A fully coupled local and global optical-thermal model for continuous adjacent laser-assisted tape winding process of type-IV pressure vessels

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Abstract
A numerical process simulation framework is introduced in this paper to describe and predict the process temperature evolution during the laser-assisted tape winding (LATW) process of a type-IV pressure vessel made of glass-reinforced high-density polyethylene (G/HDPE). A local optical-thermal model is fully coupled with a global thermal model for the simulation of continuous adjacent hoop winding cases. The predicted tape and substrate temperatures are compared with the experimental data to validate the process model's effectiveness. The inline temperature was measured by an infrared thermographic camera during the continuous winding. The continuous process temperature of the substrate is affected significantly due to the previously wound layers including the pressure vessel, and a gradual increase of the temperature of the roller and the air inside the liner. A considerable temperature increase calculated as 80–120 °C takes place for the substrate during winding of two consecutive layers of (G/HDPE) prepreg tape at the liner ends. The influence of pressure vessel size on the tape and substrate temperatures is investigated for different liner radii using the validated process model. The peak substrate temperature is found to increase approximately 45 °C by reducing the radius of the pressure vessel from 272 mm to 68 mm while maintaining all other process conditions constant.

Keywords
Laser-assisted tape winding (LATW), thermoplastic composites, numerical process model, pressure vessel type-IV, temperature analysis

Introduction
Laser assisted tape winding (LATW) is an automated and efficient method to manufacture pressure vessels out of fiber reinforced thermoplastic composites (FRTPCs) for the storage of hydrogen or compressed natural gas (CNG). The in-situ consolidation of the FRTPCs during the LATW process provides a considerable time saving without an additional consolidation step. Similar to laser-assisted tape placement (LATP) processes, the application of a laser as heat source implies some possible advantages including higher accuracy, repeatability, quality and reduced cycle time compared to conventional methods using hot air, gas or flame. The incoming fiber reinforced thermoplastic prepreg tape is bonded to a liner or an already placed substrate by means of a laser heating and compaction roller during the LATW processes. A schematic view of a LATW process and a picture from a hoop winding process as an example are shown in Figure 1. The incoming prepreg tape and substrate come into contact at the so-called nip point. A proper consolidation of the tape and substrate at the nip point requires optimum process settings to keep the consolidation pressure, time and temperature at the nip point at desired levels which is a challenging task in the LATW processes. The inherent variations in geometry and material properties makes the process difficult to control often leading to deviations of the desired temperature at the nip point. In addition, non-constant...
process temperatures are present during continuous LATW of tape layers on top of each other which was studied extensively in literature\textsuperscript{13} for manufacturing of type-IV pressure vessels made of continuous glass fiber reinforced high density polyethylene (G/HDPE). It was shown that the local heating and cooling cycles during the LATW process significantly affected the process temperature. The characteristics of the tape and substrate temperature developments near the nip point for the continuous LATW process reported in literature\textsuperscript{13} are summarized in Figure 2. The previously deposited layers’ temperature as well as the heat accumulation during the transition from one round to another round in a continuous LATW process had an influence on the tape and substrate nip point temperatures.

In order to tackle the challenges mentioned above and control the processing temperature in continuous LATW processes, computational process models are needed next to experimental observations to describe and predict the physical phenomena taking place during the manufacturing process. Several studies were conducted in literature to understand the relation between the heat input and the resulting temperature distribution in the tape, the liner/substrate and the compaction roller for thermoplastic automated tape placement (TP-ATP) processes.\textsuperscript{14-21} In those works a uniform heat flux was considered as a boundary condition in the thermal model for the tape and substrate temperature predictions. An analytical thermal model was used in literature\textsuperscript{14} for the laser assisted tape placement (LATP) process to correlate the tape placement speed with the through-thickness temperature distribution in the substrate. The roller temperature was reported to have the most significant impact on the nip point temperature and incoming tape temperature distribution in literature.\textsuperscript{15} Thermal models were coupled with crystallization models for the LATP in literature.\textsuperscript{18,19} The sensitivity of the temperature with respect to the placement speed and laser power was investigated in literature\textsuperscript{17} for tape laying of continuous carbon fiber reinforced PEEK laminates. The temperature distribution along the length and through the thickness of the flat composite laminate during a tape laying process was predicted in literature.\textsuperscript{16} The tape lay-up processing speed, nozzle exit temperature, and cooling rate were found to be the major process variables. The effects of preheating the consolidated flat laminate, temperature distributions and thermal histories were investigated for varying consolidation speeds and the overall feasibility of the proposed process was discussed in literature.\textsuperscript{21}

