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# A THREE-DIMENSIONAL NUMERICAL MODEL FOR THERMOPLASTIC WELDING PROCESS

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***Abstract.*** *Thermoplastic composites offer advantages over thermoset composites, such as recyclability, wear and impact resistance, and weldability. Welding allows for complex geometries and eliminates drawbacks of mechanical fastening and adhesive bonding. Most thermoplastic resistance welding studies rely on experiments, and reliable models are needed for wider applications. In this paper, we propose a three-dimensional numerical model for thermoplastic welding of the APC-2/PEEK composite. The model is based on the 3D temperature distribution around the joint interface provided by the transient heat transfer analysis in the Abaqus FE software. A bonding model that considers intimate contact and autohesion has been used to evaluate the bond strength via USDFLD subroutine. The material, thermal properties and processing parameters were obtained from the literature. The presented model demonstrates the ability to handle more complex and realistic geometries. It serves as a tool for predicting bond quality and allows large parametric studies by varying welding parameters. The model was found to be useful for carrying out parametric studies that can assist in the selection of processing parameters for future experimental tests.*

***Keywords:*** *composite materials, thermoplastic, welding, numerical model.*

## 1. INTRODUCTION

In applications where high strength and lightweight materials are required, carbon fiber polymer composites offer an attractive alternative to aluminum and steel (Velisaris and Seferis, 1988). Carbon fiber-reinforced thermoplastic composites have notable advantages over thermoset composites, including recyclability, wear and impact resistance, and weldability. However, the challenges posed by the high melting point of the resin and fiber constraints limit the production of complex geometries, justifying the use of joining techniques (Reis *et al.*, 2020).

Among the various joining techniques, mechanical fastening methods have certain drawbacks associated with stress concentration, galvanic corrosion, incompatibility of the coefficient of thermal expansion between the fasteners and the composite, increased weight, and delamination during drilling (Stavrov and Bersee, 2005). Adhesive bonding methods require surface preparation, often involve long cure cycles, and face challenges in bonding with the chemically inert thermoplastic matrix (Stavrov and Bersee, 2005). In contrast, welding or fusion bonding of thermoplastic composites (TPC) can mitigate some of these problems because it is possible to achieve bond performance similar to the properties of the joined materials separately, while also allowing for reprocessing (Colak *et al.*, 2002; Xiong *et al.*, 2021).

Resistance welding, also known as resistive implant welding, electrical-resistance fusion, or electrofusion (Xiong *et al.*, 2021; Ageorges and Ye, 2002), is a relatively fast and low-cost joining technique that consists of passing an electric current through a conductive heating element sandwiched between two thermoplastic composite laminates to be welded while applying pressure. The heat generated in the resistive element melts the polymer at the joint interface and follows Joule's law, which is proportional to resistance, current, and time (Stavrov and Bersee, 2005; Xiong *et al.*, 2021).

In industries such as aerospace, as production rates of structures increase, there is a growing need for manufacturing processes that are both time-efficient and cost-effective for high-volume production. Conventional manufacturing methods for composites are typically slow, involving manual ply assembly through hand-layup or the use of preforms, followed by lengthy curing cycles (Jeyakodi, 2016).

While Stavrov and Bersee (2005) mentioned that resistance welding was not yet considered fully mature, more recent research by Xiong *et al.* (2021) suggests that the technology has indeed reached maturity, with resistance welded joints demonstrating superior bonding properties compared to similar welding techniques. This method has already found application in the automotive industry for bonding vehicle bumpers and panels (Ageorges *et al.*, 2001), as well as in the aeronautical industry by Fokker Special Products (Stavrov and Bersee, 2005; Offringa, 1996), including its use in the leading edges of wings for Airbus A340-600 and A380 aircraft (Reis *et al.*, 2020; Gouin O'Shaughnessey *et al.*, 2016).

Certification authorities have considerable expertise and data on the repair of thermoset polymer matrix composite structures. However, the availability of such knowledge is significantly limited when it comes to TPC structures. Overcoming the challenges associated with the scarcity of robust processes and data on TPC materials compared to their thermoset counterparts is critical to the successful integration of TPC materials into aircraft structures (Barroeta Robles *et al.*, 2022). In addition, most of the work on thermoplastic resistance welding is still based on experimental testing (Reis *et al.*, 2020), with processing parameters determined by practical experimentation rather than analytically (Xiao *et al.*, 1992). For certification purposes and subsequent wide aerospace applications, consistent analytical models are required to allow large and robust parametric studies aimed at improving the mechanical performance of welded parts.

