parts, efficient simulation methods are required to design the consolidation process and ensure defect-free formability. While considerable progress has been achieved in recent years to increase the accuracy of forming simulation methods, e.g. by local meso-modelling [22] or higher-order gradient approaches [23], application-oriented methods for efficient and yet reliable consolidation simulations are comparatively rare. This is particularly crucial for applications where large and complex multi-layer laminates are manufactured. Thus high production accuracy is vital to verify the formability and predict the final part thickness.

The manufacturing process of thermoset prepregs generally involves forming a stack of prepreg onto a mould followed by room temperature or high-temperature debulk to eliminate voids and volatiles. [2] For autoclave-cured parts, the consolidation phase, during which a considerable reduction in thickness occurs, is a decisive manufacturing step that forms the final three-dimensional geometry of the part. A thermoset composite cure cycle typically involves one (or more) heat ramps followed by dwells and a cooling period. Below gelation (where the final T_g of the composite is less than the cure temperature) the resin is in a liquid state and is unable to transfer load [24]. The resin then passes through a rubbery phase, in which the mechanical properties of the laminate increase. Fig. 1 shows a typical consolidation event for a C-section laminate. During the manufacturing process, the material is required to deform to achieve the desired thickness and fibre volume fraction. The reduction in thickness dictated by the autoclave pressure requires the plies to shift from a larger initial radius to a smaller radius over the external corners [25]. This generates an excessive length which, in an ideal scenario, would form a bookend at the far end of the laminate (in the case of low inter-ply friction). However, if the plies are not able to slip over each other, wrinkles form either in-plane or out-of-plane, as shown in Fig. 1, and manifests itself as “missing length” at the ply terminations [12].

A number of studies have aimed to identify the key influencing parameters responsible for the formation of fibre-path defects in prepreg applications [10, 12]. Aside from the final part geometry, ply sequence, and intra-ply properties, it has been shown that the formation of wrinkling and in-plane waviness is mostly affected by inter-ply shear properties and compaction behaviour of the plies. This, in turn, is affected by process parameters such as temperature, forming rate, and pressure [26]. Process simulation tools that are gradually being developed will make it possible to avoid the trial-and-error design of these processes. Also, the ever-increasing complexity of the geometries into which composite materials are formed, along with the development of new manufacturing techniques, have increased the need for new numerical models capable of adequately describing the complexity of

the phenomena involved in the composites manufacturing process [27]. Intending to produce computationally efficient predictive tools, Dodwell et al. [25], proposed a one-dimensional model for the prediction of wrinkle formation in L-section laminates. The efficiency was achieved by treating heterogeneous layered materials as a continuum. However, some essential mechanisms controlling the deformability of prepreg sheets had to be disregarded. Particularly, the model ignores the viscoelastic nature of the laminate's behaviour. A more general hyper-viscoelastic model for the consolidation of toughened prepreps has been recently developed by Belnoue et al. [28] and implemented in a finite element (FE) framework. This phenomenological model considers the existence of a transition mechanism between squeezing and bleeding flow. The model has been applied to various part geometries [14] and yielded good prediction for the parts' final thickness and internal ply geometry at the meso-scale. Recent developments of the model have led to very efficient solutions and decreased the run time by one order of magnitude in comparison to the original ply by ply formulation of the model [29], although at the expense of neglecting ply separation [30]. As will be elaborated in Section 2, this model forms the basis of the predictive analyses presented in this paper.

Depending on laminate details and processing parameters, the manufacturing of parts with tight radius corners may result in imperfections and defects such as localized wrinkling. This work investigates the formation of manufacturing-related defects during the consolidation of corner sections in carbon-fibre epoxy fabric laminates and further clarifies the influence of critical process parameters on defect formation phenomena. The work brings contributions to both numerical and experimental aspects of fabric prepreg processing. Namely, the consolidation [28] and inter-ply shear [31] models recently presented by the authors are assembled into an efficient modelling approach that for the first time, captures the viscoelastic compressibility of textile composite plies at their interfaces (see Section 2.3). The consolidation model initially proposed by Belnoue et al. [28], which has only been verified for UD prepreps to date, is extended to fabric prepreps in the present work. Moreover, the viscous compressibility of the prepreg material (though thickness) is implemented into the contact between adjacent plies via a compliant penalty contact (and not as part of a material model). This approach enables the use of shell elements compared to expensive solid elements, while still being capable of reaching the relatively same level of complexity achieved via, e.g. a UMAT subroutine with a lower computational cost. The inter-ply shear model for ply-to-ply interaction is based on mixed lubrication principals. The model is presented in length elsewhere [31]; however, here it is incorporated and linked to the compaction model within the VUINTERACTION subroutine and is being used for consolidation simulations for the first time. Overall, the validated interface model makes it possible to take into consideration the geometric and material parameters, as well as a range of processing conditions. A series of experiments on L-section and C-section laminates is presented to (a) validate the modelling approach to increase the confidence in the model to be used in more complex forming/consolidation scenarios; and (b) shed light on mechanisms involved in wrinkling of *fabric prepreps*, which have been the subject of less attention compared to their UD counterparts in the literature.

The modelling framework is presented in Section 2, describing the structure of the Abaqus/explicit VUMAT and VUINTERACTION subroutines which allow for complete integration of user-defined material and ply/ply

interaction behaviours. A summary of compaction and inter-ply shear models implemented is also provided in this section. Attention is given to the interface behaviour and the proposed compliant penalty approach. The experimental procedures for compaction experiments (which are used to calibrate the consolidation model in Section 2.1) on fabric prepreg and manufacture of L-section and C-section specimens are presented in Section 3. Finally, in Section 4, the experimental and numerical results are discussed, focusing on the analysis of the numerically challenging case of consolidation-induced wrinkling defects formed in corners. In doing so, the significance of the role of bulk factor, inter-ply shear, part geometry, and boundary conditions is explored. Lastly, in order to demonstrate the capability of the model, forming and consolidation of a generic complex geometry is presented. The modelling framework shows it can be used to provide an informed choice in design for the manufacture of composite structures.

2. Modelling Framework

Fabric prepreps are heterogeneous materials. In that respect, the numerical model needs to consider the deformation behaviours at the smaller scale as several structural levels from yarns to preform and eventually to the laminate [32]. In each level, many geometrical and mechanical variables control or influence the final fabric behaviour [33]. There are various simulation methods for modelling a forming process according to the current state-of-the-art [34]. These methods differ substantially in the way they consider the process limiting conditions, the detail level of the calculation results, and the running times. Fig. 2 illustrates the typical mechanical behaviours expected from a fabric prepreg when subjected to a given consolidation event. First-order deformations induced during the consolidation of fabric prepreps are mainly dependent on the change in path length of the ply and how much excess length is generated. The excess length accommodated is dictated by the geometric features of the tool and the change in thickness (bulk factor) of the lay-up. Simulating this phenomenon, therefore, requires the inclusion of the through-thickness compliance of the material into a model, as well as the other characteristic material behaviours typically included in forming simulations, such as inter-ply shear, in-plane tensile stiffness, in-plane shear stiffness, and out of plane bending stiffness. Fig. 3 shows the composition of the proposed macro-scale approach. The numerical model consists of two main subroutines accounting for five deformation modes, namely, a VUINTERACTION subroutine which incorporates the compaction and frictional behaviours represented at the interface, and a VUMAT subroutine responsible for in-plane and out-of-plane deformation of the plies. The models are briefly summarized separately before their output is presented in Section 4.

