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Article

Heat Transfer Analysis in Double Diaphragm Preforming Process of Dry Woven Carbon Fibres

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Abstract: Double diaphragm forming (DDF) represents an efficient manufacturing technique leveraging vacuum pressure and heat to form composite material stacks between flexible diaphragms. This study focuses on the critical role of thermal management during preforming, essential for material integrity, defect mitigation, and process efficiency. A comprehensive three-dimensional finite element model (FEM) is developed to investigate the heat transfer dynamics in DDF, incorporating temperature-dependent material properties such as specific heat and thermal conductivity under compaction and varying density conditions. A novel approach is introduced to predict thermal contact conductance (TCC) across multi-layer carbon fabric interfaces, validated using four laminate configurations. The resulting effective thermal conductivity of the laminates is applied in production scale simulations, enabling accurate predictions of temperature distributions, which are corroborated by experimental data. The findings highlight the significant impact of mesoscale interactions, such as yarn-level deformation and surface asperities, on TCC variation. The study provides an enhanced understanding of heat transfer mechanisms in DDF, offering insights to optimise process parameters, improve product quality, and advance manufacturing capabilities for complex geometries.

Keywords: woven carbon fabrics; double diaphragm preforming; heat transfer analysis; finite element modelling; thermal contact conductance

1. Introduction

Double diaphragm forming (DDF) is a cost-effective manufacturing method widely employed in liquid composite molding (LCM) to produce high-quality unconsolidated component shapes [1]. Its scalability and suitability for diverse production volumes make it an attractive choice for manufacturing industries prioritising efficiency and precision [2]. A significant advantage of DDF is its consistent contact with the fabric plies, which reduces the risk of out-of-plane wrinkling and facilitates quick demoulding, while also ensuring process stability and repeatability [3]. Additionally, DDF supports the fabrication of near-net shapes with complex geometries, minimal material waste, automation potential, and relatively low capital investment [4–7]. This versatile technique has been successfully employed in processing various materials, including thermoset prepregs [8], non-crimp dry fabrics (NCF) [9], thermoplastics [10], and dry biaxial fabrics [11–13]. Among these materials, woven fabrics are frequently used for manufacturing structural components due to their superior control over fibre displacement and improved handling and transport characteristics in LCM [14].

In dry fabrics without binders, the compaction behaviour is influenced by their woven nature, which introduces crimp into both single ply and multilayer configurations. During the DDF process, a single ply exhibits a characteristic three-phase compaction pattern. Initially, a linear trend is

observed as pressure reduces gaps between yarns and fibres. This is followed by a nonlinear phase where increased pressure leads to a further reduction of gaps within the fabric. Finally, a second linear phase occurs, driven primarily by the bending deformations of the yarns as the material becomes increasingly compact [15,16]. In multi-layer fabrics without binders, the compaction process mirrors the three phases observed in single-ply fabrics but introduces an additional factor due to fabric-fabric interaction, also known as nesting. Nesting significantly contributes to thickness reduction as adjacent layers interact during compaction. This behaviour can be mathematically modelled by treating the crimped yarns in adjacent layers as sine waves, with the degree of nesting determined by the phase shift between these waves. Maximum nesting, which results in the greatest thickness reduction, occurs at dimensionless phase shift angles of $\pm\pi/2$ (asymmetric superimposition), while non-nesting, which minimises thickness reduction, occurs at 0° and $\pm\pi$ (symmetric superimposition) [17].

The nesting and compaction behaviour can be theoretically understood and quantified by analysing changes in crimp angles between warp and weft yarns under pressure, alongside their influence on material properties [17–19]. This phenomenon impacts the overall structural and thermal properties of the fabric stack when it is constantly under compaction load. For instance, Zheng et al. [20] investigated the structural parameters of a twill weave glass fibre fabrics numerically, demonstrating that weave structure and increased warp density both reduce heat transfer properties, primarily due to structural variations [20]. However, post decompaction, it is important to note that in dry fabrics cut for assembly and undergoing DDF process, fabric edges may lose stability, leading to fraying and elastic thickness recovery. To address these challenges, specific trimming and stabilisation techniques can be employed to maintain edge stability and dimensional consistency [21].

Stabilisation techniques are crucial for producing preforms that achieve the desired volume fraction and structural integrity. By enhancing stability, these preforms can be efficiently transported and handled automatically during subsequent LCM processing steps without incurring distortion. When stabilisation is conducted under DDF conditions, it is referred to as double diaphragm preforming (DDPF). This technique involves embedding the carbon fabric plies with thermoplastic or thermoset (reactive) binders. These binders effectively tack adjacent layers, resulting in binder-stabilised preforms upon the application of heat and subsequent cooling [21–24]. The selection of binders plays a crucial role in DDPF process, as they come in various forms, such as powders, veils, commingled yarns, hot melt webs, and adhesive films. Each type of binder distinctly influences the mechanical properties, thermophysical behaviour, and resin permeability of the preforms, allowing for optimisation of their characteristics for specific applications [25–27].

The incorporation of binders in DDPF significantly enhances the initial thickness of preforms, impacting their elastic and viscoelastic properties. Additionally, this inclusion establishes an inverse relationship between the compression rate during the forming process and the resulting thickness of the preform. Furthermore, the binders influence the nesting behaviour, the bending of the yarns, and the gaps arising from variations in contact between fabric layers. Studies indicate that an optimal binder content of 4 wt% strikes a crucial balance between formability and mechanical performance [24]. From a thermophysical perspective, a 4 wt% binder content reduces the rate of heat transfer through the thickness of the preform while increasing its specific heat capacity due to the introduction of gaps and additional material between the fabrics [28]. In terms of resin permeability, the location and concentration of binders are critical, as they can obstruct resin flow channels between fibre tows, thereby affecting the overall permeability of the resin system in various directions [26,29].

In the DDPF process, binder systems are activated using a variety of heating methods commonly employed in industrial applications, including infrared heaters, heated tooling, ovens, autoclaves, and inductive or ultrasonic heating devices [22,30]. Each of these methods offers distinct advantages and disadvantages based on factors such as cooling systems, cycle times, part geometry, heat transfer efficiency, and overall process control [30]. A particularly effective approach in the DDPF process is the use of isothermal multi-section tooling for automated preforming, which mitigates prebonding

and the formation of overheated zones that could lead to binder degradation and reduced preform quality [28,30,31]. This technique ensures uniform temperature distribution throughout the thin preform, a critical factor in achieving optimal interlayer bonding, enhancing energy efficiency, and maintaining precise process control, similar to the critical temperature control required during resin curing [31].

The DDPF process operates under complex multi-physics conditions influenced by variations in thickness, compression, friction, bending, in-plane tension, heat transfer, phase changes and boundary losses. Consequently, extensive numerical and experimental studies have been conducted across various material systems to explore these interactions [1,4,9,11,13,22,28,32–34]. Among these studies, to prioritise optimisation, numerical simulations are increasingly employed for effective mechanical process and defect predictions, significantly reducing reliance on costly trial and error experimental methodologies [3,35,36]. These mechanical simulations require accurate material characterisation models that can effectively predict defects during the numerical process simulations [33,37,38]. Similarly, thermal simulations also require precise understanding of the heat transfer properties of the preforms and bagging materials, particularly under the conditions associated with the DDPF processing conditions [28,39,40].

Current thermal characterisation techniques [41] and heat transfer studies applied to a variety of engineering materials use experimental methods [28,39,40,42–48], numerical simulations, and hybrid approaches combining both [49–62]. Among these, experimental methods typically include established techniques such as laser flash analysis (LFA), guarded heat flow meters, transient plane source (TPS) methods, and modified transient plane source (MTPS) methods. In addition to these standard techniques, non-standard approaches like thermal mapping [28,40], time-domain thermoreflectance (TDTR), the 3ω method, and frequency-domain thermoreflectance (FDTR) [42] have also been utilised to provide deeper insights. In numerical studies, some approaches accurately model the interface geometry to include the surface roughness of effects, while others simplify the representation by using a virtual layer model, depending on the application requirements. By accurately replicating the temperature boundary conditions observed in experimental setups, these studies effectively predict material heat transfer properties [42,54,63]. Some investigations extend to multilayer materials, incorporating models that account for thermal contact conductance (TCC) at the interfaces [36,41,45,63–69]. This focus on TCC is essential, as the structural composition of woven fabrics and the varying contact interactions at interfaces influence heat flow, making the effective thermal conductivity of multilayer stacks dependent on the number of interfaces [45].

Traditional experimental methods for determining thermal conductivity often rely on parameter or function estimation through inverse heat transfer problem (IHTP) techniques but generally do not account for TCC effects across multiple stack locations. This oversight can lead to underestimation of the actual thermal conductivity [45,68–72]. Alternatively, surface roughness measurements have been used to predict TCC, utilising scale-resolved boundary geometries to estimate contact resistance and applying numerical methods for TCC prediction [63,64,66]. Calibration techniques for TCC and resistance estimation have also been developed for both single metallic interfaces [65] and two interfaces in a three-layer composite stack [67]. A comprehensive approach that considers TCC effects in thick multilayer fabric stacks, such as those used in structural aircraft components, remains largely unexplored. Accurately determining the surface asperities at interfaces within multilayer stacks is challenging, particularly for woven fabrics, due to the nesting effects and the presence of binder materials. Additional factors such as presence of binder at the interface add complexity to precise characterisation under preforming conditions.

This study addresses the gap in understanding thermal contact conductance (TCC) effects by examining four configurations of multilayer fabric stacks at the coupon level, with layer counts ranging from 12 to 96, under DDPF process conditions. First, the effects of DDPF parameters, including compaction and heat transfer evolution, are experimentally investigated. A parameter-tuning approach is then applied to estimate TCC using numerical modeling, establishing a model-based relationship between layer count and TCC. This relationship is subsequently used to predict

temperature distribution in production-scale multilayer bulk preform model heating scenario in the DDPF process.

2. Materials and experimental methods

2.1. Materials and thermophysical properties

This study utilised a 3K aerospace-grade 2×2 twill carbon fabric (CF), Style 452-5, supplied by Engineered Cramer Composites (ECC), Germany, as shown in **Figure 1(a)**. This carbon fabric has an aerial weight of 204 gm⁻², with fibre diameters of 7 µm and a density of 1800 kgm⁻³. The linear density of fibres in both the weft and warp directions is 200 tex. The optical surface roughness variations in loom state are presented in **Figure 1(b)** with highest z direction variation of 1640.74 µm.