There is a scarcity of studies that present robust models for bonding thermoplastic composites using the resistance welding technique, particularly with most of these models being semi-empirical and derived from the tape placement process (Colak *et al.*, 2002; Ageorges and Ye, 2002; Ageorges *et al.*, 1998a,b). In this study, a numerical three-dimensional model is proposed to evaluate the bonding quality for the thermoplastic resistance welding process.

Building on the knowledge gained from previous one-dimensional work (Castro *et al.*, 2022), this study proposes a more robust three-dimensional method using the USDFLD subroutine within the ABAQUS finite element (FE) code. This approach allows for a comprehensive investigation of the phenomena, facilitating a more accurate analysis of complex and realistic geometries, thereby broadening its potential applications.

## 2. NUMERICAL MODEL FORMULATION

The modeling approach employed in this study comprises four distinct components: heat transfer analysis, prediction of the degree of intimate contact ($D_{ic}$), determination of the degree of autohesion ($D_{au}$) (also referred to as the degree of healing in some literature), and evaluation of the degree of bonding ($D_b$). Figure 1 illustrates the resistance welding process proposed to be modeled.

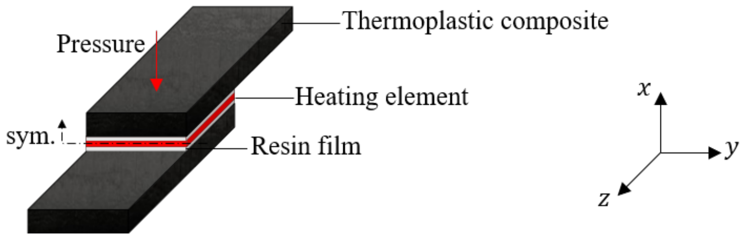


Figure 1. Schematic representation of resistance welding process (Castro *et al.*, 2022).

### 2.1 Heat transfer

The heat transfer model, which is the key component in fusion bonding modeling, provides the temporal temperature distribution. This temperature information is used as input to the subsequent temperature-dependent models (Yassin and Hojjati, 2018).

Equation 1 represents the general heat equation for the three-dimensional case, specifically applicable to orthotropic materials, such as composites (Çengel and Ghajar, 2014; Reis *et al.*, 2020). The solution was obtained using the Abaqus FE software.

$$\frac{\partial}{\partial x}\left(k_x\left(\frac{\partial T}{\partial x}\right)\right)+\frac{\partial}{\partial y}\left(k_y\left(\frac{\partial T}{\partial y}\right)\right)+\frac{\partial}{\partial z}\left(k_z\left(\frac{\partial T}{\partial z}\right)\right)+\dot{q}=\rho c_p\frac{\partial T}{\partial t} \tag{1}$$

where $k$ is the thermal conductivity, $\dot{q}$ is the heat generation rate per unit volume, $\rho$ is the density, and $c_p$ is the specific heat.

The heat generation rate generated within the heating element was considered the mean value of the power input values presented in the review works of Reis *et al.* (2020) and Ageorges *et al.* (2001) properly converted to heat generation rate per unit volume. Fiber and matrix properties for the APC-2/PEEK can be found in the works from Velisaris and Seferis (1988) and Manson *et al.* (1990), Tab. 1. Given the fiber volume and mass fraction, the rule of mixtures, a micromechanical approach, was used to compute the laminate properties, Eq. (2, 3, 4) (Holmes and Gillespie, 1993; Velisaris and Seferis, 1988).

$$\rho=\rho_f V_f+\rho_m V_m \tag{2}$$

$$c_p=c_{pf}X_f+c_{pm}X_m \tag{3}$$

$$1/k=V_f/k_f+V_m/k_m \tag{4}$$

where $\rho$ is the density, $V$ is the volume fraction, $X$ is the mass fraction, $c_p$ is the specific heat, and $k$ is the thermal conductivity. The subscript $f$ refers to the fiber, while the subscript $m$ refers to the matrix.