2.1. 1D compaction model for uncured fabric prepreps

Building on the experimental study described in [35], Belnoue et al. [28] proposed a consolidation model for toughened prepreg systems under processing conditions. The model has been verified for UD materials in the past [14] and is expanded to fabric prepreps in the current study. The main model assumptions and equations are summarized herein, while the reader is referred to the original article for further details. One of the essential characteristics of the model is its ability to account for both squeezing and bleeding flow mechanisms. This allows for an accurate description of the through-thickness strains under a given cycle of temperature, pressure, and pressure rate. It is hypothesised in the model that both flow mechanisms are present simultaneously since existing

squeezing and bleeding flow models cannot be readily combined due to the incompatible assumptions with regard to compressibility, fibre movement, and resin flow. A criterion for the transition from shear flow to resin bleeding is proposed. It is also assumed that after the transition, a change of direction of the resin flow between the fibres occurs, from transverse squeezing to bleeding along the fibres. The effective response is presented in the form of an analytical solution for squeezing flow of Newtonian fluid under no-slip boundary conditions. The prepreg's through-thickness behaviour is modelled as a viscous flow with an apparent viscosity (η_{app}), which is multiplicatively decomposed into strain and strain rate-dependent (η_{rate}) terms. The strain-dependent term of the apparent viscosity is thus itself multiplicatively decomposed into a micro-scale term (η_{micro}) (which purely treats the fibre/resin interaction within a unit cell) and a ply scale term (η_{ply}) (describing the evolution of the overall ply deformation) yielding to Eq. 1:

$$\sigma_N = \eta_{app}(\epsilon, \dot{\epsilon}) \dot{\epsilon} = \eta_{micro}(\epsilon) \eta_{ply}(\epsilon) \eta_{rate}(\dot{\epsilon}) \dot{\epsilon} \quad (1)$$

Where σ_N , ϵ and $\dot{\epsilon}$ are the Cauchy stress, strain and strain rate in through-thickness direction respectively. The large deformation Hencky measure of strain was adopted such that $\epsilon = \ln \left(\frac{h}{h_0} \right)$, and $\dot{\epsilon} = \dot{h}/h$, where h_0 and h are the initial thickness and instantaneous thickness of the fabric ply, respectively. The formulation allows to derive the right thickness for a given temperature and pressure cycle and to estimate a transition between squeezing (incompressible) and bleeding (compressible) flows which allows estimating the volume fraction.

It is postulated that a critical shear strain exists at the flow front at which the squeezing flow appears to be blocked. Accordingly, before and after the transition, the material response is described by conceptually equivalent equations. The model then only needs the input of three parameters (a , b , and k) that can be determined relatively easily from compaction tests. To have the capability to capture potential shear thinning or shear thickening effects, the strain-rate dependent term η_{rate} is assumed to follow a simple power-law function [36] as:

$$\eta_{rate} = e^{\bar{b}} (-\dot{\epsilon})^a \quad (2)$$

where a and $\bar{b}$ are material parameters that need to be determined from compaction experiments. These parameters define the equivalent viscosity of the resin present in the system. Parameters a can be seen as a flow behaviour index (dimensionless) and $e^{\bar{b}}$ as a flow consistency index (SI unit: Pa s^a) in a power-law fluid. Experimental observations suggest the existence of temperature, pressure, and geometry dependent transition mechanism from squeezing to bleeding flow. It is assumed that when the applied strain reaches a particular value (ϵ^l) the flow mechanism changes from squeezing to bleeding. This marks the point at which the fibre bed reaches a configuration that is such that it cannot deform transverse to the fibres and transverse to the loading direction. As squeezing flow theories match better the assumption of the multiplicative superposition of a strain and a strain rate dependent term, bleeding is described as squeezing flow along the fibres instead of the classical Darcy's law often used to describe percolation flow in uncured composites materials [6].

The expression for ε^l can be obtained expressing the maximum shear rate at the ply edge ($\dot{\gamma}_{max}$) and the maximum through-thickness strain rate at the bulk material ($\dot{\varepsilon}_{max}$) using Roger's analytical solution for the squeezing of an incompressible Newtonian media [37] and by subsequently integrating over time the obtained relations giving rise to:

$$\varepsilon^l = \max \left\{ -\ln \left(\sqrt{\frac{\frac{2}{3}h_0}{w_0} \tan(\gamma_{max}) + 1}, \frac{2}{3} \varepsilon_{max} \right) \right\} \quad (3)$$

Where h_0 and w_0 are the height and width of the ply before compaction, respectively, and the maximum shear at the ply edge (γ_{max}) can be determined from geometrical considerations (See [28]). The terms η_{micro} and η_{ply} in Eq. (1) are both determined from rearranging Roger's analytical solution [37]. Before locking, η_{ply} is expressed considering the squeezing flow of an incompressible (i.e. the Hencky strain in the transverse direction is the opposite of the strain in the compaction direction) ply of dimensions h_0 and w_0 as described in Eq. 4a; after locking, the ply is assumed to be fully compressible (i.e. no transverse expansion) which gives rise to Eq. (4b)

$$\eta_{ply \text{ pre-locking}} = 2 \exp(-4\varepsilon) \cdot \left(\frac{w_0}{h_0}\right)^2 \quad (4a)$$

$$\eta_{ply \text{ post-locking}} = 2 \exp(-2(\varepsilon + \varepsilon^l)) \cdot \left(\frac{w_0}{h_0}\right)^2 \quad (4b)$$

The term $\frac{w_0}{h_0}$ introduces the dependence on width but also the influence of fibre orientation. Hence, if two adjacent plies have the same orientation, then this term uses 2* the ply thickness as h_0 . It is worth mentioning that generalizing the model to more complex geometries is possible as shown in [14] where the 3D solid implementation of the compaction model used here was utilized to model the consolidation of reasonably complex tapered laminates. Nevertheless, the preprocessing phase is more complex as different material properties need to be defined within a same ply depending on the nature of neighbouring plies at different locations. This could be done relatively simply using an in-house Matlab preprocessor [14].

Following the considerations discussed at length in [28], an idealisation of the micro-scale geometry allows expressing η_{micro} by considering the squeezing of a Newtonian fluid between two idealized square fibres. The same idealisation of the micro-scale geometry is made to derive the expression of when bleeding occurs. However, this time, the squeezing flow is assumed to occur along the fibre direction. The expression for prior and after locking is given as:

$$\eta_{micro} = 2 \cdot \sqrt{\chi_l} \cdot \exp(\varepsilon) \cdot k \cdot \left(\left(\frac{k}{\sqrt{\chi_f} \exp(\varepsilon) - k} \right)^2 + 3 \right) \quad (5)$$

Where k is defined as the root square of the fibre volume fraction (V_f) at the compaction limit (i.e. where the fibre bed reached its maximum limit and no further deformation occurs). χ_l and χ_f are the aspect ratios of a unit cell at locking and on the compaction limit, respectively. χ_l is such that $\chi_l = \exp(-2\varepsilon^l)$ and in order to reduce the number of parameters needed by the model, χ_f is taken as $\chi_f = 1.2 \chi_l$ (This is in agreement with the fact that the fibre volume fraction of uncured fabric prepreg considered here is approximately 45% and reaches to ~55% after complete consolidation). Most of the variables introduced here, such as the fabric dimensions or the fibres' size are constants known in advance. The only parameters that are material constants and need to be determined from experiments are the rate term parameters defined in Eq. (2) and the parameter k from Eq. (5). As elaborated in [28], for each of the temperatures investigated in the experimental program described in Section 3.2, a and b can be determined by varying the value of k until all the experimental points converge to two well determined straight lines in a log-log diagram. The model was implemented into a user material subroutine VUINTERACTION for the FE package ABAQUS/Explicit, as discussed in Section 2.3.

2.2. Mixed lubrication model for inter-ply shear behaviour

At very early stages in the cure cycle, the shear and Young's moduli of the resin are extremely low [38], allowing a certain amount of ply movement to occur. Plies can, therefore, move relative to each other and the tool surface. Both processes will result in the formation of frictional shear stresses between the surfaces in question. Lubrication theories can be employed to study the effects of friction in composites processing, as the matrix at certain temperatures and strain rates can act as a lubricant between prepreg layers [39, 40]. The Stribeck curve charts an overall view of friction variation in the entire range of lubrication [41]. As illustrated in Fig. 4a, the curve provides a graphical representation of the friction coefficient, μ as a function of the Hersey number H , a non-dimensional quantity that is dependent on the dynamic viscosity of the lubricant film η , velocity v , and the Normal force N . The curve can be categorized into three regions based on the level of contact between the plies or surfaces of interest, namely, boundary lubrication region in which Coulomb friction is dominated, hydrodynamic lubrication, where shear deformation of the fluid film is dominant, and mixed lubrication which is a combination of the two modes. The onset of fibre entanglement is the state where the frictional response falls into the mixed lubrication zone [39]. As shown in Fig. 4a and described in extensive detail in [42], the examination of resin coverage on the surface shows a clear transition from hydrodynamic to boundary lubrication with the majority of friction coefficients falling in the mixed lubrication region. As depicted in Fig. 4b, the extent of fibre-fibre interaction may vary depending upon the thickness of the resin layer, h , highlighting the significance of film thickness prediction which can be considered a *dynamic* phenomenon, given the sliding movement of plies over each other. In other words, the key factor distinguishing the lubrication regimes is the resin film thickness relative to the surface roughness.