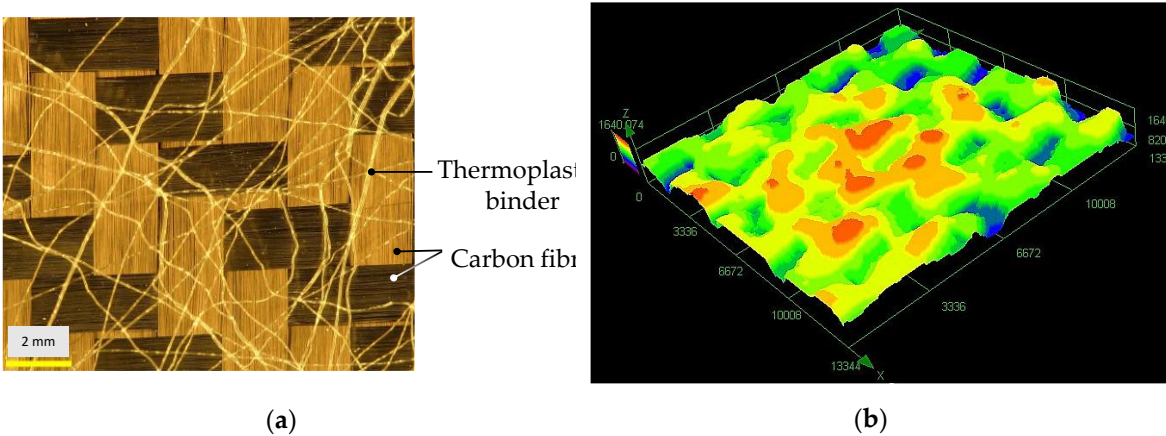


Figure 1. Optical images of (a) filtered 3K, 2 × 2 twill weave carbon fibre fabric with embedded thermoplastic web on top; (b) micro meter scale surface asperities in loom state of the fabric.

The binder material for preforming is Spunfab PA1203 (a co-polyamide binder) sourced from Spunfab Ltd., Ohio, USA, embedded between two fabric layers for tacking, as shown in **Figure 1**. This Spunfab PA binder has an aerial weight of 7.4 gm⁻², a melt volume rate of 30–50 cm³ per 10 minutes, and a melting range of 85–98 °C. For the diaphragm (bagging) material used in DDPF process, portions of a silicone diaphragm membrane (Mosites #1453-D) from Mosites Rubber Company, USA, are employed. This silicone membrane has a density of 1150 kgm⁻³, an aerial weight of 1840 gm⁻² a thickness of 1.52 mm, and an operating temperature range of -72 to 232 °C.

The thermophysical properties of these materials, examined by the authors under DDPF processing conditions, are summarised in **Table 1**, as detailed in reference [28].

Table 1. Thermophysical properties of carbon fibres + 3.6 wt% Binder samples and Silicone.

Material (Units)	Conductivity (<i>k_T</i>) W/m.K	Specific heat (<i>c_P</i>) J/g.K	Density (<i>ρ</i>) Kg/m ³
Carbon fibre (CF) + 3.6wt% Binder	0.0835	$c_{P1}(T)=0.75 + 0.0038T \ (0 \leq T \leq 85^{\circ}C)$ $c_P(T)=c_{P1}(T) + 0.0016(T-85) \ (85 \leq T \leq 150^{\circ}C)$	$V_f 1 \times PCF$
Silicone	0.238	$c_P(T)=1.33 + 0.0018T \ (0 \leq T \leq 200^{\circ}C)$	1150

¹ Volume fraction of carbon fibres with 3.6 wt% binder under vacuum conditions.

where *P_{CF}* is the density of the carbon Fibres, *c_p*(*T*) is the temperature dependent specific heat capacity, *V_f* is the volume fraction and *k_T* is the homogenised thermal conductivity of the 3.6wt % binder and carbon fibre preforms in the thickness direction. The thickness of the carbon fabric changes from 0.38 mm in its loom state to 0.204 mm in the laminate.

2.2. Double diaphragm preforming (DDPF) process of woven fabric preforms

A production scale double diaphragm former (Fill Gesellschaft m.b.H) with a forming area of 1500 mm × 1500 mm was used to validate experimental results as shown in **Figure 2(a)**. Two multilayer woven fabric stacks, with ply counts of 60 and 30 were prepared using binder embedded plies, each with dimensions of 300 mm × 200 mm. These stacks were positioned on the bottom diaphragm, separated by a release film, and fitted with embedded sensors to monitor processing conditions. In addition, BR180 Breather layer cloth, a 140-gsm non-woven felt fabric of 90% polyester and 10% polyamide, was used as a breather layer in vacuum bagging to avoid air pockets and equal distribution of vacuum as shown in **Figure 2(b)**. This material withstands curing temperatures up to 180°C, providing effective air evacuation during the composite layup process. The initial compaction step applied an 850-mbar vacuum between the diaphragms at a rate of 200 mbar/min, compacting the fabric plies within the bagging structure. A second vacuum stage of 80 mbar was then applied at a controlled rate of 20 mbar/min, pressing the bagged stack against a flat tooling surface measuring 1200 mm × 1200 mm × 100 mm. This tool was fitted with heating elements with embedded thermocouples (TC) beneath an active heating zone of 600 mm × 200 mm, with a guard heater surrounding the active area to reduce heat loss and ensure uniform temperature distribution.

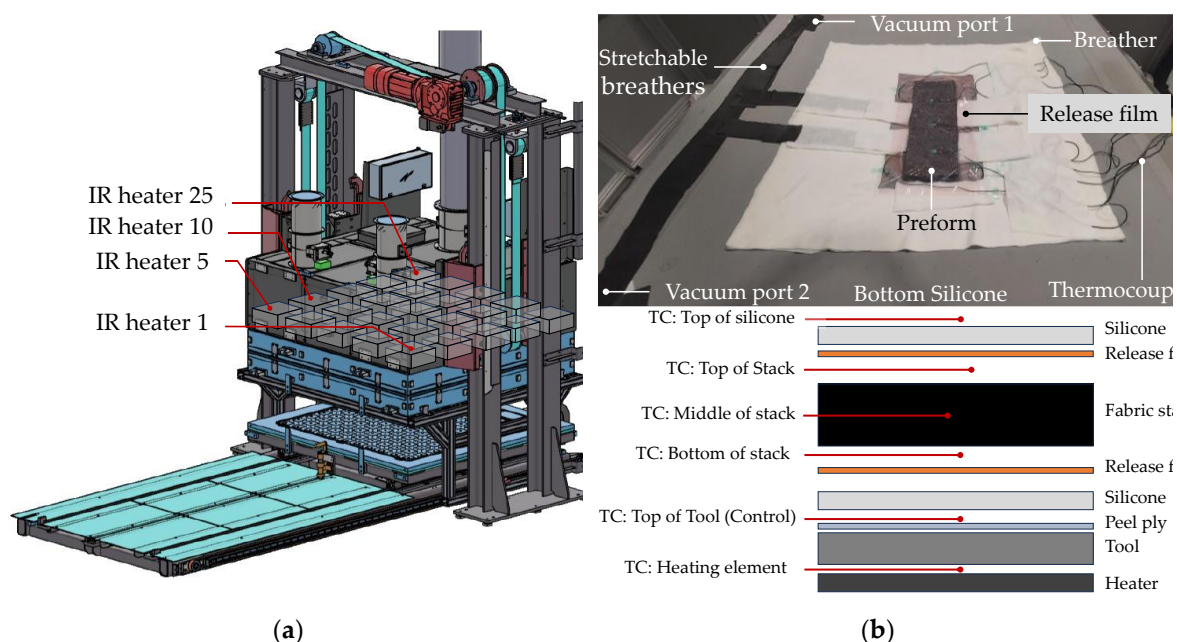


Figure 2. (a) Fill double diaphragm former machine with infrared (IR) heater zones and vacuum machine bed, (b) Experimental layup setup using preforms and different consumable materials along with through thickness location of the sensors.

During these vacuum cycles, the binder at the fabric interfaces conforms to the surface asperities of adjacent layers, promoting interfacial alignment through layer thickness reduction and slight yarn flattening. While the binder stabilised the stack, it also constrained direct interlayer contact compared to a binder-free configuration, where layer nesting primarily drives thickness reduction [17]. Following compaction, a heating cycle activated the binder, causing it to melt at the layer interfaces and further compact the stack. The melted binder penetrates the porous structure of the yarns, enhancing interfacial bonding and contact between layers [24]. The DDPF apparatus, equipped with infrared (IR) heaters, is shown in **Figure 2(a)** along with a schematic of the sensor integration, shown in **Figure 2(b)**. Thermocouples were positioned at various thickness points to track heat transfer through the woven fabric under DDPF conditions, providing critical data for thermal analysis. To numerically validate the heat transfer model, one experimental case was conducted using the DDPF setup to replicate bottom heating scenario. In this case, bottom-only heating was applied through

integrated heated tooling to establish baseline heat transfer coefficients in the process. This case provides essential benchmark for assessing the model’s accuracy in predicting thermal behaviour within the DDPF setup and understand the energy consumption and waste due to the inherent convection and radiation losses. The set target temperature of the heating cycle is 120 °C at a ramp rate of 8 °C per minute and has a dwell period of 30 minutes.

Since the DDPF process inherently involves both compaction and heat transfer, two additional experiments were designed to decouple these phenomena for detailed analysis. The first experiment focused on a compression test conducted using an MTS universal testing machine to measure thickness reduction and calculate the preform volume fraction. The second experiment utilised a custom heating setup to replicate the preforming cycle for multilayer stacks.

2.2. Compaction of multilayer coupon preforms.

For compression testing, four preformed sample configurations with ply counts of 12, 24, 48, and 96 were selected, each with an embedded binder web at the layer interfaces to closely replicate real preforming conditions. Tests were conducted using an electromechanical universal testing machine (UTM), specifically the Exceed Model E45 by MTS Systems, USA, outfitted with a 200 mm diameter compression plate rig. Initial calibration was performed without a sample to zero the crosshead displacement, after which each sample was centred precisely on the compression plates. Testing parameters included a strain rate of 1 mm/min, a controlled temperature of 22 ± 2°C, and a relative humidity of 56 ± 2%. With the sample in position, a force equivalent to full vacuum pressure, as shown in **Table 2**. was applied. Upon reaching the set force, the crosshead displacement was recorded to capture the compression response of the preforms under vacuum pressure as shown in **Figure 3(a)**.