### 2.2 Intimate contact

The intimate contact model determines the degree of physical contact between the two surfaces. It refers to the temporal evolution of the contact area at the interface (Butler *et al.*, 1998). The concept of intimate contact, originally introduced by Loos and Dara (1987), aimed to replicate the surface irregularities of composites by approximating them with a summation of non-uniform rectangles. This approach was further simplified by Woo Il Lee and Springer (1987), who considered a series of equal-sized rectangles based on the previous model. Mantell and Springer (1992) used the conservation of mass principle and assumed laminar flow to quantify the flow occurring at the healing interface, allowing the model to be applied to tape laying and filament winding processes. Subsequently, the model was found to be suitable for resistance welding and fusion bonding due to the common characteristics of these processes, such as their non-isothermal nature (Colak *et al.*, 2002; Ageorges *et al.*, 1998a; Schell *et al.*, 2009).

The degree of intimate contact represents the extent of surface interaction at the interface (Fig. 2b) and is influenced by factors such as surface roughness, temperature, and pressure (Pitchumani *et al.*, 1996). Surface roughness restricts molecular movement across the interface due to the presence of voids. Given the high viscosity of thermoplastics, achieving proper resin flow requires the application of pressure in conjunction with temperature (Sonmez and Hahn, 1997). In this study, the degree of intimate contact is quantified using Eq. (5), which is based on a semi-empirical formulation proposed by Mantell and Springer (1992). This formulation has been widely used in subsequent works (Reis *et al.*, 2020; Colak *et al.*, 2002; Ageorges *et al.*, 2001, 1998a; Schell *et al.*, 2009; Pitchumani *et al.*, 1996; Sonmez and Hahn, 1997; Behrens *et al.*, 2021; Butler *et al.*, 1998; Stokes-Griffin and Compston, 2016).

$$D_{ic}(t) = a^* \left[ \int_0^t \frac{P_{app}}{\mu_{mf}} dt \right]^{\frac{1}{5}} \tag{5}$$

where $P_{app}$ is the applied pressure, $a^*$ is the roughness or geometric parameter determined empirically by fitting the model to experimental data (Colak *et al.*, 2002; Sonmez and Hahn, 1997), and $\mu_{mf}$ is the temperature-dependent matrix-fiber viscosity for the composite matrix-fiber system (Mantell and Springer, 1992). The degree of intimate contact ranges from 0 to 1, with a value of $D_{ic} = 1$ indicating complete contact between the surfaces.

The viscosity model used for the APC-2/PEEK matrix-fiber system in this work, as well as in most subsequent studies, is based on the formulation proposed by Mantell and Springer (1992), Eq. (6).

$$\mu_{mf} = 132.95 \cdot e^{\left(\frac{2969}{T}\right)} \tag{6}$$

### 2.3 Autohesion

Autohesion, also known as healing or self-adhesion, is a mechanism associated with the diffusion of polymer chains across the interface of two polymeric surfaces in contact (Fig. 2c). The initial studies that aimed to explain this mechanism originated from de Gennes' theory of reptation, first applied to amorphous polymers under isothermal conditions (de Gennes, 1971). Subsequent investigations expanded upon this theory, considering additional conditions such as non-isothermal processing (Sonmez and Hahn, 1997; Bastien and Gillespie, 1991; Yang and Pitchumani, 2002).

In order for autohesion to occur, intimate contact is required. The physical obstacles between the two surfaces diminish, allowing the polymer molecules to diffuse freely across the interface. This phenomenon plays a critical role in the progressive strengthening of the bond region. In addition, the temperature must be raised above the glass transition temperature ($T_g$) for amorphous polymers and above the melting temperature ($T_m$) for semicrystalline polymers to overcome the resistance imposed by the crystalline phases (Yassin and Hojjati, 2018; Colak *et al.*, 2002; Tierney and Gillespie, 2006; Pitchumani *et al.*, 1996; Stokes-Griffin and Compston, 2016).

After incorporating the autohesion models proposed by Bastien and Gillespie (1991) in earlier works (Pitchumani *et al.*, 1996, 1997), Yang and Pitchumani developed an improved model to accurately calculate the degree of autohesion in non-isothermal processes (Yang and Pitchumani, 2002, 2003). This model demonstrated robustness and addressed limitations observed in the previous models of Bastien and Gillespie (1991) and (Sonmez and Hahn, 1997), which were mainly applicable to low molecular weights. However, for engineering semicrystalline thermoplastics such as PEEK, higher molecular weights are involved (Wool and O'Connor, 1981; Wool *et al.*, 1989; Yang and Pitchumani, 2002).