Rashidi et al. [31] have recently proposed a mixed lubrication model capable of handling the transition between boundary lubrication, mixed lubrication, and full hydrodynamic state covering the entire range of a material processing window, solely based on the rheological properties of the matrix constituent, surface roughness, and the fibre-bed response. One of the advantages of the model is that the film thickness can be predicted from the normal

pressure and velocity. A combination of the inverse hydrodynamic lubrication (IHL) theory and Greenwood–Williamson (G-W) model is adopted to solve the axisymmetric, stationary (i.e. only the coefficient of friction at yield is considered at the time being), and isothermal IHL problem. Only a brief description will be provided in this section, while more information can be found in [31]. In the present study, the proposed model is coded into the user subroutine VUINTERACTION to be utilised in consolidation simulations.

As shown in Fig. 4b, the load sharing in the interface of contact can be addressed by coupling the fibre bed compaction response and a fluid lubrication model. The contribution of frictional forces on the dry and lubricated regions is calculated by superposition of the coulomb frictional force between the intersecting filaments and the shear force of the resin film:

$$F_{Total} = F_{Fiber} + F_{Resin} \quad (6)$$

Where the Coulomb friction is governed according to Eq. (7) in which τ_{Fiber} is the shear stress, μ is the coefficient of friction, and N_{Fiber} is the normal force to the friction interface:

$$\tau_{Fiber} = \mu N_{Fiber} \quad (7)$$

For hydrodynamic friction, as given by Eq. (8), shear stress is governed by the shear stress of the resin film, τ_{Resin} where η is the viscosity of the fluid, h is the thickness of the fluid film, and U is the velocity of one surface over the other.

$$\tau_{Resin} = \int_{-L}^L \eta U / h + h/2 \frac{\partial p_{Resin}}{\partial x} \quad (8)$$

Summing both fibre and fluid parts, the total force can be calculated:

$$F_{Total} = \mu_{Fiber} N_{Fiber} + \iint_{A_{Resin}} \tau_{Resin} dA_{Resin} \quad (9)$$

In order to solve Eq. (9), N_{Fiber} and resin film thickness, h is required. During the compaction, the composite part is compressed through the thickness, leading to multiple fibre-to-fibre contacts when consolidation forces are applied during processing. According to Terzaghi's law, at any point in the laminate, the normal stress is given by the following expression [43]:

$$P_{Applied} = \bar{\sigma} + P_{Resin} \quad (10)$$

Where $P_{Applied}$ is the total applied stress, P_{Resin} is the resin pressure, and $\bar{\sigma}$ is the fibre bed effective stress. The resin flow through the channels of the plies can be described by Darcy's law using the permeability parameter, which establishes the relationship between the flow rate and the pressure gradient necessary to drive the flow. Assuming the composite material as a void-free fibre bed fully saturated with a thermoset resin, the effective stress model implemented in COMPRO® [44] can be applied to extract the hydrostatic resin pore pressure between the layers, assuming a uniform pressure distribution of the resin underneath the filaments for the time being. The film

thickness is then obtained using the thin lubrication theory approach employing a 1D steady-state Reynold's equation [45]. For a laminar and transient flow of an incompressible, isothermal and viscous lubricant, the Reynolds equation can be written as:

$$\frac{\partial}{\partial x} \left(\frac{h^3}{\eta} \cdot \frac{\partial p_{Resin}}{\partial x} \right) = 6U \frac{\partial h}{\partial x} \quad (11)$$

Where h is the film thickness, p_{Resin} the resin pressure, η the dynamic viscosity, and U the velocity in sliding direction. Since, in most cases, no analytical solutions for any of these equations exist, a numerical solution is necessary. The inverse hydrodynamic lubrication theory is used to solve the Reynolds equation. Since the resin pressure distribution is known, the Reynolds equation can be converted to a cubic equation, and the film thickness can be solved easily by the Newton method [46]. The details of film thickness calculation is provided in Appendix A. The breakdown of a lubricating film in the mixed lubrication regime must be associated with contact between the filaments on the two plies. The Greenwood-Williamson classical contact model was used to calculate the asperity contact stress between the filaments. The GW model assumes that all the asperities are purely elastically deformed. When the rigid plane compresses the height of asperities from z to d , the equivalent contact stress can be obtained as Eq. (12) according to the Hertz contact theory and assuming a Gaussian distribution for the roughness height of mating surfaces.

$$p_{Fiber} = \frac{4}{3} \frac{E'}{1 - \nu^2} \bar{\sigma}^{3/2} \int_d^\infty \Psi(z)(z - d)^{3/2} dz \quad (12)$$

Where $\bar{\sigma}$ is surface roughness, E' represents the reduced elastic modulus of the plies, ν is the Poisson's ratio, and $\Psi(z)$ is the probability density function. Determining the homogenized film thickness using Eqs. (10-11), and dry contact pressure from Eq. (12), the friction force, F_{Total} can be found by superimposing the friction force at the interacting asperities and the shear force of the hydrodynamic component (Eq. (6)). It is worth noting the dry contact pressure (effective fibre bed stress) must be equal to the sum of the film pressure and the contact pressure of the asperities under resin lubrication conditions. An iterative procedure is used to adjust the film thickness by reducing the difference between the total dry fibre bed stress ($\bar{\sigma}_{Fiber\ bed}$), the sum of the asperities' contact stress (p_{Fiber}), and resin pressure (p_{resin}), i.e. $\Delta p = 0$. The functional relationship between tangential stress, normal pressure, and relative slipping displacement was used to calculate yield tangential stress while the effect of temperature was accounted for by changing the resin viscosity in Eq. (8). The stress was then introduced into the user subroutine VUINTERACTION.

2.3. VUINTERACTION subroutine: Interface modelling

The main novelty of the interface modelling approach in the present work is the use of the 1-D prepreg consolidation model presented in Section 2.1 in the form of compliant penalty contact between shell elements, as depicted schematically in Fig. 5. Previously trialled by Thompson et al. [47] describing the pressure-overclosure relationship of dry woven fabrics derived from compaction experiments, this method allows for the through-thickness compliance of the material to be captured through the interface (i.e. inter-penetration of the shell elements), rather than the material behaviour, which in turn decouples the compaction behaviour from in-plane properties and eliminates the need to use continuum elements to account for thickness reduction of individual plies.

This is particularly important when simulating the consolidation behaviour of very thick parts which would result in long running times if 3D continuum elements were used. In the present study, the compliant penalty approach established for dry fabrics [47] is extended to account for the viscous behaviour of the fabric prepreg under compressive loading (Section 2.1) within a user-defined VUINTERACTION subroutine rather than native contact controls in Abaqus/Explicit. The subroutine also indirectly accounts for the nesting between the plies as the model parameters were derived from the stack compaction with several repeats. In addition, through this subroutine, the inter-ply shear model (Section 2.2) will be coupled with the compaction model, allowing to predict the tangential shear stress which is dependant to the instantaneous normal stress at every timestep.

The contact problem in the frame of the FEM is briefly described to highlight the context of the implementation of interfacial laws, governing the inter-ply interface, for consolidation simulation purpose. This section summarizes relations that are necessary to formulate the geometrical contact conditions. The normal gap (g_n) and the tangential gap (g_t) are the two main geometrical parameters of interface contact in the mechanical calculation. Concerning the normal gap, the impenetrability condition can be formulated as follows:

$$g_n = (x^s - \bar{x}^m) \cdot \bar{n} \leq 0 \quad (13)$$

Moreover, the penetration function (g_N) can be formulated in the following form:

$$g_n = \begin{cases} 0 & ; g_n < 0 \text{ No contact} \\ d_p = (x^s - \bar{x}^m) \cdot \bar{n} & ; g_n \geq 0 \text{ Contact} \end{cases} \quad (14)$$

Where $\bar{n}$ denotes the shortest distance of a point x^s to the master surface (Ω^m), as illustrated in Fig. 5 and d_p is the inter-ply penetration based on which the penalty stiffness, ε_N is enforced. A modified implementation of the penalty method is adopted during the implementation of interfacial laws proposed in the present paper, through the user interface VUINTERACTION in ABAQUS/Explicit. The subroutine involves the deactivation of the existing contact laws used by default in the ABAQUS/Explicit code. Therefore, the system of equations governing the problem of contact will be defined in this user interface. A finite quantity of penetration (g_n) (see Fig. 5), is prescribed to generate a contact force and therefore a contact pressure in the normal direction. The contact stiffness is defined as the contact pressure between two surfaces as a function of the overclosure. The overclosure of the two surfaces is the depth of the penetration. By manipulating this relationship, to capture the nonlinear through-thickness stiffness of the textile material, through-thickness compliance can be included in the forming simulations as interpenetration between adjacent layers. For this purpose, it is necessary to calculate the resulting normal and tangential contact stresses on the basis of the relative displacements in the contact for every time increment. The resulting normal and tangential contact stress σ_N is calculated under compression stress, according to Eqs. (1) and (6) for the nodes within the contact margin that passed the local contact check condition ($g_n \geq 0$). There is no limit in the contact formulation on the magnitude of contact pressure, which can be transmitted between the surfaces. Using the penalty method that defines the normal contact pressure, the frictional stress (τ_f) at the contact interface can be defined. The tangential stress depends strongly on the used friction law described in Section 2.2.