Table 2. Compression testing mean sample measurements for 12, 24, 48 and 96 layers.

Parameter (Units)	Magnitude	Standard deviation
Contact area (mm²)	14609.57	1828.15
MTS Compaction load (kN)	1.48	0.185
Equivalent vacuum pressure (MPa)	0.1013	0.012

The thickness values obtained from the UTM experiments as shown in **Figure 3(b)** were used to calculate the fibre volume fraction of the stack (preform) during DDPF compaction. This was achieved using an empirical relation outlined in Equation 1.

$$V_f = \frac{n \times \rho_a}{\rho_f \times H}$$

(1)

Here, V_f represents fibre volume fraction, n is the number of plies, ρ_a denotes the areal density of the fabric, ρ_f is the carbon fibre density and H is the total stack thickness at that compression load [73]. To calculate the thickness of each individual ply within a laminate stack, the total stack thickness is divided by the number of plies. This normalised ply thickness is then applied in Equation 1 to determine the fibre volume fraction for each ply. Additionally, curve fitting was performed to establish the relationship between the number of plies in the preform and their respective thicknesses under an equivalent compaction load, as shown in Equation 2. The determined ply thickness in each preform configuration and determination of density and volume fraction in each configuration are shown in **Table 3**. The results indicate that as the ply count n increases, the preform thickness H increases, while the per-layer thickness l slightly decreases from 0.235 mm to 0.211 mm.

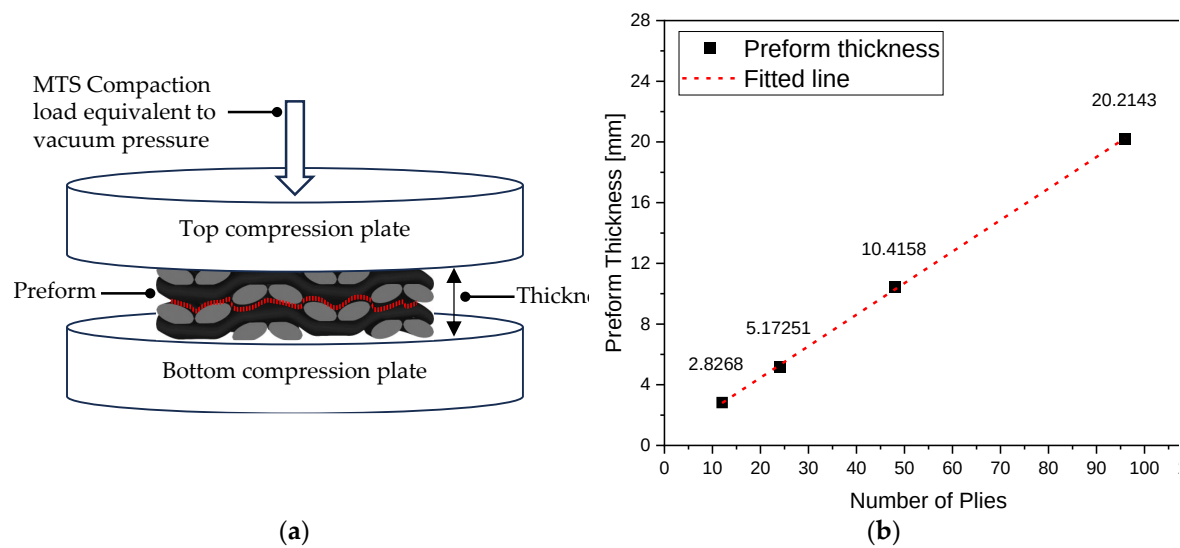


Figure 3. MTS Compression experiment (a) schematic, (b) measured thickness values of 12, 24, 48 and 96 layer preforms under equivalent performing compaction pressure.

Here, H represents the stack thickness, n is the number of plies, and the model achieved a high accuracy with an R squared value of 0.999.

$$H = (0.21 \times n) + 0.31 \tag{2}$$

The fabric density shows a corresponding increase from 865.99 kg/m³ to 968.81 kg/m³, with the fibre volume fraction rising from 0.48 to 0.53. The mean volume fraction across all ply counts is 0.51, with a deviation of 0.02, suggesting a stable relationship between ply count and compaction, which enhances fibre packing efficiency in thicker stacks.

Table 3. Determined ply thickness, density and volume fraction form MTS experimental data.

Ply count (<i>n</i>)	Preform Thickness (<i>H</i>)	Per layer Thickness (<i>l</i>)	Areal weight*	Fabric density	Fibre density*	Volume fraction
	mm	mm	g/m ²	Kg/m ³	Kg/m ³	
12	2.82	0.235	204	865.99	1800	0.48
24	5.17	0.215	204	946.54	1800	0.52
48	10.41	0.217	204	940.11	1800	0.52
96	20.21	0.211	204	968.81	1800	0.53
Mean				929.5±44.6		0.51±0.02

* Manufacturer datasheet.

2.3. TCC parameter tuning of multilayer coupon models under DDPF process conditions

2.3.1. Empirical relations for TCC parameter tuning in multilayer coupon stacks.

Structural configurations of dry woven fabrics exhibit inherent gaps at layer interfaces due to yarn crimp, especially when undergoing deformation under compaction loads. These gaps are further influenced by nesting behaviour, which is governed by the superimposition phase angle between adjacent woven fabric layers at the interface. To quantify the layer-by-layer nesting effect, empirical relationships are applied that consider both transverse and horizontal yarn shifts relative to the laminate [17,74]. This variation in contact at the interface affects heat transfer and leads to surface roughness related thermal contact resistance (TCR). As the number of layers in the laminate increases, so does the number of interfaces, and consequently, the number of contact resistances.

Figure 4(a) illustrates a schematic of the TCR between two contacting rough surfaces, modelled as a series resistance network combined with material resistance. and **Figure 4(b)** shows two layers of twill weave fabric with a binder at the interface.

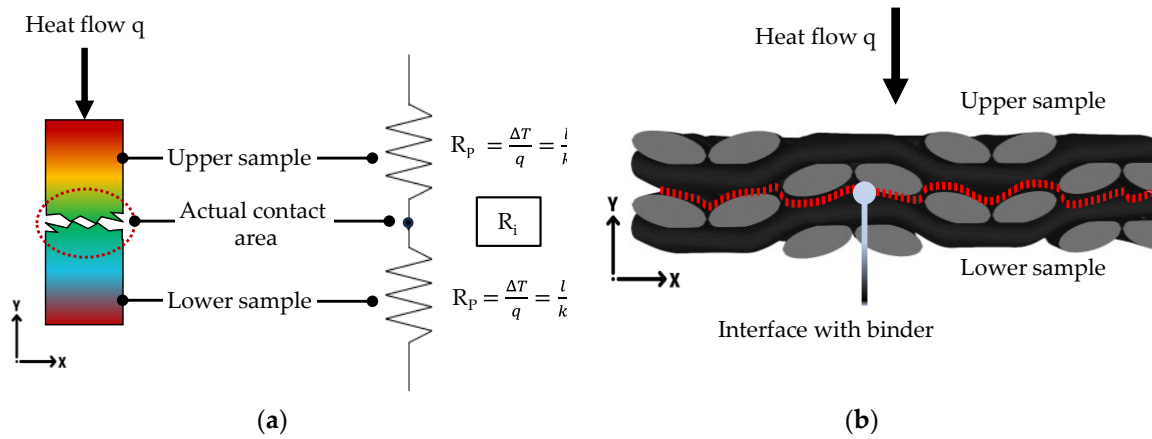


Figure 4. Schematic of the resistance-based networks of thermal contact resistance (a) In between two solid surfaces, (b) Interface contacts between two carbon fabrics layers with binder.

Considering this, Levy et al. [67] experimentally calculated thermal contact conductance (TCC) for a 3-layer thermoplastic laminate, considering surface roughness, TCR and contact conditions. This methodology forms the basis for the current study, which generalises the framework to multilayer stacks using a combined numerical and experimental approach to tune TCC parameters. In one dimensional heat flow models, both material properties and TCR are treated as thermal resistances in series, enhancing heat transfer modeling by accounting for surface roughness at layer interfaces. Both material properties and TCR are modelled as thermal resistances in series under one-dimensional heat flow, enabling the incorporation of surface roughness effects at layer interfaces. For a generalised stack of “ n ” layers, heat flow encounters “ $n-1$ ” TCRs, each contributing to the cumulative resistance.

Under steady-state conditions, the Voigt-Reuss-Hill [67] homogenisation principle ensures a constant heat flux across the stack and its interfaces. Assuming symmetrical layer alignment and uniform surface roughness at each interface due to the woven fabric structure and the presence of a binder, the effective conductivity and ply conductivity can be correlated to determine the common TCR (R_i) which is the inverse of the TCC (h_c) [41,63–67]. This allows the calculation of TCC using the empirical relations shown in Equations 6 and 7.

$$h_c = \frac{1}{R_i} = \frac{(n-1)q}{\Delta T - nR_p q} \quad (3)$$

$$h_c = \frac{1}{R_i} = \frac{(n-1)k_p k_e}{nl(k_p - k_e)} \quad (4)$$

If the TCC of the contact interface (h_c) is known, the effective conductivity of the stack representing the homogeneous effective conductivity in thickness direction can be determined from Equation 4 as follows.

$$k_e = \frac{nk_p l h_c}{(n-1)k_p + nh_c l_p} \quad (5)$$

Here, k_e is the effective conductivity of the stack and k_p is the conductivity of the ply in the thickness direction. ΔT is the temperature difference in the steady state where a constant heat flux q exists in the stack in thickness direction. To validate this relationship, combined numerical and

experimental approach was followed in which a ply-wise model of the experimental setup was modelled, assuming a 1D problem due to the stack thickness being significantly smaller than other dimensions.

2.3.2. TCC parameter tuning in multilayer coupon stacks – Experimental validation.

To replicate the DDPF process undertaken in Fill double diaphragm performer machine, a heated platen from a hydraulic and electric laboratory press (20-ton, LabTech Engineering Company Ltd., USA) was utilised as the heat source for vacuum layups comprising 12, 24, and 48 unidirectional (UD) fabric plies. The stretchable vacuum bag material, sourced from Mates Italiana Srl, operates at a maximum temperature of 120°C, boasts 490% elongation, and has a thickness of 75 μm . Temperature responses through the thickness of the stack were monitored (TC1) using a welded-tip fibreglass-insulated TFK2-K-type thermocouple (TC) (Instrument Choice, Syntronics Pty Ltd, Australia). This thermocouple was strategically positioned at various locations within the carbon stack to minimise edge effects, as illustrated in **Figure 5**.