The model proposed by Yang and Pitchumani (2002, 2003), as represented by Eq. (7), has been noted in recent studies (Tierney and Gillespie, 2006; Stokes-Griffin and Compston, 2016; Liu *et al.*, 2022; Khan and Schledjewski, 2009; Khan *et al.*, 2010; Martin *et al.*, 2020). Considering the non-isothermal nature of resistance welding (Ageorges *et al.*, 1998a; Bastien and Gillespie, 1991), the appropriateness of selecting the Yang and Pitchumani (2002, 2003) model as the autohesion model for this study was acknowledged.

$$D_{au}(t) = \left[ \int_0^t \frac{1}{t_w(t)} dt \right]^{\frac{1}{4}} \tag{7}$$

The welding time $t_w$ is defined as the duration required to achieve maximum bond strength (Yang and Pitchumani, 2003). For this study, the formulation proposed by Khan and Schledjewski (2009); Khan *et al.* (2010), as represented by Eq. (8), was adopted. The variable $T$ represents the temperature at time $t$, while $R$ corresponds to the universal gas constant.

$$t_w = 2 \cdot 10^{-5} \cdot e^{\left(\frac{43000}{RT}\right)} \tag{8}$$

## 2.4 Degree of bonding

The bonding process is a coupled phenomenon in which both intimate contact and autohesion occur simultaneously. It is defined by the mutual evolution of the degrees of autohesion and intimate contact (Bourban *et al.*, 2001). Figure 2 illustrates the molecular dynamics of polymer chains at the interface during the processes of intimate contact and autohesion.

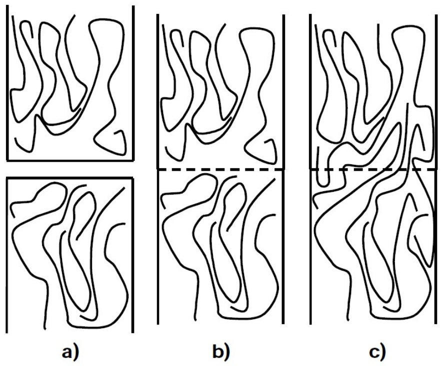


Figure 2. Healing of a polymeric interface. (a) Two distinct interfaces; (b) intimate contact; (c) autohesion (Prager and Tirrell, 1981).

The formulation for the degree of bonding was initially introduced by Woo Il Lee and Springer (1987) and further developed by Mantell and Springer (1992). The calculation of $D_b$ can be performed using the formula provided in Eq. (9). This formulation has been referenced in several works (Reis *et al.*, 2020; Pitchumani *et al.*, 1996, 1997; Mantell and Springer, 1992; Bourban *et al.*, 2001; Ageorges *et al.*, 1998a; Behrens *et al.*, 2021; Khan and Schledjewski, 2009; Khan *et al.*, 2010; Yassin and Hojjati, 2018; Jeyakodi, 2016). The consolidation-healing process is considered complete when the degree of bonding reaches unity ($D_b = 1$), varying between 0 (no bonding) and 1 (complete bonding).

$$D_b(t) = D_{ic}(t) \cdot D_{au}(t) \tag{9}$$

The degree of bonding is related to the strength of the interfacial bond and exhibits a proportional relationship with lap shear strength (Liu *et al.*, 2022). The degree of bonding is dependent on the processing temperature, contact pressure, and time.

Different models for thermoplastic fusion bonding can be found in the literature, and significant variations can be observed among them. The variability is particularly evident in aspects such as the matrix-fiber viscosity models ($\mu_{mf}$) and the models used to determine the welding time ($t_w$) (Martin *et al.*, 2020; Stokes-Griffin and Compston, 2016). These differences highlight the diversity of approaches and considerations that researchers have adopted in the study of thermoplastic fusion bonding, including the empirical nature of these models.

## 2.5 FE model setup

The heat transfer model in Abaqus consisted of two steps: step 1 represented the heating phase and step 2 represented the cooling phase. Each step had a duration of 45 s. In other words, the heating element was activated for 45 s, followed by the cooling period with no heat input ($\dot{q} = 0$) for another 45 s, emulating the resistance welding process. The input parameters used in the simulation are shown in Tab. 1.

According to Colak *et al.* (2002) and Sonmez and Hahn (1997), autohesion calculations are not applicable for temperatures below 270 °C due to limited molecular mobility, resulting in a very slow bonding process. As a more conservative approach, Pitchumani *et al.* (1996) considered this temperature threshold to be 280 °C. In this study, the autohesion process was assumed to initiate at the melting point ($T_m$ = 338 °C) as reported by Stokes-Griffin and Compston (2016) and Pitchumani *et al.* (1996), and terminate at 280 °C. Since the two processes are coupled, the calculation of the degree of bonding begins and ends with the autohesion. The autohesion mechanism is temperature and time-dependent and is complete when $D_{au} = 1$. According to Bourban *et al.* (2001) and Sonmez and Hahn (1997), autohesion is achieved at a faster rate in fusion bonding processes compared to intimate contact.