Fig. 6 shows the implementation algorithm of contact via the user interface VUINTERACTION in the ABAQUS/Explicit code. Firstly, parameter values (constants) of the programmed laws in VUINTERACTION are introduced via the input file. Subsequently, a loop on the slave nodes is launched, where for each node, the mechanical quantities (i.e. normal and tangential stresses, and sliding speed) are calculated provided the contact is engaged. These quantities are then transmitted via the arguments of the subroutine to the code for the following calculations and storage (post-processing).

2.4. VUMAT Subroutine: Fabric In-plane and out-of-plane behaviour

The mechanical behaviour of the fabric prepreg is complex due to the intricate interactions of the yarns and fibres. In principle, the modelling of the intra-ply behaviour of fabrics is different from common continua such as metals. Textile fabrics are fibrous and non-continuous material. Therefore, the bending stiffness cannot be readily deduced from in-plane properties [48]. As shown in Fig. 1, the tensile stiffness in yarn direction is much higher than the others, and shear response is highly nonlinear. Moreover, bending rigidity varies depending on the direction of the yarn. In order to account for both the in-plane nonlinear anisotropic behaviour and bending rigidity, the material behaviour can be described by a parallel connection of membrane elements and shell elements such that the tensile and shear behaviours are decoupled from the bending behaviour (See Fig. 3). The membrane behaviour was described via a co-rotating, non-orthogonal coordinate system by a constitutive equation. As such, a hypo-elastic material model proposed by Khan et al. [49] was used to ensure the directions of the high stiffness fibres are tracked during the simulation and to implement the nonlinear in-plane shear stiffness of the material. As shown in Fig. 3, both shell and membrane elements were used to represent the material implemented into a VUMAT subroutine and were superimposed with mutually constrained nodes. The membrane elements were assigned with the in-plane material properties (tensile stiffness of fabric fibre directions and the nonlinear shear stiffness) while shell elements were assigned with only the flexural modulus of the material deduced from cantilever bending experiments at elevated temperatures [50].

3. Experimental procedures

3.1. Test material

The material investigated was an aerospace-grade T300/CYCOM 970 fabric prepreg system developed by Cytec ® with a nominal cured ply thickness of 0.204 mm. The thermoset resin is a controlled-flow 179 °C curing epoxy resin formulated for autoclave processing. Some physical characteristics of the material are provided in Table 1. The rheology of the CYCOM 970 resin system studied previously [42] was used to ensure that all the experiments were conducted within the range of temperatures and time-scale, where no considerable change of the degree-of-cure occurs. It has been shown [51] that in an isothermal test, a considerable change in viscosity of the resin only occurs at 115°C after much longer time scales (above 40 minutes) than those the specimens were kept at between the hot platens in the compaction experiments performed in this study (i.e. less than 21 mins for the longest test performed).

3.2. Compaction experiments

Following the study conducted on toughened unidirectional prepregs [35], an experimental study was designed to understand the compaction behaviour of uncured fabric prepregs under processing conditions and determine the parameters a , b , and k for the compaction model presented in Section 2.1. As illustrated in Fig. 7, the material testing involved a series of compaction tests conducted on 16-ply cruciform shaped specimens with a length and width of 50 mm and 30 mm, respectively. The effective pressurised area (region of interest) of samples was, therefore, limited to 30 mm x 30 mm which corresponds to a nominal pressure of 0.26 MPa under 240N of compaction force (see Fig. 8b). As shown in [35], this covers the range where changes in thickness are most substantial. The unconstrained edges on the specimen enable to explore the behaviour of uncured prepreg situated near the edges of a laminate or generally, in areas where manufacturing defects tend to form when substantial movement occurs. To this end, the ply edges were left unconstrained, allowing them to be used to build a material model compatible with arbitrary boundary conditions. The cruciform specimen design with extended plies was designed to allow the material that spreads laterally to remain in contact with plies above and below it and thus be pressurised. The unconstrained edges allow for arbitrary boundary conditions in the compaction model [28]. Specimen lay-up was undertaken in standard clean room conditions following standard lay-up procedures. The thicknesses for laid-up specimens were ~4.2 mm. Prior to testing, prepreg sheets were removed from the freezer and left for 30 minutes at room temperature. A 40-minute long debulk cycle under vacuum was applied to compact the specimens with 4-ply intervals into a single 16-ply thick specimen.

As shown in Fig. 8a, the experimental testing was carried out using an Instron 5969 universal testing machine with temperature controlled hot platens transferring the temperature and pressure profile to the specimens. Temperatures ranging from 30 °C to 110 °C were considered. All the tests were performed within the range of temperatures and time scales where the degree of cure does not evolve (maximum temperature of 110 °C, and test duration did not exceed 21 min). A force-controlled ramp-dwell loading condition [28] was programmed due to the requirement for model calibration and the need for understanding the material behaviour under time-dependent conditions (i.e. visco-elasticity). Hence, specimens were loaded in a ramp-dwell regime where the fast application of load is followed by long creep intervals (Fig. 8b). Specimens were loaded to 240 N in 1200 s, and the ramp-dwell program included five steps, with load increments of 40 N, starting with 80 N at the first step. The ramp-dwell technique allows the parameters extracted to remain valid in a wide range of loading rates/pressures and therefore cover the whole range observed in the manufacturing process from autoclave processing (slow loading rates at high temperatures) to Automated Fibre Placement (AFP) or forming (high loading rates at lower temperatures).

A thin release film was inserted between the loading plates and the specimens. This film minimises friction between the plate/tool surface and the specimen. Since the temperature set in hot platens may differ from the actual temperature on the sample, a K-type thermocouple (attached directly to the edge of the sample) was used to measure the surface temperature of the prepreg and ensure the applied temperature was known in the area where strains and stresses were measured. Both the thermocouple and the PID controller monitor showed that the temperature stabilizes between 2 to 5 minutes, after which the tests were initiated. To maintain the consistency of the results, a small pre-

load (~ 2 N) was applied to all the samples. After testing, the specimens were soft-cured while the platens remained at the final compacted position to prevent the samples from “springing-back”. The samples were then mounted in Prime20LV resin and polished for microscopic examination and X-Ray micro-CT. Final thicknesses were also measured at three different positions of the sample using a Vernier Caliper, and the average value was calculated and recorded. Five replications were tested at each temperature.