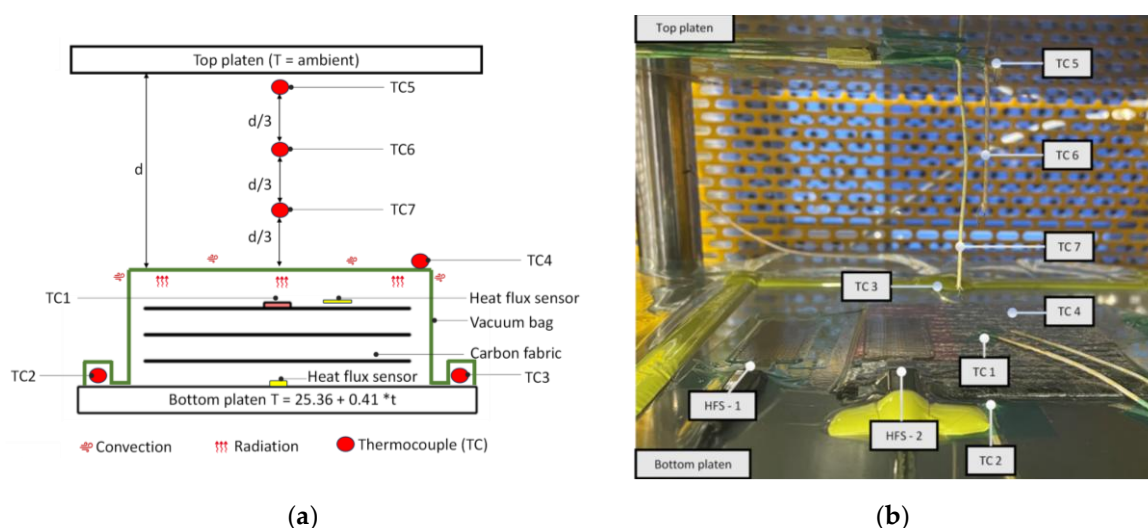


Figure 5. TCC parameter tuning approach (a) Experimental schematic with sensor locations, (b) Experimental setup with vacuum bagged carbon fabric preform with embedded binder and sensors.

TCs were connected to a data logger with a sampling rate of one second (Graphtec GL840 midi LOGGER, Graphtec Corporation, Japan). In addition to the main thermocouples, several control thermocouples were employed: TC2 and TC3 monitored the temperature of the heat source, TC4 assessed the temperature of the vacuum bag exposed to ambient air, and TC5, TC6, and TC7 measured the vertical air temperature directly above the carbon fabric. The vacuum layup was maintained at a pressure of 100 kPa and heated from ambient temperature to 120°C at a ramp rate of 25°C/min, after which it was held at that temperature. Each layup configuration was tested in three separate trials to ensure repeatability and establish a reliable relationship between the number of plies and the tuned common TCC parameter. Additionally, the experimental procedure was replicated using a 96-ply configuration to validate the correlation between the TCC parameter and ply count. To maintain accuracy throughout all experimental runs, the heated press environment was carefully controlled to minimise air circulation, ensuring consistent temperature readings. The results of the surface roughness analysis of the debagged coupon fabric stacks, conducted using an optical microscope, are presented in **Appendix A**.

3. Numerical methods for heat transfer simulations

3.1. Heat transfer governing equations

The transient governing equation and the considered boundary conditions are shown in equations 6 to 8:

$$V_f(\rho c_p) \frac{\partial T}{\partial t} = k_e \frac{\partial^2 T}{\partial x^2} \quad (6)$$

At the bottom surface at $x = 0$, Dirichlet temperature boundary condition is defined to match experimentally measured temperature T_1 as follows:

$$T(0, t) = T_1(t) \quad (7)$$

On the top surface at $x = H$, heat losses due to convection and radiation are defined as follows:

$$-k_e \frac{\partial T}{\partial x} \big|_{(x=H)} = h[T(H, t) - T(\infty, t)] + \epsilon \sigma [T^4(H, t) - T^4(\infty, t)] \quad (8)$$

To model heat transfer in production-scale preforms, conduction was identified as the dominant mode, with additional energy losses due to radiation and natural convection. These losses arose from heat sinks outside heating zones, air convection driven by temperature gradients, and radiative emission. The conservation of energy principle was applied to formulate the governing equations for both transient and steady-state conditions shown in Equations 9 and 10. Where $\dot{Q}$ is the internal heat generation term and ∇ operator denotes the gradient operator.

$$v_f(\rho c_p) \frac{\partial T}{\partial t} = \nabla \cdot (k \nabla T) + \dot{Q} - h(T - T_\infty) - \sigma \epsilon (T^4 - T_\infty^4) \quad (9)$$

$$\nabla \cdot (k \nabla T) + \dot{Q} - h(T - T_\infty) - \sigma \epsilon (T^4 - T_\infty^4) = 0 \quad (10)$$

In the above equations, V_f represents the volume fraction of the carbon fabric, while ρ denotes the effective density. The specific heat capacity is indicated by c_p , and T is the temperature of the material at time t . The effective thermal conductivity is represented by k_e , with x serving as the spatial coordinate and H indicating the thickness of the carbon fabric. The term T_1 refers to the experimentally measured temperature at the bottom surface, whereas $T(H, t)$ is the temperature at the top surface. Additionally, T_∞ signifies the ambient air temperature, h is the convection heat transfer coefficient, ϵ indicates emissivity, and σ represents the Stefan-Boltzmann constant [75,76]. Since woven fabrics are highly anisotropic materials, the heat conduction can be modelled using the anisotropic thermal conductivity and the corresponding thermal gradient, respective heat flux as shown in equation 11:

$$k_{ij} \frac{\partial^2 T}{\partial x_i \partial x_j} + \dot{Q} = \rho c_p \frac{\partial T}{\partial t} - Q_{convection} - Q_{radiation} \quad (11)$$

For fully anisotropic model, all the elements in the conductivity matrix are to be determined. To simplify the model, the material model is assumed to be orthotropic with conductivities considered in three principal directions and k_{11} is equal to k_{22} due to structured woven nature of the preform. The k_{33} term represents the through thickness direction thermal conductivity as shown in Equation 12, which is the resultant effective conductivity (k_e) computed using Equation 5.

$$\begin{pmatrix} q_1 \\ q_2 \\ q_3 \end{pmatrix} = \begin{pmatrix} k_{11} & 0 & 0 \\ 0 & k_{22} & 0 \\ 0 & 0 & k_{33} \end{pmatrix} \times \begin{pmatrix} \Delta T_1 \\ \Delta T_2 \\ \Delta T_3 \end{pmatrix} \quad (12)$$

where k_{ij} is the conductivity in the respective “ ij ” location in matrix form to completely represent the anisotropic thermal material properties.

3.2. TCC parameter tuning in multilayer coupon stacks

To replicate the experimental heating process for the coupon models, a ply-wise finite element (FE) model was developed in Abaqus to simulate one-dimensional (1D) transient heat transfer through the thickness of compacted plies as shown in **Figure 6**. Each carbon fabric ply was represented as a homogenised layer composed of woven fabric with a 3.4 wt% binder at the interface, with material properties derived from prior experimental testing specific to this preform composition [28]. To account for the porous nature of the carbon fabric, a porosity-dependent volume fraction was

applied to each ply, influencing heat transfer behaviour. In this modeling framework, ply thickness was determined using a normalisation method (see **Section 2.2**), allowing for the introduction of a TCC parameter at each ply interface. The TCC parameter was applied to capture variations in contact effects between plies, enabling a more accurate representation of the thermal response across the preform structure.

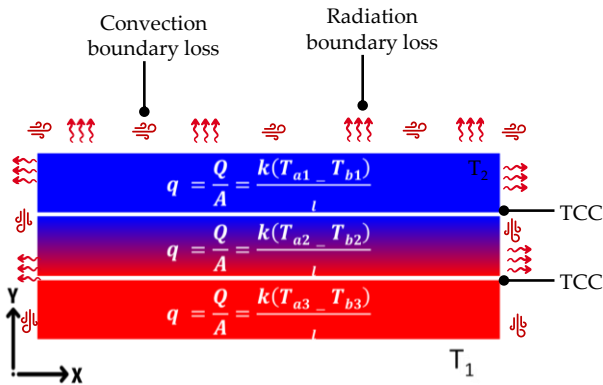


Figure 6. Schematic representation of the 1D numerical model replicating the experimental process incorporating contact interactions, temperature boundary conditions, and boundary losses (convection & radiation) in a ply-wise framework.

As illustrated in **Figure 6** the model’s bottom boundary condition (BC) was based on experimentally measured temperature data, T_1 , while the top boundary conditions accounted for both natural convection and radiative losses. To incorporate ambient air temperature fluctuations due to heat loss, air temperature measurements were taken at three separate locations and integrated into the simulation. The numerical model parameters for TCC tuning are provided in **Table 4** and the parameter that needs to be tuned for each ply configuration is represented as x_i and the temperature is compared with the experimental temperature.

Table 4. Numerical model setup parameters for various ply configurations.

Parameter		Symbol (Units)	12 - Ply	24 - Ply	48 - Ply	96 - Ply
Material inputs	Ply Conductivity*	k (W/mK)			0.835	
	Density	ρ (Kg/m ³)	865.9	946.5	940.1	968.8
	Specific heat*	$V_f \times c_p$ (J/KgK)	48%	52%	52%	53%
Process step definitions	Step time	t (s)	3200	3250	4500	4600
	Min Increment	t_{min} (s)			5.00E-05	
Interaction No. of contact interactions definitions	TCC	h_c (W/m ² K)	x_1	x_2	x_3	x_4
	No. of contact interactions	-	11	23	47	95
	Emissivity	ϵ (N/a)			0.3	
	Convection	h (W/m ² K)			3.3	
Mesh	Edge seeds	Thickness			4	
	Element type	Quad Shell			DC2D4	
	Total elements in assembly	N/A	11200	22400	44800	89600

* Experimental data

The effects of phase change and the resultant thickness variation of the binder were not included in the model, as both specific heat and thermal conductivity parameters inherently account for the 3.6 wt% binder in the preform [28]. The TCC estimation can be performed using an analytical

approach with experimental heat flux data or numerical heat flux data combined with temperature data in both cases. This is carried out to establish an empirical relationship between the number of layers and the TCC parameter in each configuration. With a defined convection heat transfer coefficient and emissivity for the top surface, the TCC was estimated by iteratively minimising the difference between simulated and experimentally measured top surface temperatures, T_2 . Additionally, multiple heating cycles demonstrated no significant impact on temperature evolution, further justifying the exclusion of these factors in the analysis. This approach simplifies the model while ensuring an accurate representation of the thermal behaviour within the preform. Positioning sensors between the top carbon fabric surface and the semi-transparent vacuum bag posed challenges in accurately determining the convection coefficient and emissivity of the top surface. To approximate the convection coefficient, the top surface temperature and ambient air temperature were initially used, based on empirical relations provided in **Appendix B**. Since the vacuum bag has a micrometer-scale thickness and is semi-transparent, it was omitted from the model, and adjusted convection and emissivity coefficients were instead applied directly to the top carbon fabric surface.