Table 1. Input parameters.

| Property | Symbol | Value | Unit |
|---|---|---|---|
| Joint thickness | $L$ | 3.859 | $mm$ |
| Heating element thickness | $h_{he}$ | 0.1206 | $mm$ |
| Resin film thickness | $h_{rf}$ | 0.0804 | $mm$ |
| Ambient temperature | $T_\infty$ | 25 | $^\circ C$ |
| Heat generation rate | $\dot{q}$ | $7.877 \cdot 10^8$ | $W/m^3$ |
| Convection heat transfer coefficient | $h$ | 10 | $W/m^{2\circ}C$ |
| Carbon fiber mass fraction | $X_f$ | 68 | $\%$ |
| Carbon fiber volume fraction | $V_f$ | 62 | $\%$ |
| Matrix density | $\rho_m$ | 1262.6 | $kg/m^3$ |
| Fiber density | $\rho_f$ | 1790 | $kg/m^3$ |
| Matrix specific heat | $c_{pm}$ | 1339.8 | $J/kg \cdot K$ |
| Fiber specific heat | $c_{pf}$ | 1256 | $J/kg \cdot K$ |
| Matrix thermal conductivity | $k_m$ | 0.25121 | $W/m \cdot K$ |
| Fiber thermal conductivity | $k_f$ | 0.42705 | $W/m \cdot K$ |
| Pressure | $P_{app}$ | 1.4 | $MPa$ |
| Roughness parameter | $a^*$ | 0.147 | - |
| Universal gas constant | $R$ | 8.3145 | $J/mol \cdot K$ |
| Matrix melting point | $T_m$ | 338 | $^\circ C$ |

The dimensions of the model were based on the ASTM D1002 standard, with a width of 25.4 mm, a length of 101.4 mm (each), and an overlap of 12.7 mm. The thickness of the laminate $L$ was 3.859 mm, while the heating element had a thickness $h_{he}$ of 0.1206 mm and the resin film had a thickness $h_{rf}$ of 0.0804 mm. See Fig. 3 for a visual reference.

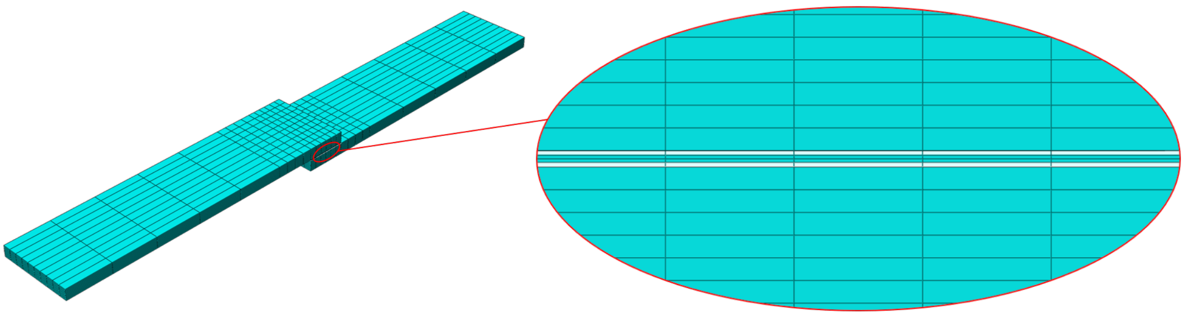

Figure 3. 3D mesh of the model created in Abaqus.

The analysis performed was a heat transfer standard analysis using the DC3D8 mesh element, which is an 8-node linear heat transfer brick from the Heat Transfer family. Along the thickness ($x$ direction, see Fig. 1), each laminate has 10 elements, the heating element has 2 elements, and the resin film (two layers in white) has 1 element. In total, the model has 3240 elements and 4411 nodes. The element library used was Standard and the geometric order was set to Linear. The analysis consisted of two steps of 45 s each: heating and cooling. The thermal boundary conditions are defined by convection (surface film condition) $h$ (Cogswell, 1992) due to ambient air temperature at the outer top and bottom surfaces. The initial temperature of the joint is assumed to be equal to the ambient temperature $T_\infty$.