Further studies employed a combined optical-thermal model for the LATP process in literature.\textsuperscript{15,22} A ray tracing approach was implemented to estimate the heat flux distribution on the tape and substrate. It was shown in literature\textsuperscript{15,22} that there was a shadow region which caused a significant temperature drop immediately prior to the nip point. More advanced heat flux description was introduced in literature\textsuperscript{23,24} by incorporating non-specular laser reflections from the tape, substrate and roller in an optical process model. The scattering reflectance behavior of a unidirectional thermoplastic prepreg tape was formulated in literature\textsuperscript{23} through a developed bidirectional reflectance distribution function (BRDF) model for LATW and LATP processes. A micro half cylinder (MHC) approach was presented in literature\textsuperscript{24} to simulate the scattering of laser irradiance via a commercial software (OptiCAD10) for a flat substrate in the LATP process. Both approaches enabled capturing the shadow regions of the tape and substrate prior to the nip point where the input laser heat flux cannot be received. The same optical model reported in literature\textsuperscript{24} was used in literature\textsuperscript{25} to include the laser power distribution in a two-
dimensional (2D) thermal analysis performed by a commercial finite element package Ansys. The tape and substrate temperature distributions were studied comprehensively.

Next to the numerical modeling studies focusing on the LATP processes on flat tooling as in literature, optical-thermal models for curved coolings were developed in literature by the current authors. The laser radiation on circular surfaces was investigated in literature based on a three-dimensional (3D) ray tracing optical model combined with a 1D through-thickness thermal model. The effect of non-specular reflections on the process temperature was studied for different laser power distributions. A 3D optical model was presented in literature to consider the effect of out-of-plane winding on the substrate and tape temperatures in the LATW process. The effect of different winding orientations on the nip point temperature was studied in literature for curved geometries.

The mentioned combined optical-thermal process models achieved a realistic temperature prediction, however, they only dealt with modeling discontinuous winding/placement on either flat or curved geometries. The lack of in-depth numerical optical-thermal models for continuous LATW processes on curved geometries still leads to unwanted temperature variations as shown in Figure 2. One of the most important aspects not covered comprehensively by the current models is the influence of the previously deposited layer’s temperature on the nip point temperature when a new layer is deposited during continuous LAWT/LATP processes.

The objective of the present work is to critically analyze the temperature evolution near the nip point during continuous LATW processes by means of a comprehensive computational model. The focus is on the process modeling of adjacent hoop winding of a type-IV pressure vessel made of G/HDPE composite. A fully or two-way coupled local and global process models is introduced for the process simulation of continuous LATW processes. More specifically, a local optical-thermal model is combined with a global thermal model to incorporate the global cooling and local heat accumulation as described in Figure 2. The conducted experiments of three consecutive rounds of adjacent hoop winding from current authors in literature are used as the basis of this paper for validating the developed process model. After validating the model with the measured tape and substrate temperature distributions, the effect of liner radius size on the peak temperature and gradual heating/cooling behavior is addressed by the developed process model for continuous LATW processes. The performed experiments by current authors are briefly discussed in Section “Experimental work”. The local optical-thermal and global thermal models are presented in Section “Modeling of continuous LATW process”. The local optical-thermal and global thermal models are part of the comprehensive OTOM (Optical Thermal Optimization Model) simulation tool developed by using MATLAB at the University of Twente for the simulation of the LATW/LATP processes. The optical model includes a generic 3D ray tracing model to simulate the laser intensity distribution on the tape and substrate surfaces. The local thermal model comprises a 2D/3D fully implicit transient advection-diffusion thermal model to calculate the temperature distribution of the tape and substrate near the nip point. The global thermal model employs a 2D heat conduction model to calculate the temperature of the pressure vessel during the cooling period, i.e. non-irradiated locations. The studied case studies are presented in Section “Process model parameters and case studies”. The obtained results are discussed in Section “Results and discussion”. The final part of the paper presents the conclusions and recommendations for further research in Section “Conclusions and outlook”.