3.3. Manufacture of L-section and C-section brackets

Five manufacturing trials to evaluate the performance of the modelling approach were conducted. Two 18-ply, two 24-ply L-section lay-ups, and one 24-ply C-section lay-up ($t = 4$ mm and $t = 5.56$ mm). For the 24-ply laminates, two forms of end boundary conditions were investigated. In the first case, cork dams were used all around the laminate edges (this will be referred to as the “Free-end” case); in the second case, the end conditions were altered by taping every single ply onto the tool with silicone adhesive flash tape, to restrict slippage and maximize any excess length generation (this case will be referred to as “Constrained”). The samples are hereafter referred to as {shape | number of plies | end boundary condition (C or F)} e.g., L|24|F. No constraint condition was applied to the C-section trials, relying on the fact that the longer arm length would “naturally” result in the generation of wrinkles by preventing the excess length from being dissipated at the end of the laminate. As depicted in Fig. 9a, for L-section and C-section trials, $200\text{ mm} \times 50\text{ mm}$ and $300\text{ mm} \times 50\text{ mm}$ {0/90} plies were laid up (by hand) over male tools with a radii of 15 mm, 10 mm and 7.5 mm, respectively. The laminates with free ends were laid-up with a 15 mm shorter flange on one side to investigate the effect of arm length on wrinkle formation. The laminates were subject to room temperate debulk after every four plies laid down. The specimens were then cured in an autoclave following Cytec's recommended cure cycle for CYCOM970. Two specimens were fabricated for each configuration to ensure repeatability. The choice behind manufacturing laminates with only {0/90} plies was to represent a worst-case scenario since the bulk factor of a laminate is maximum due to the plies' higher bending stiffness in the {0} direction. This results in the formation of more bulk in the corner as a perfect contact between plies is unlikely to be achieved, with the bending stiffness of the ply creating small separations between plies around the radii. Release film was used between the first ply and tooling for all laminates (see Fig. 9b). Once the parts were cured, sections from the corner and flanges' ends were cut off and mounted in Prime 20 LV resin for polishing and optical microscopy. The thicknesses were measured over several points using digital Vernier calliper at the mid-section of all samples and averaged.

4. Results and Discussions

4.1. Compaction behaviour of the fabric prepreg

Fig. 10a shows the compaction response of the cruciform samples by normalizing the average instantaneous thickness of the specimens with respect to the initial thickness. A majority of samples follow a similar trend at each tested temperature. In general, each sequential step generates a smaller displacement increment both on the loading and dwell/creep stages. The largest proportion of through-thickness deformation occurred during the early stage of the test (at the first load step - Fig. 10a). This is postulated as being due to the elastic behaviour of

fibre bed and reduction of yarn undulations due to sudden compression [52]. Fig. 10b depicts the final thicknesses normalised with respect to the average of the specimens' initial thicknesses at different temperatures. For a given maximum load and loading regime, the compaction deformations gradually increase with temperature up to $\sim 70^{\circ}\text{C}$. Beyond this temperature, however, the thickness-pressure curves start to converge, and slight thickness reduction is observed. This convergence suggests fully compacted and bled out preform under the prescribed loading conditions (See also Fig. 11). This thickness level will be referred herein, as the compaction limit (i.e. the thickness at which further compaction is trivial and only possible at loads much greater than available in conventional manufacturing). It is worth mentioning that un-equal nesting of the yarns resulted from the lay-up procedure (See Fig. 11a) within the samples may prevent convergence of all samples to a fairly obvious compaction limit.

In addition to the changes in thickness, it was particularly of interest to investigate the tendency of plies to spread laterally to interpret the compaction response, as this may suggest a competition between the squeezing and bleeding mechanisms discussed in [28, 53]. The widths of specimens were obtained by X-ray micro CT as the ply widths can be measured during the post-processing of the scans (See Fig. 11a). The evolution of the normalised ply spreading at three different testing temperatures is illustrated in Fig. 11b. Although some ply widening was observed due to flattening of yarns and thus reduction in the crimp amplitude [54], the samples exhibited minimal transverse expansion which implies the dominance of bleeding flow mechanism compared to squeeze flow. This can be attributed to the interlocking of tows during compaction which 'locks' lateral spreading and therefore gives further validation to the non-existence of squeezing flow for woven pre-pregs, confirming the resin bleed-out in the direction of fibres as the main flow mechanism at play.

Additionally, the microstructure of the prepregs experiences a significant transformation throughout compaction. The examination of the cross-section of cured samples through the mid-sections shown in Fig. 12a and b provides further insight into the deformation mechanisms. A summary of micrographs of specimens subjected to ramp-dwell compaction is given in Fig. 12c-h. Several features in these micrographs are worth highlighting. Specimens tested at higher temperatures showed both reduced void size and reduced amount of low fibre density areas. The degree of nesting, judged by the adjacency of ply interfaces, was higher at elevated temperatures. The resin tends to flow predominantly along the fibre direction via channels in between the fibre tows and out via the interfaces between plies. The bleeding flow mechanism was observed upon approaching a compaction limit where the pressure gradient begins to cause resin flow independently of the fibre bed. Traces of transverse bleeding were noticed in samples at 90°C (not shown in the figure) and 110°C , evidenced by displacement of fibres at the edges (Fig. 12f). Traces of leaked resin were also seen on the edges of the samples loaded at higher temperatures, as shown in Fig. 12f). Extensive inter and intra-ply voids, along with regions of low fibre density were observed mostly at lower temperatures (Fig. 12c-e). Similar to the observations in [35], the intermittent debulk was shown to be relatively ineffective in the suppression of the voids prior to compaction experiments given the extent of voids and resin pockets after compaction at 30°C . Moreover, compaction at low temperatures (30°C) did not change the characteristic dimensions of specimens (note that in the actual manufacturing process the ply is compacted under vacuum which assists in eliminating voids. The analysis presented here is purely to characterise the sample

deformation). It is worth noting that the model presented in Section 2.1 does not directly account for the voids in the compaction response. Overall, the results revealed a strong dependency of the compaction response and its convergence limit to temperature and viscosity for thermoset fabric prepregs. These flow and deformation mechanisms throughout the consolidation phase are required to be captured for accurate and meaningful wrinkling simulation purposes. The experimental observations in this section will be described with the model discussed in Section 2.1 to account for thickness changes, compaction limit, and strain rate dependency of plies with arbitrary thicknesses in a ply-by-ply finite element analysis.

4.2. Verification of VUINTERACTION subroutine

To verify the validity of the VUINTERACTION subroutine, Finite Element (FE) models of the compaction and inter-ply shear tests were built in Abaqus/Explicit (see Fig. 13). The model parameters for the plies' viscous through-thickness behaviour were extracted from compaction experiments reported in Section 4.1 automatically using an in-house Python code and set as per Table 2 (The procedure for derivation of model parameters is described in [28]). Mechanical properties of the prepreg were obtained from tensile, bias extension, and high-temperature cantilever tests as per Table 3. Therefore, in-plane and out-of-plane behaviours were accounted for in the FE model for membrane and shell elements, respectively. The numerical simulations were performed representing the composite plies using superimposed linear, reduced integration shell (S4R), and membrane elements (M3R). It was assumed that tests were performed at isothermal conditions at 110 °C. The ply dimensions and initial fibre directions in each ply were also inputted as material parameters. As shown in Fig. 13, the gap between the neutral axes of shell elements was set to 0.136045 mm which corresponds to half the thickness of T300/CYCOM970 prepreg after debulk, t_{ply} , while the other half was represented as the thickness of the shell elements. The two heated platens between which the samples were compacted were modelled as rigid bodies. The bottom plate was fully fixed, and the movement of the top plate was controlled through the application of a single point load at the reference point of the rigid body. The same ramp-dwell force-controlled program presented in Fig. 8 was applied to all the simulations. As illustrated in Fig. 14, where a graph of the predicted displacement with time is presented, the compliant penalty approach is able to reproduce the main features of the compaction behaviour of uncured fabric prepreg under the processing conditions used in the experimental program. Particularly, it is capable of capturing the existence of a compaction limit that varies with the sample geometry and testing temperature, as well as the final thickness.

To assess the inter-ply shear behaviour, the inter-ply shear model presented in Section 2.2 was also modelled in ABAQUS/Explicit. Details of the model parameters are presented in Table 4. The VUINTERACTION subroutine was applied using the setup presented in Fig. 13b and compared with the experimental results. The inter-ply shear finite element model consisted of a top platen, bottom platen, and two fabric samples. The Aluminium plates that were used to heat and press the pull-out specimen are represented by two rigid plates. One of these plates is fixed, and the other one (mobile block) can be translated in the y-direction to apply pressure on the sample. The warp and weft directions for the plies were parallel to the sides of the plate, and the compaction pressure is applied to the mobile block. The fabric pull-out specimen was meshed using square shell elements. The sample mounted on the mobile block is 23 mm x 23 mm, while the fixed ply is 30 mm x 40 mm to preserve a constant area while the

fabric is being pulled out. The contact area is, therefore, 23 mm x 23 mm. The user-defined contact was applied between the two plies while both plies were tied to the rigid plates to mimic the adherence in the experiments. The mobile ply was defined as the slave surface, and the larger fixed ply was considered to be the master surface. The simulation consisted of two steps; first, a normal load was applied to the top mobile block as an equivalent pressure to that used in the experiment. Once the prescribed pressure was maintained in the contact area, in the second and final step, the pressure and temperature were held constant, and an instantaneous, constant velocity was prescribed in the x-direction on the nodes located on the edge of the mobile block. The predicted yield shear force and pressure profile over the effective contact area are visualised in Fig 14b. A good homogeneity was observed except at the edges where the pressure is slightly higher, which was also observed in the actual experiments (Note that the deformation of mobile and fixed blocks mentioned in [55] was minimised in the designed setup; see [42] for more details). The overestimation of yield values especially at higher pressures may be attributed to an intensified intermingling of fibres at unflooded (dry) regions where the assumption of Gaussian distribution of summits at points of contact may not be valid. Nevertheless, the simulation results fall within the scatter range of the experimental measurements reported in [42].