The influence of edge effects in the width direction is neglected in this study due to the assumption that the specimen is obtained by cutting it from a plate, enabling the application of constant conditions as in Nagel *et al.* (2021). The heat generated during welding is implemented as a body heat flux $\dot{q}$ within the heating element, assuming a uniform distribution over the overlap area. The heating element was modeled as a layer of APC-2/PEEK (polyetheretherketone carbon fiber reinforced composite), following a similar approach to the works of Colak *et al.* (2002), Ageorges *et al.* (1998b), and others (Don *et al.*, 1990; Holmes and Gillespie, 1993). A PEEK resin film was used as an interlayer between the heating element and the composite to facilitate the formation of a resin-rich region, thereby improving the bonding process (Colak *et al.*, 2002; Reis *et al.*, 2020; Holmes and Gillespie, 1993; Jahromi, 2019). The pressure required for consolidation is derived from the intimate contact model and is assumed to be constant in both the longitudinal and transverse directions applied directly to the specimen.

In Abaqus software, performing a simulation using a subroutine, which is in Fortran language, allows you to incorporate custom user-defined functionality into the simulation process. By utilizing a user subroutine, you have the flexibility to customize and extend the capabilities of Abaqus to suit your specific simulation requirements. This includes implementing complex constitutive models, non-linear material behavior, user-defined contact algorithms, or specialized solution procedures.

In this study, a subroutine was employed to incorporate the temperature-dependent models for the degree of intimate contact, the degree of autohesion, and the degree of bonding. This subroutine was integrated into the simulation workflow and executed during the analysis process. Specifically, the USDFLD (user subroutine to redefine field variables at a material point) subroutine was used. In addition, the GETVRM utility routine was called to access material point data, specifically temperature. The solutions of the integrals Eq. (5) and Eq. (7) were obtained by the trapezoidal rule, from the Newton-Cotes numerical integration formulas.

## 3. RESULTS AND DISCUSSIONS

From the model input parameters (Section 2.5), the temperature distribution at $t = 45$ s, which represents the end of the heating phase, is shown in Fig. 4. The color scale represents the temperature values in degrees Celsius.

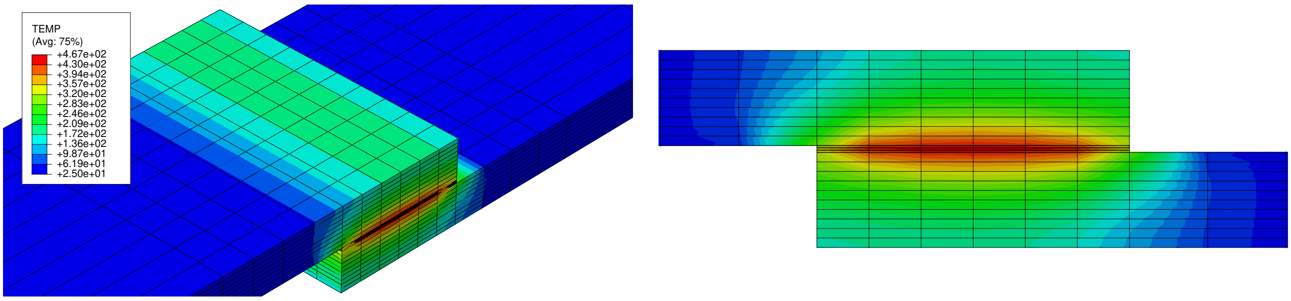


Figure 4. Temperature distribution at the end of the heating phase (t = 45 s).

The temperature at the interface between the resin film and the laminate, which is slightly lower than the center of the heating element, reaches $T_m = 338$ °C in 26.2 s. It is noteworthy that the maximum temperature recorded at the center of the interface exceeds the melting point of PEEK by 129.4 °C reaching 467.4 °C. According to Bourban *et al.* (2001), to achieve effective bonds, semi-crystalline polymers should be processed above their melting temperatures.

The temperature distribution at the end of the simulated cooling process (step 2) at $t = 90$ s is shown in Fig. 5.