3.3. Homogenised production scale preform models.

Figure 7(a) illustrates a half-section of the 30-ply preform configuration, including the associated components. The representative numerical model was developed in ABAQUS using a symmetric configuration along the primary axis to improve computational efficiency (See **Figure 7b**). The setup included 30 and 60-ply preforms with tooling, bagging, and heater elements to replicate the preforming process.

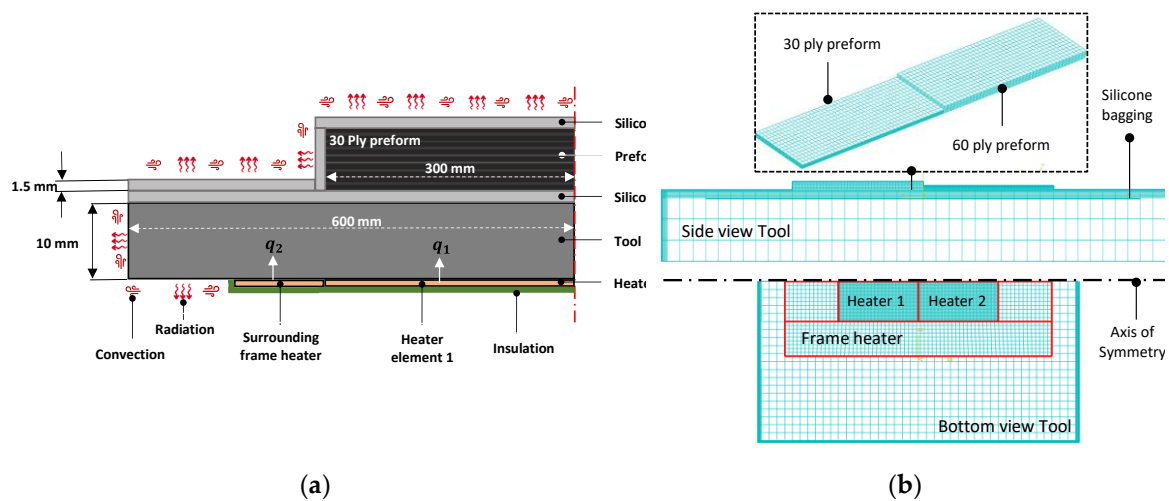


Figure 7. (a) Schematic of the half-model setup used in the DDPF process, showing the 30-ply preform, heater elements, and frame dimensions, along with the applied boundary conditions for the FE model. **(b)** Meshed components of the FE model prepared for numerical simulations.

Thermal properties specific to the materials and their interactions, including conduction, convection, and radiation, were defined to closely replicate the experimental conditions, as outlined in **Table 5**. Contact conductance at the material interfaces was determined by correlating the temperature differences across the interfaces with heat flux values recorded by sensors positioned above the silicone membrane. The calculated contact conductance values were 6700 W/m²K for the heater to tool interface, 1247 W/m²K for the silicone to CF interface, 23.69 W/m²K between silicone membranes, and 68 W/m²K for the tool to silicone interface. A grid dependency analysis was performed to refine the mesh of linear hexahedron elements, ensuring a balance between computational accuracy and efficiency. Volumetric heat flux values, derived from the energy consumption data of individual heater elements during the DDPF cycle experiment were applied as boundary conditions to the heater element geometry (see **Figure 7b**) to simulate the heating process.

Finally, the simulation temperature is compared in three distinct locations in the thickness direction of each zone with experimental data.

Table 5. Simulation parameters for the homogenised production scale preform model.

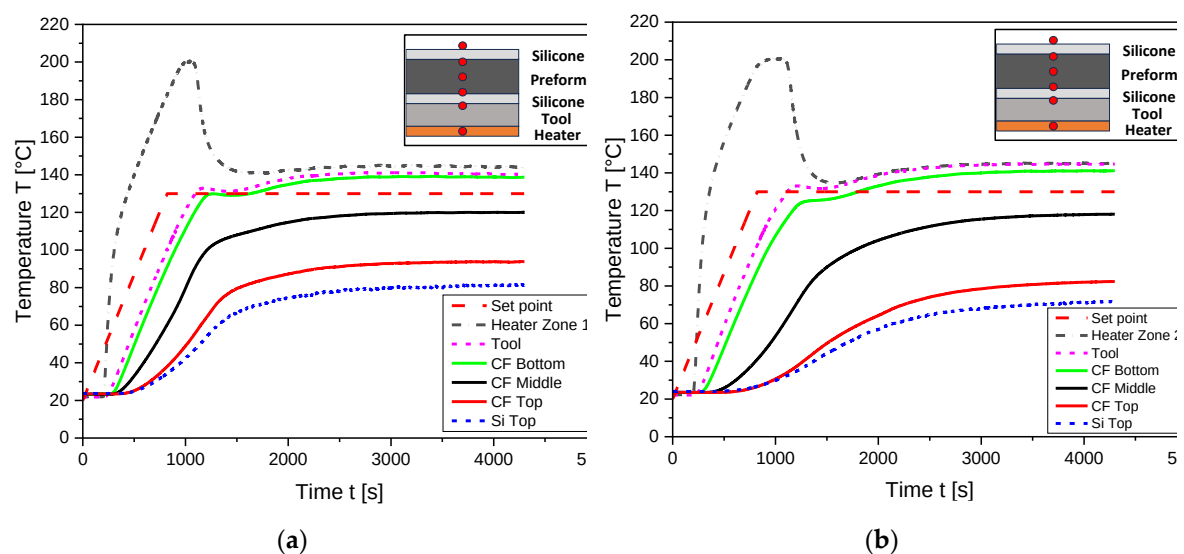
	Parameter	Symbol (Units)	30-ply	60-ply	Silicone	Tool	Heaters
Material inputs	Bulk conductivity	K_e (W/mK)	0.078	0.076	0.238	55*	0.216*
	Density	ρ (Kg/m ³)	925	946.5	1150*	7870*	1249.6*
	Specific heat	$v_f \times c_p$ (J/KgK)	51.4%	52.6%	Measured	508*	1880*
Step definitions	Step time	t (s)			4500		
	Min Increment	t_{min} (s)			0.001		
	Max Increment	t_{max} (s)			10		
Interaction	Emissivity	ε (N/a)	-	-	0.85	0.3	-
	Convection	h (W/m ² K)	-	-	7	4	-
	Mesh	Edge seeds	8	8	6	6	8
		Nodes	5940	5940	220624	11970	42678
Mesh	Edge seeds	Thickness	8	8	6	6	8
	Nodes	N/A	5940	5940	220624	11970	42678
	Elements	N/A	4816	4816	191096	9768	35704

* Manufacturer data sheet.

4. Results and Discussion

4.1. DDPF process experimental results

Figure 8 shows the temperature evolution at various locations within the experimental setup, including the heater elements, carbon preforms, tool surface, the top layer of silicone rubber, and the surrounding air, as a function of time. The heater output is controlled by the tool's TC, positioned on the surface directly above the heater element. The setpoint temperature, aimed at activating the carbon preform, is set to 130°C, ensuring that a temperature of 120 °C is achieved beneath the preform. At this setpoint, the heater elements reach a peak temperature of approximately 200°C during the ramp up phase. Heaters one and two stabilize after about 1300 seconds, while the frame heater exhibits oscillatory behaviour, fluctuating between 160°C and 200°C as shown in **Figure 8(c)**.



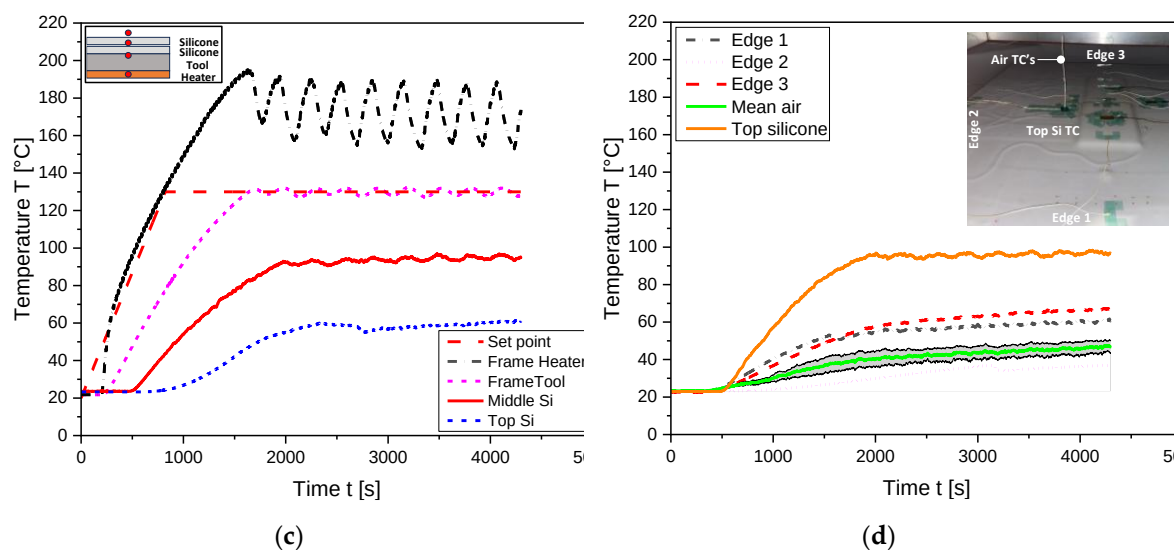


Figure 8. Temperature distribution during the DDPF process with bottom heating, measured across the thickness direction at various locations: (a) Zone 1 (30 plies), (b) Zone 2 (60 plies), and (c) Frame heater region, (d) top surface of the silicone near the tool edges, and ambient air temperature.