4.3. Wrinkling case studies

Fig. 15 shows a summary of cross-sectional images taken from all manufactured lay-up configurations. Visual inspection of the wrinkle formation and size showed almost similar trends from trial iterations, indicating a reproducible test method, particularly for the constrained conditions. The wrinkles were quantified according to the descriptors shown in Fig. 1b and compared with the FE results. Namely, the amplitude (A), angle (β), width (λ), location (λ), and the number of wrinkled plies are reported in Table 5. Characteristics and size of wrinkles were shown to depend strongly on the manufacturing process parameters (i.e. tool geometry, tool radius, number of plies, or bulk factor). Given the considerable bulk in each ply from yarn undulation and voids, substantial thickness reduction occurred, which produced excess fibre-path, mostly in the region of the corner radius, as shown in Figs. 16 and 17. The 18-ply lay-up showed minimal out-of-plane defects, while both 24-ply cases exhibited severe wrinkling in the corner region. The results further show that restricted ends generate more prominent wrinkles to the extent that in the L-section samples the excess length manifested itself as an expanded through-thickness wrinkle, creating a resin-rich zone between the first ply and the tool (see Fig. 17b). Interestingly, different wrinkling mechanisms were observed at the edge and centre of the specimens where the magnitude and location of wrinkles vary through the transverse plane. This highlights the importance of pressure distribution across the vacuum bag, as also studied by other researchers [13]. Possible sources of the so-called mechanisms are described in Appendix B. In addition, at the radii of the C-section and L-section parts, partial fibre waviness can be observed in the majority of plies through the laminate thickness, as demonstrated by micrographs in Figs. 16 and 17, indicating bending-induced compression (either due to bending of the plies in hand-layup or during debulk. The instability caused by compressive forces on the tows is typically accompanied by a combination of in-plane fibre waviness, and out-of-plane wrinkling [12]. Besides in-plane waviness, in the case of woven fabrics, there exists a competition between yarn undulation and wrinkling over the course of consolidation. As also noted by Weber et al. [12], not all pressure forces on the fibres

result in local ply wrinkling, and a global increase in undulation likely occurs in the fibre bed. This can be seen in the L|18 samples (Fig 17 c-d). Comparing the L-section samples with radii of 15 mm and 10 mm, this phenomenon appears to be more severe in the former while in the latter, the discrete out-of-plane wrinkle is dominant. The increased undulation (larger amplitudes) developed due to the compaction deformation in the convex radii may suggest a longer path along the corner would reduce the chance of discrete ply wrinkles.

To evaluate the predictive capability of the proposed approach, finite element models for the manufacturing scenarios described in Section 3.3 were built in Abaqus/Explicit. A ply-by-ply modelling approach and width of all the models was set to 5 mm. As in the rest of the paper, a dynamic explicit analysis was performed using superimposed shell and membrane elements. The process was simulated on the “as-laid up” laminate which marks the beginning of the autoclave cycle. A custom MATLAB code was written to automatically generate the superimposed mesh along with element connectivity and material property assignments, refined in the corner area (discretized with $0.2 \text{ mm} \times 5 \text{ mm}$ elements) and coarser at the arms' and flanges' ends (i.e., $0.5 \text{ mm} \times 5 \text{ mm}$ elements). This yielded in a total of 10,400 elements for the model of the L|18|C&F and 12,900 elements for the L|24|C&F and C|24|C&F specimens, respectively (a symmetry boundary condition was applied along the centre of the web for the C-section case. It should be noted that this approach may not generally be applicable to any symmetrical shape, however, in the case of C-shaped profiles, wrinkles mainly occur in the single-curved regions, and hence, the assumption of symmetrical boundary condition holds true.). Furthermore, the formed laminate across the profile of the tool had a thickness of 6.53 mm (24 plies), and 4.90 mm (18 plies) with the thickness of each ply being 0.272 mm after room-temperature debulk. Constrained ends for the L-section case were modelled by preventing the nodes at both ends of the layup from moving along the tool surface. The tools were modelled as analytical rigid bodies. The vacuum bag was essential for the analysed process as it was the sole source of applying the consolidation action. As demonstrated by Thompson et al. [47], considering the distribution of the pressure applied by the vacuum bag significantly improves the prediction of wrinkle shape and magnitude. Depending on the configuration of the vacuum bag and its imposed boundary conditions, excess or insufficient bagging material may lead to wrinkle formation or thinning in the radius region, respectively. The bag was modelled as a linear elastic material using membrane elements. A no-slip boundary condition was applied to both ends of the vacuum bag to mimic the actual consolidation process and avoid bag slippage relative to the tool surface. Temperature effects were neglected for the compaction behaviour on the basis that most of the deformation is likely to occur at elevated temperatures, before the resin reaches the gelation stage and that the parameters controlling the prepreg through-thickness response do not change significantly beyond 70°C . The laminate was subjected to the autoclave pressure ramped to 7 bar in 600 s followed by a dwell at 7 bar, with interface properties representative of 110°C being used (see Table 2).

Figs. 16 and 17 show the deformed models for C-section and L-section samples at the mid-sections, respectively, where the simulation outputs are compared to those measured on the micrographs of the real specimens. The density was scaled by a factor of 100 to reduce run-time in the explicit analysis. Dynamic effects were monitored to ensure that this mass-scaling did not affect the results (i.e. the ratio of kinetic energy to internal

energy was within 10% range). Total simulation time for L|18|C&F and L|24|C&F runs were ~ 3.5 h and 4.5h, respectively (15 cores on a dual 2.20 GHz CPU). The model appears to capture the mechanism behind consolidation-induced wrinkles well, and the deformed geometry shows a similar trend to what was observed in the experiments. For the L|24|F specimen, the dependency of frictional forces to the contact length resulted in asymmetric wrinkle formation in the corner closer to the joint due to shorter flange length on one end and accumulated frictional forces on the longer flange. The predicted wrinkles for the L|24|C&F (Fig. 17a and b) overestimate the localization of the wrinkle. Two reasons for this effect can be postulated. Firstly, the fixed end constraints applied in the model do not perfectly replicate the experimental conditions. Even with taped ends, some ply movement may still occur, and some excess length can be lost. Secondly, the degree of in-plane misalignment is higher in the case of the constrained laminate, implying some of the excess length is dissipated through this mechanism at the corners. Besides, recalling L|24|F in Fig. 15a, the predicted wrinkles are more similar to the images taken at the edges of the samples. This may be attributed to the non-uniform pressure distribution over the laminate surface which according to previous studies [13], can be far from hydrostatic due to variability caused by the bagging process and tool geometry (see Appendix B). This calls for more investigation on the effect of common bagging defects such as wrinkling and bridging and the necessity of accounting for their influence in numerical simulations. Nevertheless, as shown in Fig.18, the compressibility captured through interpenetrations between layers yielded good prediction for the laminates' final thickness as well as the compaction limit observed in the compaction experiments. This study further reinforces that full FE approaches are useful in identifying the possibility of fibre-path defect formation within reasonably short simulation times. These findings may also assist with further enhancing analytical or semi-empirical models [56] for prediction of corner thickness deviation.