Figure 2. The trend of the substrate (left) and tape (right) temperature developments during continuous LATW process of type IV pressure vessels with three rounds of adjacent hoop layers. Round-1: placing tape on a pure thermoplastic liner. Round-2 and 3: placing tape on the already wound layers. The peaks in substrate temperature are due to the heat accumulation during winding on a previously deposited round. The gradual heating is due to an increase in roller and inner air temperature.
Experimental work

The continuous adjacent hoop winding of pressure vessels experimentally studied by current authors in literature13 was the basis of the present process modeling. The summarized process of adjacent hoop winding of the pressure vessel is depicted in Figure 3 from the neat thermoplastic liner to adjacent hoop winding procedure and then finished adjacent hoop winding (left to right). The prepreg tapes were made of G/HDPE with 47% fiber volume content and the liner was made of pure HDPE with pigments. A schematic view of the continuous LATW process for winding of three rounds is depicted in Figure 4. The length ($L_L$) and radius ($R_L$) of the cylindrical part of the liner were 640 mm and 136 mm, respectively. The winding angle ($\theta$) was set to 88.2° to obtain proper layers adjacently wound. Total of 22 layers were adjacently wound for each round, i.e. Round-1, Round-2 and Round-3 as seen in Figure 4. The winding of a single layer took approximately 6.2 s corresponding to approximately 135 s for each round. The layers of Round-1 were started and ended at point O1 and O2, respectively, as illustrated schematically in Figure 4. A deformable roller was used to consolidate the incoming prepreg tape with the substrate locally. An infrared thermographic camera was utilized to measure the temperature on the tape and substrate surfaces during the continuous hoop winding process. Further details can be found in literature.13

Modeling of continuous LATW process

A local optical-thermal process model domain was developed near the nip point to predict the substrate and tape temperatures. On the other hand, a global thermal model was employed to predict the cooling of the substrate after the nip point as well as the heating of the roller and the air inside the pressure vessel. The process simulation of the continuous adjacent hoop winding of FRTPCs therefore followed a fully coupled local optical-thermal and global thermal modeling procedure in the present work. The flowchart of the process modeling framework is presented in Figure 5. The corresponding equations are also represented in this figure to clarify flow of the implemented method. The laser irradiation was calculated by the optical model and the obtained heat flux distribution was transferred to the local thermal model. The heat loss in the substrate after the nip point was calculated using the global thermal model and the updated substrate temperature was used as a boundary condition (BC) in the local thermal model. Similarly, the time dependent temperatures of the roller and air inside the vessel were updated in the global thermal model and transferred to the local thermal model as a BC. This coupling was done when the local thermal domain passed the calculation points defined for each layer as shown in Figure 4. This procedure was applied for three rounds of adjacent hoop winding with a total process time of approximately 405 s. The details of the optical, local and global thermal models are presented in the following.

Optical model

The optical model compromises the liner, roller, incoming prepreg tape and substrate which were defined in a 3D global coordinate system as a reference. Each object in the global axis system had its own local coordinate system. A schematic view of the 3D optical model domain is seen in Figure 6. The deformable roller was assumed to have a contact with the liner at the line N1–N2 and C1–C2, respectively. N1–N2 was defined as the nip line. The tape-roller contact was defined by the angle $k$. A 3D ray tracing approach was implemented in the optical model as indicated in Figure 7(a). Here, $I_i$ was the $i^{th}$ incoming ray defined as a 3D line, $P_i(X, Y, Z)$ was the intersection point of $I_i$ with substrate, $n_i$ was the surface normal, $\beta_i$ was the incident angle, $r_i$ was the 3D reflected ray and $P_{i\text{ref}}(X, Y, Z)$ was the intersection point of $r_i$ with the tape. The defined incoming rays can collide with multiple surfaces, e.g. tape (indicated as ray-a in Figure 7(a)), roller (ray-b) and substrate in the optical model. A top-hat laser power distribution with a divergence angle of $\gamma$ as seen in Figure 7(b) was considered. The laser divergence was considered as 4 degrees in the simulations.13,30 A 3D representation of the
corresponding reflections from the defined objects is shown in Figure 7(c). The incoming rays were assumed to reflect specularly in the current model to prevent high computational cost.

The procedure used on the optical model started with irradiating diverged rays from the laser cross-sectional plane with dimensions $H_L \times W_L$. Based on the preliminary convergence study, the total number of rays used in the optical model was 9600 (120 along the length and 80 along the height of the laser source plane). The rays were distributed uniformly along the length and width directions of the laser plane aligned with the laser optic direction as shown in Figure 7(c). The intersection points of each incoming/reflected rays defined as a 3D line with the objects (liner, roller, tape and substrate), e.g. $P_i(X, Y, Z)\text{ were calculated analytically as described by Langford in literature.}$