This oscillation compensates for heat sink losses caused by excess material around the frame. Zone two, which contains 60 plies, exhibits a slower temperature increase during the ramp-up phase and a lower stabilised top temperature compared to zone one. This behaviour is attributed to the increased thermal mass due to the additional 30 plies. Furthermore, the top surface temperature of the silicone bagging is higher in the tool's active zones compared to the edge regions, reflecting localised heat concentration near the heater elements. For the bottom heating configuration, the mean ambient air temperature above the silicone surface was measured at distances of 20 mm, 120 mm, and 220 mm. As shown in **Figure 8(c)**, the air temperature reaches a maximum mean value of 45°C due to convective heat transfer losses implicating some of the inherent losses in the process. In addition to the temperature data, **Figure 9(a)** and **9(b)** present the heat flux leaving the top surface and energy consumption of the heater elements over a cycle duration of 4500 seconds.

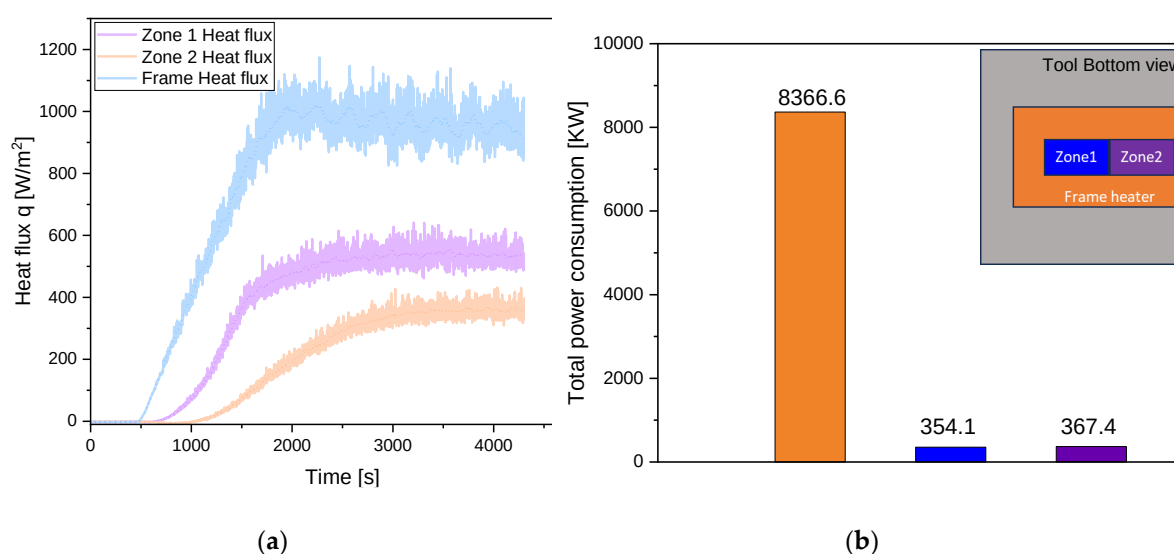


Figure 9. Experimental data on energy consumption and heat flux during the DDPF process: (a) Heat flux measured at the top surface of the silicone for the 30-ply region (violet), 60-ply region (red), and frame heater zone (blue), and (b) Total energy consumption over 4500 seconds of the heating and holding cycle for the three heater elements.

Heat flux analysis reveals a linear heat transfer rate in the frame heater region during the ramp-up phase, in contrast to the non-linear behaviour observed in the 30- and 60-ply preform regions. In the quasi-steady-state region, the stabilised heat transfer rates are 954 W/m², 537 W/m², and 356 W/m² for the frame heater, 60-ply region, and 30-ply region, respectively. The presence of additional carbon fabrics in zone two reduces the through-thickness heat transfer rate, potentially leading to longer cycle times. Energy consumption, derived from voltage and current data recorded by the controller, shows higher energy use in the frame heater compared to heaters one and two located in the active zones. Initially, all three heaters contribute to raising the material temperature. However, during the holding phase, only the frame heater remains active to maintain the desired temperature. Total energy consumption for the frame heater is 8366.6 kW, while heaters one and two consume 354.1 kW and 367.4 kW, respectively.

4.2. TCC parameter coupon numerical tuning and experimental validation study

This study is aimed to develop an empirical TCC relationship for multilayer coupon stacks by using a combined numerical and experimental approach. Three cases of 12, 24 and 48 are utilised to develop the TCC relation and the 96 layers is utilised as the validation for the developed empirical relation. The experimental evaluation initially demonstrated that with the increase of number of layers, heat transfer in the thickness direction reduces. The numerically tuned TCC parameters for different ply counts are presented in **Figure 10(a)** and the quasi-steady state temperature comparison of numerical and experimental top surface temperatures are shown in **Figure 10(b)**.

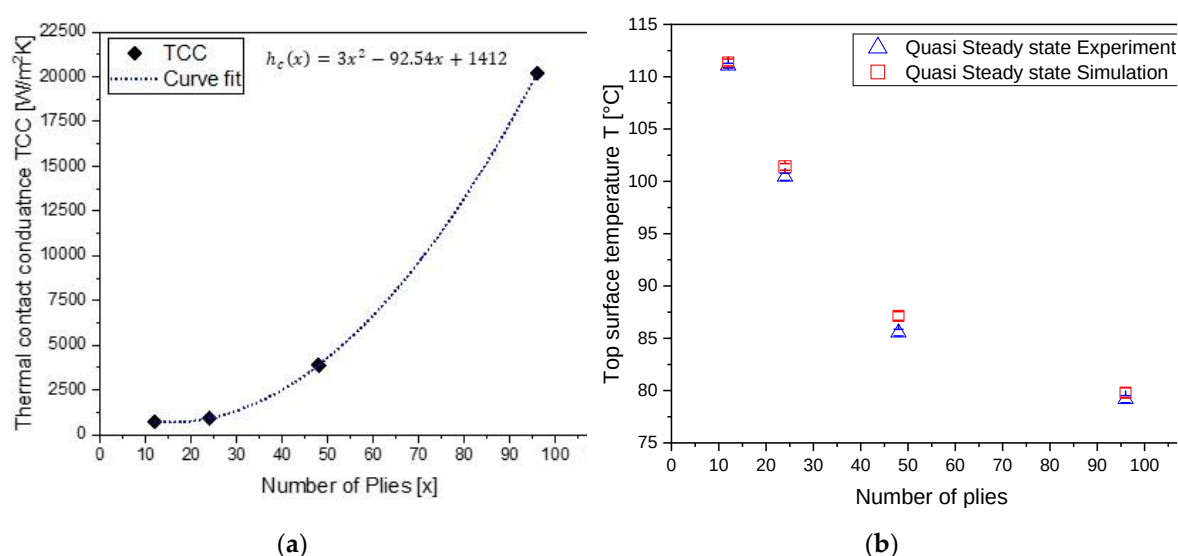


Figure 10. (a) Curve fitting to calculate TCC (h_c) as a function of number of plies developed form numerical tuning; (b) Experimental and simulation quasi steady state temperature comparison of the top surface.

During the ramp up phase, the ramp rate for 12, 24 and 48 layers are 20.12 °C/min, 14.53 °C/min and 8.86 °C/min respectively with a quasi-steady temperature difference of 9 °C, 18.5 °C and 32.2 °C respectively. In the case of 96 Layers, the ramp rate initially was 4.18 °C/min, before the temperature evaluation takes a nonlinear rise and finally attains a temperature difference of 40 °C in quasi steady state region. For the numerically tuned TCC values of 734 W/m²K, 921 W/m²K and 3890 W/m²K for 12, 24 and 48 ply stacks, the simulated temperature predications are in good agreement with the experimental results. By using these three points of data, curve fitting was carried out to obtain a TCC value for 96 ply stacks, which resulted in a TCC value of 20208 W/m²K, which predicts the top surface temperature accurately. The curve fitted empirical relationship between the TCC parameter, and the number of plies is shown in Equation 16, with an R squared value of 0.999. This equation

when used in conjunction with equation 8, the effective through thickness bulk conductivity of the stack can be estimated for numerical preforming heat transfer simulations.

$$h_c(x) = 3x^2 - 92.54x + 1412 \quad (16)$$

In addition to this steady state, the transient effects of the tuned TCC parameter are also analysed on the numerically predicted top surface temperature for 12, 24, 48 and 96 layers as shown in **Figure 11(a) to (d)**.