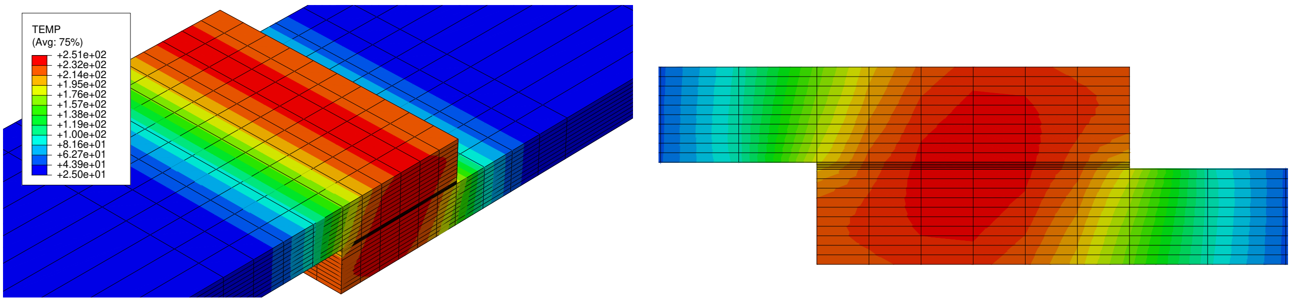


Figure 5. Temperature distribution at the end of the cooling phase (t = 90 s).

Compared to Fig. 4, it can be observed that during the cooling process ($\dot{q} = 0$), the temperature decreases in the center while increasing towards the edges of the specimen due to heat conduction. This temperature gradient continues until equilibrium is reached, at which point the ongoing effect of convection gradually cools down the entire sample. The temperature distribution along the thickness was in agreement with the results obtained in the work of Castro *et al.* (2022), which used the same model input parameters, although in a one-dimensional approach. Heat transfer occurs primarily by conduction from the heated element to the cooler parts, exceeding the rates of convection and radiation (Colak *et al.*, 2002; Xiao *et al.*, 1992).

The degree of bonding results, obtained from the temperature distributions calculated using the USDFLD subroutine, are represented by the solution-dependent state variable SDV4 in the legend and are visualized in Fig. 6 below. Under the selected processing parameters in this study, a maximum degree of bonding of 0.82 was achieved. The color mapping in the figure indicates the spatial variation of the degree of bonding, with different colors indicating different bonding levels, where each colored element experienced a maximum temperature of at least 338 °C (start of autohesion) and a minimum temperature of 280 °C (end of autohesion).

In order to show the capabilities of the model toward parametric investigations, the heating and cooling time was increased to $t = 60$ s and the pressure to $P_{app} = 2$ MPa. The new degree of bonding results can be seen in Fig. 7.

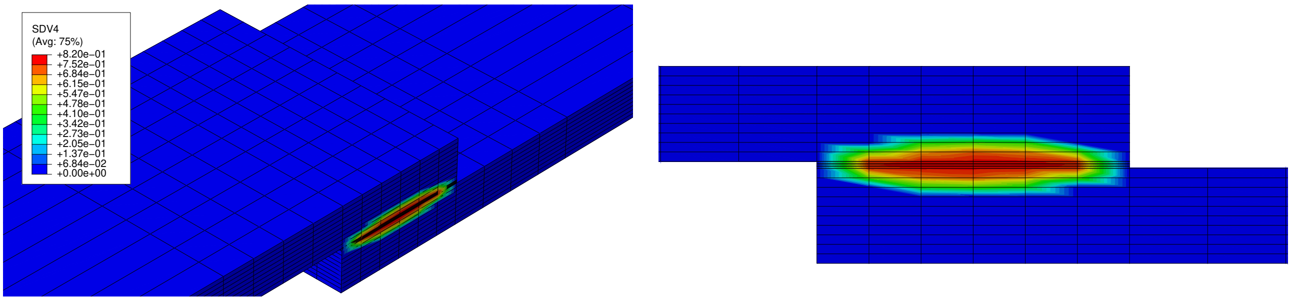


Figure 6. Degree of bonding (SDV4) at the end of the cooling phase (t = 90 s).