4.3.1 Complex geometry numerical demonstrator

To demonstrate the performance of the proposed modelling framework and provide a case study where closed-form solutions fail to capture the complexities involved, a stepwise numerical study is presented in this section. As shown in Fig. 19, a truncated pyramid-shape forming tool was chosen, which includes features representative of geometries for aerospace applications. The converging male features accompanied by sharp chamfers (ranging from 30° to 60°) around the tool would prohibit the deformation to “wash out” and therefore, increasing the chance of wrinkle formation in those areas. Similar to the numerical case studies constructed for C-section and L-section laminates, a custom MATLAB code was written to generate the flat 18-ply lay-up. The simulation consists of two steps. Namely, drape forming of the stack, and subsequently, consolidation step mimicking the conditions inside the autoclave. The consolidation simulation was performed with the same compaction and inter-ply shear parameters used in Section 4.2. A friction coefficient of 0.28 was used between the plies during the forming stage, and the stack was compacted at 0.1 MPa (1 bar) and consequently consolidated under 0.7 MPa of pressure (7 bars).

The simulation results are shown in Fig. 20a-f. The maximum shear angle at the sharp corner of the part was found to be ~ 26 degrees, as depicted in Fig.20a. Despite this low shear deformation, wrinkles were formed as the excessive length was trapped in the corner at both forming (Fig. 20c), and consolidation stages (Fig. 20c d). A number of defects can be observed from the final deformation contour shown in Fig. 20b. A cross-sectional view of

the part is shown in both figures to compare the evolution of defects during the manufacturing process. As the ply is formed to the shape of the forming tool, wrinkles appear nearby areas where sudden transitions occur. Interestingly, the high pressure from consolidation suppresses some of these wrinkles as the plies undergo compaction (Zone III). Conversely, a number of forming-induced wrinkles are amplified through consolidation and new wrinkles form. This is also followed by the formation of consolidation driven defects in the transition zones (Zone I) as well as sheared areas around edges of the plies (Zone II). Figs 20 e and f also show the gradient plots of the thickness after forming and consolidation steps, respectively. It can be observed that the bridging in the female transition zones between the tool and the base plate is reduced in the consolidation stage as the material is more compliant at higher temperatures. Fig. 21 shows the bridging effect more closely in the female corners of the tool. Since the $\{0/90\}$ orientation has fibres orientated along the radii of the tool, they possess a higher bending rigidity in the material to resist out-of-plane buckling. Yet without the assistance of a rigid male tool or pressure intensifiers, the plies fail to fully conform the shape of the tool, and hence a resin-rich region would form in those areas [57]. These areas are referred to instability sites [38] and may lead to folded plies (S-shaped wrinkles) if UD plies are used. Further work is required to measure the interfacial shear stress between plies of different fibre orientations and between different types of reinforcement to examine ply–ply interactions in the vicinity of the tool surface. Overall, the analysis of a complex geometry demonstrates the model capability to be utilised as a design tool to assess the final quality of parts which possess complexities beyond what analytical models can offer.

5. Conclusions

In this paper, a combined experimental and modelling approach was presented to shed light on the process-driven mechanisms responsible for the formation of out-of-plane defects in the course of the manufacture of fabric prepreg composites. The main focus of the modelling work was on through-thickness compaction and inter-ply shear as the main mechanisms responsible for the generation of wrinkles, both of which were implemented in a VUINTERACTION user subroutine at ply interfaces. While the compaction model is capable of accounting for the hybrid interaction of squeezing and bleeding flow, the experimental observations suggested only the latter takes place in the case of textile pre-pregs (specifically with the CYCOM 970 resin here). Inter-tow locking of undulated yarns along with the absence of any tougheners in the resin confirms this observation. The compliant penalty method offered a good compromise between the required accuracy of simulation results and the resulting calculation time by taking advantage of shell elements as opposed to computationally expensive continuum elements. A VUMAT subroutine was also used to describe the in-plane and out-of-plane behaviour of the fabric prepreps. The advantage of the modelling approach lies in the fact that decoupling the through-thickness compliance from the material behaviour enables the use of shell elements, which considerably yields faster simulation times.

Finally, industrially-relevant test cases encompassing the consolidation of a laminate over an external radius were investigated. The results revealed the laminate bulk factor, the geometry of the tool, and the boundary conditions applied to the laminate as the key drivers for wrinkle formation. Upon the combined effect of these phenomena, an excess length is created and accommodated through out-of-plane (and also in-plane) wrinkling of the composite plies. The simulation results agree well with the data obtained from experiments. The study also showed

that an informed choice of bagging could be used to aid the management of variability for the manufacture of composite structures. This, in turn, generates a better understanding of their impact on fibre wrinkling risk and size.

The numerical approach presented herein proved its ability to effectively capture the appearance of consolidation-induced wrinkles that form during the manufacturing processes of textile composites. Its ultimate goal is to understand the relationship between material and manufacturing process variations with the formation of fibre-path defects. The scheme used here is relatively versatile and can be applied to a broad spectrum of potential part geometries/fabric prepreg systems throughout the whole manufacturing process from preforming to consolidation. Moreover, it provides a good basis for defect mitigation through the optimization of the process parameters and part geometry as shown in section 4.3.1 where a complex generic geometry was analysed. It, therefore, holds great potential to help reduce part development cost and wastage, through the early introduction of the manufacturing constraints at the design stage of the production of the parts. Future efforts should take advantage of this understanding gained here. For example, there is a need for a more accurate representation of some of the physical aspects behind the formation of wrinkles, namely, the viscoelastic behaviour of prepreg in the fibre direction, the strain path dependency of the fabric in tensile and shear direction (as described in [58]), the tack between the plies and possible interaction between the laminate and the resin, which bleeds out of the system, not considered in the current model. Moreover, the effect of voids is only represented phenomenologically in the extraction of the material parameters from the experiments with the fluid considered being a mixture of air and resin instead of pure resin. However, the consolidation model could be extended to directly account for the compression and possible migration of entrapped gas in the system. The 1-D compliant approach may also be further updated to describe the in-plane deformation arising from compaction of more sophisticated prepreg systems. Such a model would then be able to provide further insights into the ways to control the manufacturing processes.

CRedit authorship contribution statement

Armin Rashidi: Conceptualization, Methodology, Software, Formal analysis, Investigation, Writing - original draft. **Jonathan P.-H. Belnoue:** Conceptualization, Methodology, Supervision, Writing - review & editing. **Adam J. Thompson:** Conceptualization, Methodology, Software. **Stephen R. Hallett:** Conceptualization, Resources, Supervision, Funding acquisition, Writing - review & editing. **Abbas S. Milani:** Conceptualization, Resources, Supervision, Funding acquisition, Writing - review & editing.

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python code allowing automatic extraction of the material parameters for the compaction model directly from the experiment is, in particular, greatly acknowledged. Prepregs were provided by the Boeing Canada Technology Ltd., Winnipeg. All data to support the conclusions are provided in the results sections of this paper.

Appendix A. Details of film thickness calculation procedure

The topology of the yarns alters the pressure distribution at the entrance of the contact zone. This inlet zone may influence the film profile significantly, and the film pressure distribution cannot be represented only by the contact pressure distribution calculated by FE analysis. Hence, the film pressure needs to be modified in the inlet zone. Fig. A1 shows the contact zone in a 2-ply stack. As shown earlier, h_0 can be obtained by solving the Reynolds equation by using the IHL method, and the film thickness can be expressed as:

$$h(x) = 1.5 h_0 + h_y(x) \quad (\text{A1})$$

where $h(x)$ is the nominal film thickness defined as the distance between the mean levels of the two surfaces. Assuming the yarn geometry as an ellipse with a and b as the semi-major and semi-minor axes, h_y is defined using the following description [40].

$$h_y(x) = \left(1 - \sqrt{1 - x^2/a^2}\right) \cdot b \quad (\text{A2})$$

Appendix B. Effect of bagging configuration on the formation of wrinkles

One of the interesting observations in the wrinkling experiments is the impact of bagging configuration on the size and location of the wrinkles near the edges and in the centre region of the laminates (See Fig. 17). As also described by Bloom et al. [13] and Thompson et al. [59], boundary conditions imposed by the bag may result in uneven distribution of pressure across the laminate and thus the formation of low and high-pressure regions. As shown in Fig. B.1, apart from bridging effect from the bag or existence of pleats/creases, another factor found in this study is the height difference between the debulked laminate and the cork dam, which results in disrupted pressure distribution on the edges of the laminate once the bag is stretched in the transitional area between the plies' edges and the dams.

Tables and Figures

Table 1. Characteristics of the fabric prepreg system.