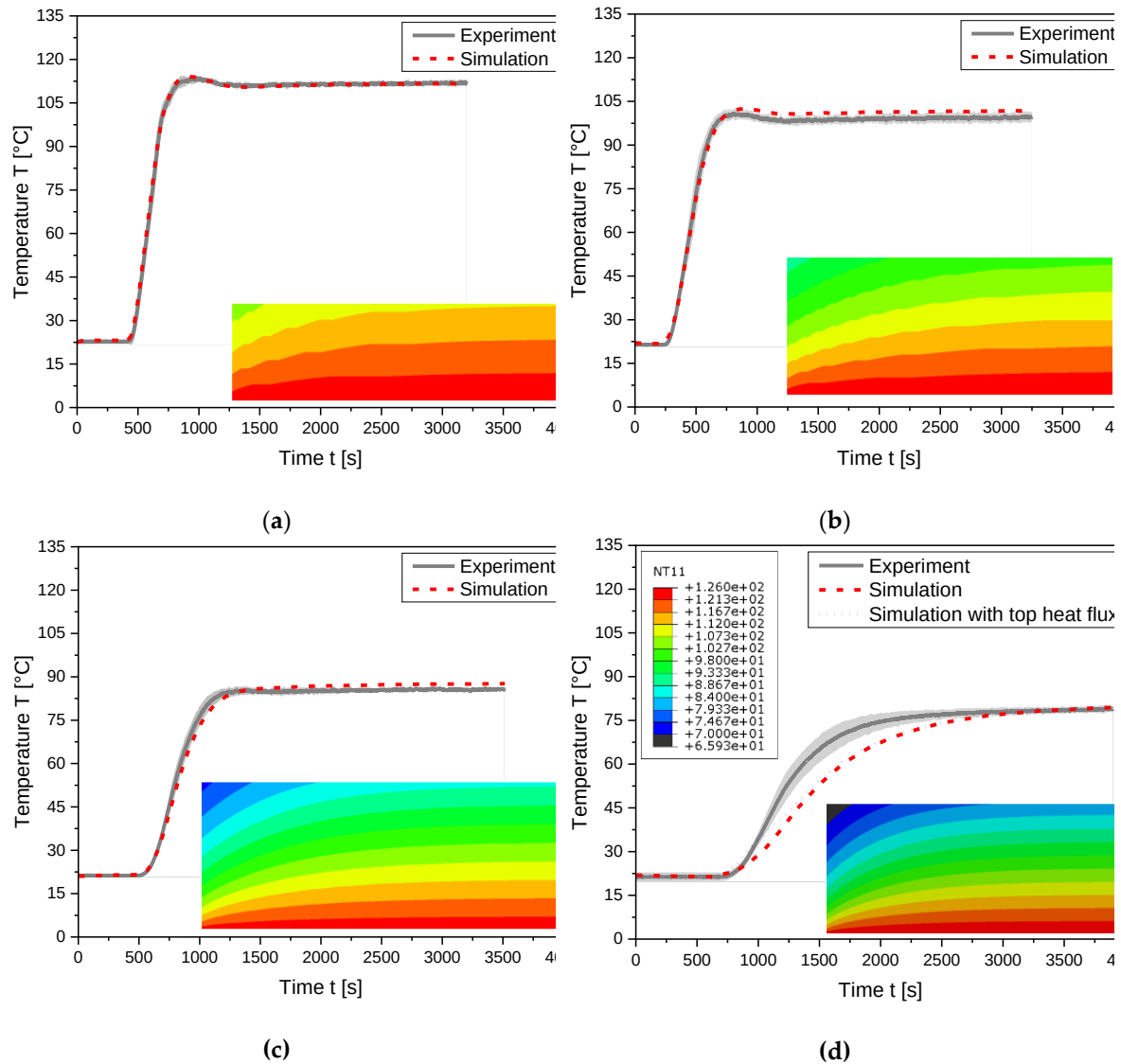


Figure 11. Comparison of experimental and simulation results for transient temperature profiles during the heating cycle of ply wise models along with end simulation temperature and edge effects: (a) 12-layer stack, (b) 24-layer stack, (c) 48-layer stack, and (d) 96-layer stack.

These results for multi-layer coupon stacks reveal specific trends related to heat transfer mechanisms and the influence of external convection factors in transient domain. For thinner stacks, such as the 12 and 24-layer configurations as shown in **Figure 11(a) and (b)**, the experimental and simulation results align closely, as heat conduction dominates, and external factors have minimal impact. Heat energy dissipates efficiently through the material, resulting in consistent temperature profiles. In thicker stacks, particularly the 96-layer configuration as shown in **Figure 11(d)**, the experimental results deviate from the simulations due to slower heat conduction through the stack thickness. Additionally, the vacuum bag, a micrometer thick layer covering the stack and heater tool outside the preform perimeter, heats rapidly and influences heat transfer at the top surface TC sensor.

Surrounding air above the vacuum bag also heats up and circulates, especially during the initial stages of the heating cycle. This external convective heat transfer affects the temperature readings at the top thermocouple sensor, particularly in the 96-layer stack, where heat dissipation through the material is slower. The effect is less pronounced in the 48-layer stack but still contributes to minor deviations.

Despite these limitations, all configurations reliably predict the quasi-steady-state top temperatures of the preforms for a given bottom temperature, highlighting the model's effectiveness in capturing thermal equilibrium.

4.3. Homogenised production scale preform model validation

The numerical model developed in ABAQUS was validated against experimental temperature data for both 30-ply and 60-ply dry fabric preforms, demonstrating strong agreement across multiple measurement locations as shown in **Figure 12**. The model accurately predicted the temperature behaviour during the quasi-steady state, but some discrepancies were observed during the ramp-up, particularly at the top surface. For the 30-ply preform, the predicted peak temperature at the bottom surface reached approximately 133°C, with a deviation of 4°C (2.92%). At the mid-plane, deviations of 7°C (5.93%) were observed, while the top surface temperature during steady-state conditions showed a deviation of 2°C (2.17%) as shown in **Figure 12(a)**. For the 60-ply preform, the peak temperature at the bottom surface was predicted to be around 133°C, with a deviation of 3°C (2.21%). The mid-plane exhibited a deviation of 6°C (5.17%), and the top surface showed a deviation of 2°C (2.38%) during steady-state conditions. During the ramp-up phase, the model's accuracy varied in predicting the ramp rates as shown in **Figure 12(b)**. For the 30-ply preform, the experimental ramp rates were 0.12°C/s at the bottom, 0.09°C/s at the middle, and 0.059°C/s at the top. The simulation predicted ramp rates of 0.11°C/s at the bottom, 0.08°C/s at the middle, and 0.061°C/s at the top.

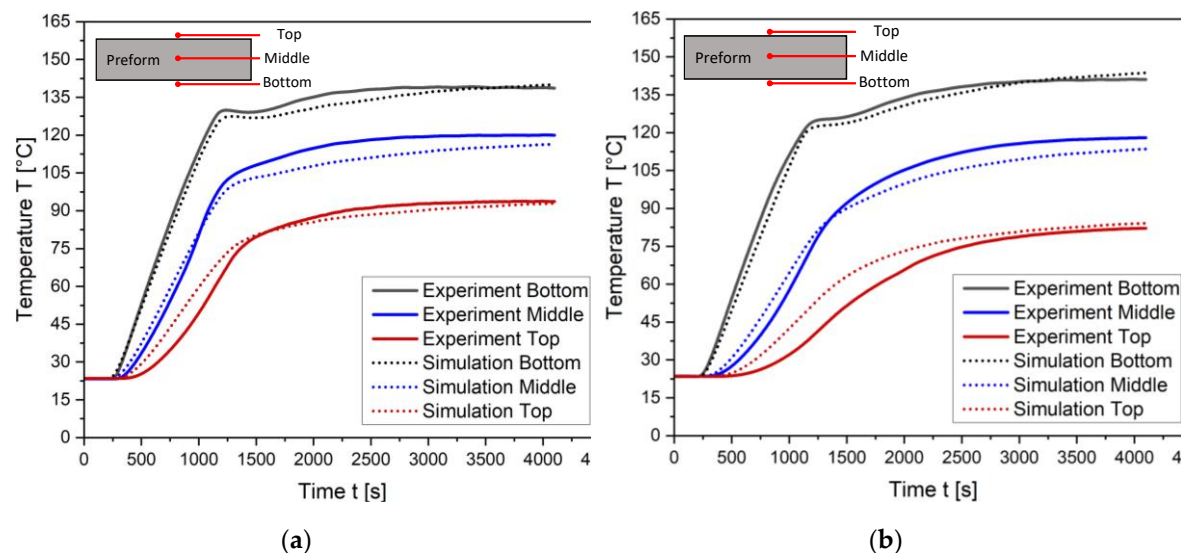


Figure 12. Numerical validation of the heat transfer model with experimental data in bottom, middle and top locations: (a) 30 Ply preform and (b) 60 Ply preform.

This numerical correlation indicates that the model slightly underpredicted the ramp rates at the bottom and middle, with deviations of 3% and 8%, respectively, while closely matching the top ramp rate with a deviation of 3.3%. For the 60-ply preform, the experimental ramp rates were 0.119°C/s at the bottom, 0.072°C/s at the middle, and 0.035°C/s at the top. The simulation predicted ramp rates of 0.115°C/s at the bottom, 0.07°C/s at the middle, and 0.044°C/s at the top. The model slightly underpredicted the ramp rates at the bottom and middle, with deviations of 3.7% and 3%, respectively, while over predicting at the top ramp rate with a deviation of 20%. These discrepancies

observed, particularly during the ramp-up phase, can largely be attributed to the limitations of the numerical model in accounting for certain factors inherent in the experimental setup such as the convection losses, radiation exchange with the top IR heater array and assumed in plane conductivity for fabrics. The numerical nodal temperature distribution at the end of 4100 seconds is presented in **Figure 13**, showing temperature gradients in the model indicating the inherent heat losses in the system. To optimise this and minimise the heat losses, efficiency studies are necessary with change in the experimental strategy to minimise convection and radiation heat losses.

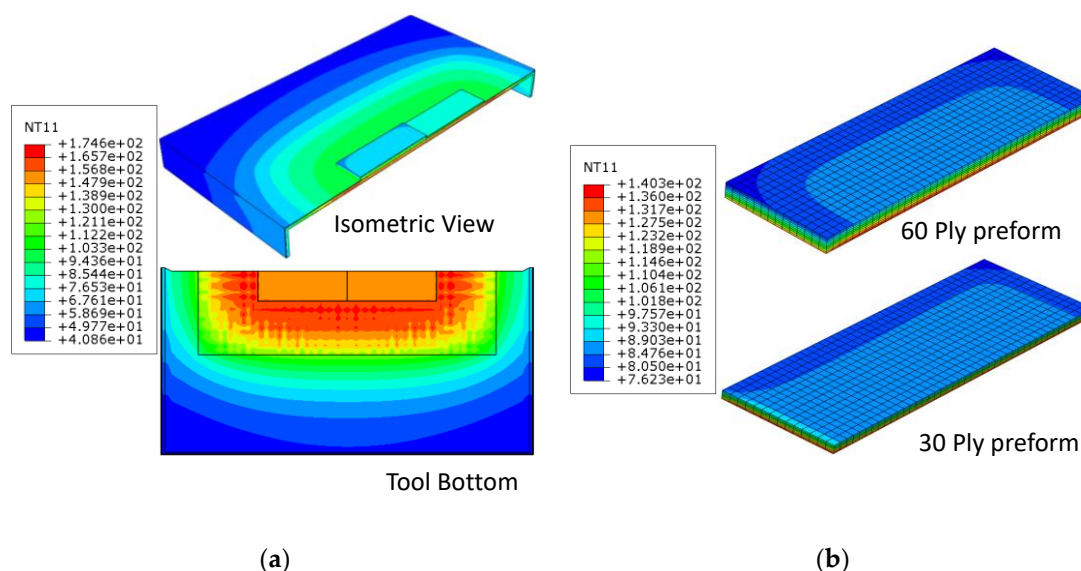


Figure 13. Numerical temperature distribution at 4100 seconds of the FE model: (a) Tool Isometric view and bottom view, (b) 60 and 30 Ply preforms.