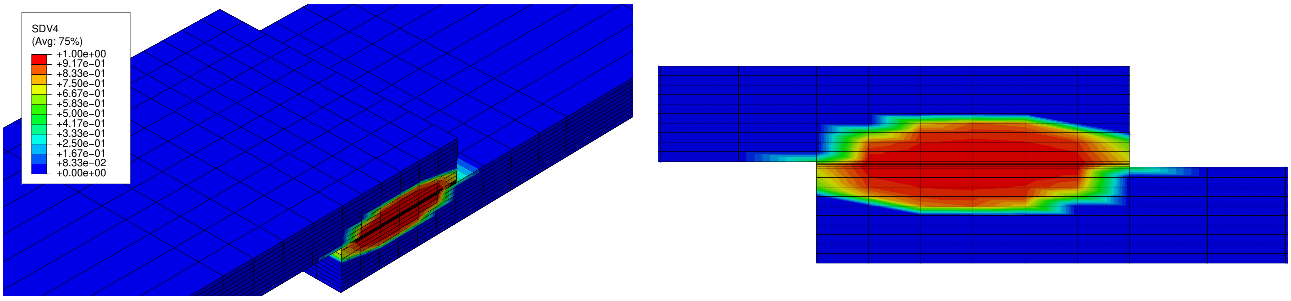


Figure 7. Degree of bonding (SDV4) at the end of the cooling phase (t = 120 s and $P_{app}$= 2 MPa).

As the heating time was increased to 60 s, the temperatures achieved on the joint significantly rose. Consequently, the degree of bonding also increased, resulting in superior bonding quality. A maximum degree of bonding of 1.0 was achieved, indicating complete bonding. The maximum temperature reached at the end of the heating phase was 547 °C. It is worth noting that Ageorges *et al.* (1998b) reported the degradation temperature of PEEK to be 550°C for short residence times.

For the first case studied, increasing the pressure to 3.8 MPa would result in a degree of bonding of 1.0, as shown in Fig. 8 below. There was no change in the temperature distribution since all parameters except the applied pressure were kept the same, so the color mapping for the degree of bonding is also the same as in Fig. 6, with the change only in the magnitude of $D_b$.

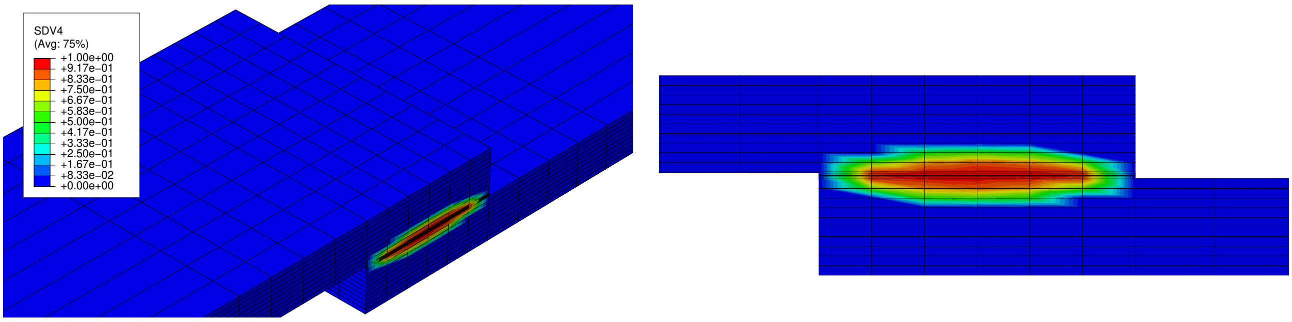


Figure 8. Degree of bonding (SDV4) at the end of the cooling phase (t = 90 s and $P_{app}$= 3.8 MPa).

## 4. CONCLUSIONS

In this study, a three-dimensional model for evaluating the bonding quality in a thermoplastic resistance welding process was carried out. The heat transfer model from Abaqus provided the temperature distribution along the composite for the degree of intimate contact, degree of autohesion and degree of bonding calculations via the USDFLD subroutine.

The processing parameters selected for this study, including power input, heating time, and pressure, were relevant and reliable for the resistance welding process. In addition, these parameters were carefully selected to match the practical conditions observed in practice (Ageorges *et al.*, 2001).

The proposed model allows large parametric studies by varying variables such as pressure, heating rate, heating time, heating element and laminate thickness in order to define the optimum welding parameters for a given composite joint configuration.

Besides the model limitations and simplifications, the numerical predictions in this study, although not directly validated by experiments, can provide a framework for the experimental data. The numerical model complements the experimental methodology and serves as a useful tool for determining appropriate processing parameters in the experimental tests, thereby providing a more comprehensive understanding compared to relying solely on experimental results.

The proposed model will be enhanced to incorporate the applied pressure from Abaqus through a more comprehensive coupled-temperature displacement analysis. Since there is a lack of sufficient experimental data in the existing literature, no direct comparisons between experimental results and model predictions were provided in this study. However, an ongoing experimental program is being conducted at LNCA-ITA to validate and refine the proposed model formulation, and comparisons between model predictions and experimental results will be presented in a subsequent paper.

## 5. ACKNOWLEDGEMENTS

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