Manufacturer	Cytec
Architecture	Plain Weave, 3K
Fibre type	T300 filament PAN-Carbon
Area density (g/m ²)	2722
Sett (yarns/cm)	3.5
Filament diameter (μm)	7
Nominal thickness (mm)	0.33
Resin type	CYCOM 970 Epoxy Resin

Table 2. Material parameters controlling T300/CYCOM 970 prepreg viscous behaviour at tested temperatures. Data sets consist of three parameters for each temperature.

$T, ^\circ C$	30	50	70	90	110
k	0.8373	0.7868	0.738	0.7222	0.7385
a	-0.769	-0.855	-0.8929	-0.8812	-0.8082
b	-13.71	-14.24	-13.41	-13.12	-12.95

Table 3. Material properties used in the FE models. E represents the stiffness in the two fibre directions and G12 is the shear stiffness as a function of the shear angle γ . Membrane elements include the in-plane material properties and shell elements describe the flexural stiffness [60, 61].

Material	E_1 (MPa)	E_2 (MPa)	G_{12} (MPa)	G_{23} (MPa)	ν
Fabric (Shell)	88	88	0	40	N/A
Fabric (Membrane)	55000	55000	$45.3\gamma^4 - 43.53\gamma^3 + 25.3\gamma^2 - 5.2\gamma^2 + 0.73$	N/A	N/A
Bag (Membrane)	3000	N/A	N/A	N/A	0.3

Table 4. Input parameters for the mixed lubrication model [31].

Description	Symbol	Unit			
Temperature	T	$^\circ C$	50	75	115
Viscosity	μ	Pa. s	346.61	79.46	25.02
Root mean square roughness (RMS) of ply surface	σ	μm		12.33	
Fibre radius	β	mm		0.0035	
Reduced Young's modulus	E'	MPa		29493.78	
Fibre's Poisson's ratio [62]	ν	N/A		0.26	
Yarn contact area	A_{yarn}	mm^2		2.76	
Empirical friction coefficient	μ	N/A	100 kPa	300 kPa	500 kPa
			0.313	0.305	0.281

Table 5. Quantification of wrinkles and comparison with the FEM results. Only the main central wrinkle is considered for the 18-ply specimens. CV is the coefficient of variation within the measurements.

Specimen ID: {shape | number of plies | end boundary condition (C or F)}

Specimen	Final thickness			Location (λ)		Number of wrinkled plies		Amplitude (A)			Width (λ)			Angle (β)		
	Exp.	CV (%)	FE	Exp.	FE	Exp.	FE	Exp.	CV (%)	FE	Exp.	CV (%)	FE	Exp.	CV (%)	FE
C 24 F	5.02	0.9	4.91	45°	45°	18	22	3.94	3.9	4.87	7.07	2.4	7.46	33°	2.6	36°
L 24 F	5.01	1.2	4.88	60°	60°	19	23	3.30	2.5	3.97	5.49	1.9	4.94	15°	2.1	20°
L 24 C	5.03	1.4	4.89	45°	50°	24	24	5.80	4.1	5.88	9.73	3.7	8.02	44°	3.6	52°
L 18 F	3.82	2.0	3.84	Increased yarn undulation - no prominent wrinkling												
L 18 C	3.81	1.7	3.83	50°	60°	11	17	1.37	3.5	2.21	6.29	5.1	4.76	12°	4.1	43°

Fig. 1. (a) Consolidation of fabric prepreg stack over curved tooling. Formation of bookends and ply wrinkling is highlighted in the corners and flange's ends; (b) Geometric parameters associated with a wrinkle.

Fig. 2. Main deformation behaviours considered herein for the consolidation phase of composites manufacturing. The plotted strain-stress relationships for each mode are accounted for in the modelling framework described in Section 2. More emphasis is placed on the interface behaviour (i.e. compaction and inter-ply shear) while a linear elastic model is chosen for ply bending for the sake of simplicity.

Fig. 3. Overview of the modelling framework. A VUMAT subroutine describing the in-plane and out of plane deformation through a superimposed membrane and shell elements is linked to VUINTERACTION user subroutine which is responsible for frictional and compaction behaviours.

Fig. 4. Significance of mixed lubrication in the processing of fabric prepregs: (a) Stribeck curve showing the dominance of mixed lubrication mode in inter-ply shear experiments at various temperatures [42]; (b) Load Balance between the external pressure and the sum of the resin pressure and the effective fibre bed stress of the stack prepregs.

Fig. 5. Compliant penalty formulation proposed in this study. The compressibility of each ply is captured through inter-ply penetration (d_p) between the shell elements.

Fig. 6. Flow chart description of the VUINTERACTION subroutine.

Fig. 7. Dimensions of crucifix compaction specimens along with lay-up of stacks.

Fig. 8. (a) High-temperature compaction fixture mounted on Instron 5669 load frame; (b) Ramp-dwell loading program used for compaction experiments.

Fig. 9. (a) Manufactured L-Section and C-Section laminates; (b) Bagging arrangement of lay-ups. Silicone adhesive flash tape was used at the flanges' ends to achieve constrained ends condition; (c) Manufacturer's recommended cure cycle (MRCC) for CYCOM 970 resin system along with the progress of cure.

Fig. 10. (a) Thickness evolution in the ramp-dwell program; (b) Final normalized thickness vs. Temperature. A compaction limit is reached around ~ 70 °C.

Fig. 11. (a) Reconstructed X-CT slices from the cured samples tested at 30 °C, 70 °C, and 110 °C ;(b) Relative expansion of ply widths. Minimal sign of transverse spreading was observed due to tow inter-locking.

Fig. 12. Microscopic observations of samples consolidated isothermally (a) and sections from centre and edges (b); Voids, resin bleeding and fibre movement, are indicated in the figures at 30°C (c-d), and 110 °C (f-h).

Fig. 13. Finite element model of (a) compaction tests; (b) Inter-ply shear tests. As illustrated in the zoomed-in figure, a gap was defined as half the ply thickness.

Fig. 14. (a) Verification of the compliant contact showing the change in thickness at 110 $^{\circ}$ C. The overshoots on the reaction force before 400s is a result of dynamic effects caused by the explicit FE scheme; (b) Verification of mixed lubrication model based on the results obtained in [42]. The pressure distribution on the fixed ply at $P=100$ kPa is shown as an example.

Fig. 15. Images representative of each tested L-section and C-section configurations. Two sets of photographs are presented for each case at the mid-section (validation case for simulations) and edges of the laminates.

Fig. 16. Comparison between a micrograph of the C-section sample and the FE model. Good agreement is seen between the shape and size of the wrinkles as well as the termination angle toward the ends of the flange.

Fig. 17. The comparison between cross-section images of L-section samples and their corresponding FE model; (a) 24-ply L-section with constrained ends. The accumulated frictional forces on the longer flange resulted in asymmetric wrinkle formation at the radius; (b) 24-ply L-section with free ends; Bending-induced partial in-plane waviness resulted from can be seen in the zoomed-in region indicating the tension and compression zones; (c) 18-ply L-section with free ends; The increased yarn undulation can be observed in the zoomed-in figure. The bookend captured by the model is also shown in the figure. (d) 18-ply L-section with constrained ends.

Fig. 18. (a) Comparison between the average final thickness of the cured samples and the FE model. Less than 5% error was observed for all simulations.; (b) displacement of one of the nodes on the top ply for 18-ply and 24-ply samples over the course of consolidation.

Fig. 19. (a) A truncated pyramid-shape forming tool, which is a representative geometry for aerospace applications. The simulation consists of two steps: forming and consolidation; (b) FE model setup: The simulation consists of two steps: forming and consolidation.

Final Shear Contour

(A)

Final Deformation

(B)

Forming

(C)

Consolidation

(D)

(E)

(F)

Fig. 20. (a) Final shear contour after consolidation; (b) Final deformation after consolidation; (c) Section A-A after forming; (d) Section A-A after consolidation. Zones I, II, and III show the occurrence and evolution of defects between the two stages of the processing. The tool contour is also shown in (c) and (d) to visualise better the deviation of the lay-up profile due to wrinkling, (e) Gradient plot showing the variation of thickness after forming and; (f) Consolidation.

Fig. 21. Bridging in the female corners during the forming stage.

Fig. A.1. (c) Sketch of the contact zone for the inter-ply shear model along with film thickness distribution. The yarn geometry is assumed as an ellipse where a and b are the semi-major and semi-minor axes [31, 40].

24-ply - L-Section - free ends

Fig. B.1. layup configuration and the difference between the wrinkles formed in the edge and centre of the specimens after cure.

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