One key factor is the silicone membrane heating up faster outside the preform active zone in the experimental setup, which is located directly on the frame heater. This rapid heating of the silicone membrane creates localised airflow. It affects the sensor measurements at the top location, causing it to reach higher temperatures before the top location of the preform. Additionally, the high emissivity of the IR heater elements likely increases radiative heat absorption, altering the localised thermal environment and influencing the airflow patterns and radiative heat transfer, especially at the top surface. These interactions, simplified in the model, may explain the overprediction of temperature, particularly at the top surface during the ramp-up phase. In terms of modelling, for this internal heat generation model, time step integration also had effect on predicted temperature with lower time integrations predicting accurate temperature.

Based on these validation results, it is evident that the model should include additional heat loss factors at the top location and incorporate accurate in-plane conductivity of the fabrics. Currently, the model assumes the in-plane conductivity to be ten times that of the through-thickness direction [77]. These refinements would help reduce the observed deviations and significantly enhance the model's accuracy in capturing the transient behaviour, providing more precise temperature predictions across different preform thicknesses.

5. Conclusions

A numerical model was developed to simulate the heat transfer dynamics of the DDPF process, with a particular emphasis on multilayer preform configurations. This model integrates experimentally derived TCC data to predict temperature distributions and assess energy efficiency across different stack thicknesses and material properties. Key parameters considered in the simulation include localised temperature gradients, thermal contact resistance, and material thermal conductivity.

The model showed strong agreement with experimental data, accurately capturing both steady-state and transient thermal behaviours. Results revealed that maintaining uniform temperature profiles becomes progressively more challenging as stack thickness increases, primarily due to variations in thermal contact resistance. While discrepancies were observed during the ramp-up phase due to external convection effects, the overall predictive accuracy and reliability of the model were validated.

Future work should focus on integrating multi-physics phenomena, including air convection, preform thickness changes, and radiative heat transfer, to enhance modelling accuracy. Additionally, efforts should aim to develop adaptive heating systems, optimise energy-efficient processes, and conduct a comprehensive full-scale computational fluid dynamics (CFD) study to improve the reliability and efficiency of multilayer preforming.

Author Contributions: **Srikara Dandangi:** Writing – original draft, Methodology, Software, Investigation, Formal analysis, Data curation, Conceptualization, Visualization, Validation. **Sadegh Ghanei:** Writing – original draft, Formal analysis, Data curation. **Mohammad Ravandi:** Writing – review & editing, formal analysis, Supervision, Methodology, Conceptualization, Project management. **Jamal Naser:** Writing – review & editing , Supervision. **Adriano Di Pietro:** Writing – review & editing, Funding acquisition.

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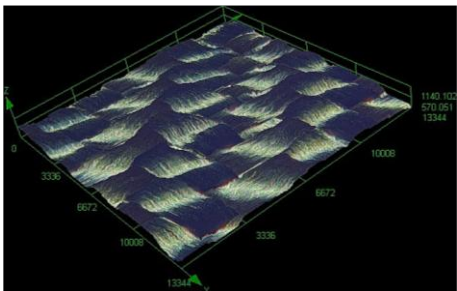
Data Availability Statement: Data will be made available on request.

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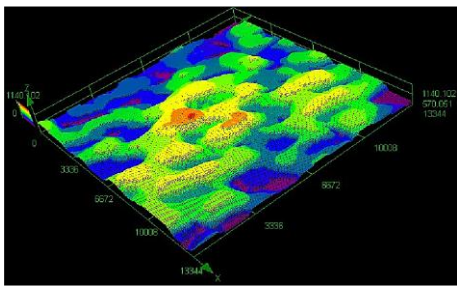
Conflicts of Interest: The authors declare that they have no known competing financial interests or personal relationships that could have appeared to influence the work reported in this paper.

Appendix A

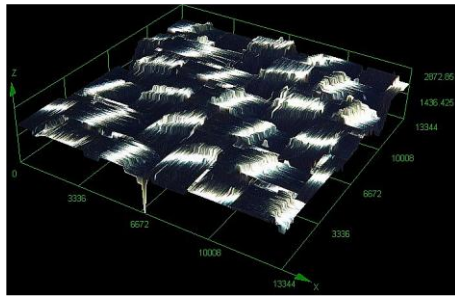
- Optical images and micrographs of coupon model surface asperities following de-compaction at the tool-material interface.



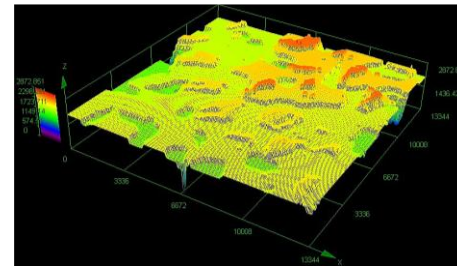
(a)



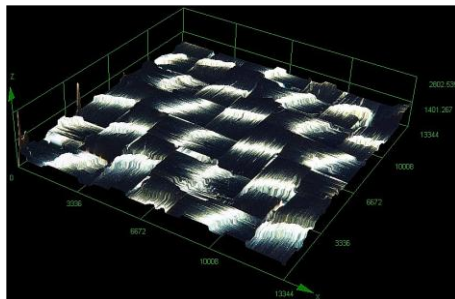
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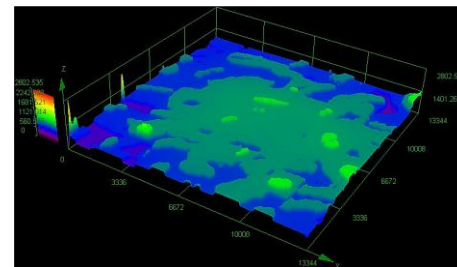
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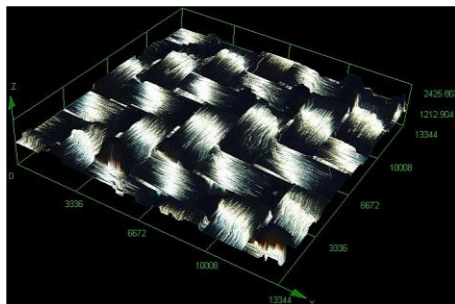
(d)



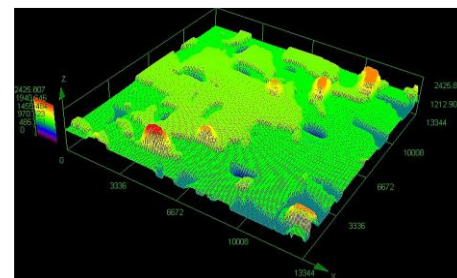
(e)



(f)



(g)



(h)

Figure A1. Optical images and micrographs of coupon model surface asperities following de-compaction at the tool-material interface - 12 Layers – (a) and (b); 24 Layers – (c) and (d); 48 Layers – (e) and (f); 96 Layers - (g) and (h).

Appendix B

• Estimation of convection coefficient in natural heating.

To calculate the convection coefficient in the experimental closed DDPF setup, heat transfer begins as the heating process warms the stack and bagging from bottom. The heated air above the bagging becomes less dense and rises, creating unstable natural convection. The convection coefficient is estimated using surface and air temperatures, governed by fluid dynamics relationships involving the Nusselt, Rayleigh, Grashof, and Prandtl numbers. The convection coefficient is related to the Nusselt number (Nu) through equation 17:

$$Nu = \frac{hL}{k} \quad (17)$$

where, h is convection coefficient, k is thermal conductivity of air and L is characteristic length of the surface. For natural convection, the Nusselt number depends on the Rayleigh number (Ra) as shown in equations 18 and 19.

$$Nu = 0.54Ra^{\frac{1}{4}} \quad \text{for } (10^4 < Ra < 10^7) \quad (18)$$

$$Nu = 0.54Ra^{\frac{1}{3}} \quad \text{for } (10^7 < Ra < 10^{11}) \quad (19)$$

The Rayleigh number is expressed as product of Grashof (Gr), and Prandtl (Pr) numbers which quantify buoyancy forces to viscous forces and ratio of momentum diffusivity to thermal diffusivity respectively as shown in equations 21 and 22:

$$Ra = Gr \times Pr \quad (20)$$

$$Gr = \frac{g\beta\Delta TL^3}{\nu^2} \quad (21)$$

$$Pr = \frac{\nu}{\alpha} \quad (22)$$

Here, g is gravitational acceleration, β is thermal expansion coefficient, ΔT is temperature difference, ν is the kinematic viscosity and α is the thermal diffusivity. In addition, the convection coefficient is also influenced by the surface shape and scale, with the characteristic length varying based on the surface area-to-perimeter ratio for 1D, 2D, or 3D configurations as shown in **Figure 15**. The location of the heating surface (top or bottom) further impacts the convection coefficient by stabilizing or destabilizing airflow, thereby affecting the convective heat transfer behavior.

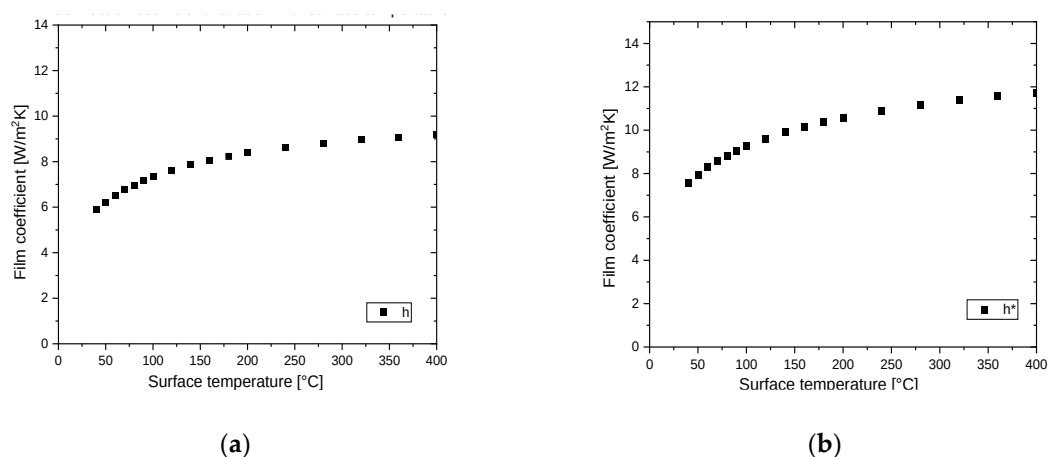


Figure A2. Variation of the convection coefficient with surface temperature for (a) 1D geometry with a characteristic length of 0.1 mm and (b) 3D geometry with a characteristic length of 0.025 mm.

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