According to the intersection point, the surface normal $n_i$ and the incident angle $\beta_i$ of the $i^{th}$ ray were determined. The 3D reflected ray $r_i$ was obtained using the expression in equation (1)

$$r_i = 2(I_i \cdot n_i)n_i - I_i$$  

Similarly, the intersection point of $r_i$ with another parametric surface, i.e. $P_{ref}(X, Y, Z)$, together with $n_{ref}$ and $\beta_{ref}$ were calculated. The irradiation power distributions on the objects were calculated based on the obtained intersection points of rays/reflections with objects via the ray tracing approach. The irradiation power distribution of the $i^{th}$ ray $\Phi_i$, was calculated by dividing the total laser power $P_{laser}$ by the total number of rays $N_{ray}$. The conservation of the energy was maintained by considering

$$\Phi_i = \phi_{ia} + \phi_{ir}$$

Figure 4. A schematic view of the continuous LATW process for winding of three rounds consisting of 22 adjacent layers. The unfolded representation of the cylindrical shaped liner is shown on the right.

Figure 5. The flowchart of the coupled local optical-thermal and global thermal modeling approach for the process simulation of continuous LATW of FRTPCs. The corresponding equations are also shown.
where \( \phi_{ia} \) and \( \phi_{ir} \) were the the absorbed and reflected energy at the intersection point \( P_i(X, Y, Z) \), respectively. Here, the transmitted energy was neglected similar to conducted analyses in literature\(^{23,32} \) \( \phi_{ia} \) was calculated using the following expression

\[
\phi_{ia} = \Phi_i F_i(n_i, \beta_i) \tag{3}
\]

where \( F_i(n_i, \beta_i) \) was the fraction of the absorbed energy calculated by using the unpolarized Fresnel equations\(^{23,33} \) and \( n_i \) was the refractive index. Similarly, the absorption of the reflected energy \( \phi_{ir} \) at point \( P_{i,ref}(X, Y, Z) \) was calculated using \( \phi_{ir,ref} = \Phi_i F_i(\beta_{i, ref}) \). This approach was successively continued for the total number of rays and reflections until all intersection points of the incoming rays and corresponding energy were calculated. Note that only one reflection was considered in the optical model as similarly conducted in literature\(^{32,34} \) because the energy carried by the second and following reflected rays were estimated as less than 5% of the energy of the incoming ray.\(^{27} \) In the present work, it was assumed that all optical phenomena took place at the irradiated surfaces and all non-reflected light was therefore considered to be absorbed. Thus, the imaginary part of the complex refractive index was not considered in this study as also done in literature\(^{23} \) The roller deformation was also taken into account in the current optical model. The roller was moved towards the mandrel vertically with a value of \( \delta \) as shown in Figure 7(a) for the sake of simplicity. The resulting geometry and the intersection points of the roller with the substrate (\( N_1-N_2 \) and \( C_1-C_2 \)) were considered in the optical model.

**Local thermal model**

The local thermal model employed the optical model output (power intensity distribution) as a heat flux boundary condition (see Figure 5). A schematic view of the local thermal model domain for the tape and substrate is given in Figure 8. The curved tape and substrate geometries defined in the optical model were unfolded to the computational domains seen in Figure 8 in order to handle the numerical implementation in an easy manner. Since the incoming tape thickness was relatively thin and remained constant during the process, a 2D thermal domain was used for the tape. On the other hand, a 3D thermal model was employed for the substrate in order to account for the thickness increase during the continuous winding process of three rounds as illustrated in Figure 4. The corresponding transient heat conduction equations for the tape and substrate are given in equations (4) and (5), respectively

\[
\rho C_p \left( \frac{\partial T}{\partial t} + \nu \frac{\partial T}{\partial x} \right) = k_x \left( \frac{\partial^2 T}{\partial x^2} \right) + k_y \left( \frac{\partial^2 T}{\partial y^2} \right) + k_z \left( \frac{\partial^2 T}{\partial z^2} \right) \tag{4}
\]

\[
\rho C_p \left( \frac{\partial T}{\partial t} + \nu \frac{\partial T}{\partial x} \right) = k_x \left( \frac{\partial^2 T}{\partial x^2} \right) + k_y \left( \frac{\partial^2 T}{\partial y^2} \right) + k_z \left( \frac{\partial^2 T}{\partial z^2} \right) \tag{5}
\]

where \( \nu \) was the feeding velocity of the tape showing the material movement toward the nip line (\( N_1-N_2 \)), \( C_p \) was the specific heat, \( \rho \) was the density, \( x, y, z \) represented the local spatial locations, \( k_x, k_y, k_z \) represented the thermal conductivity in the \( x-, y-, z \)-direction, respectively, \( t \) was the time and \( T \) was the temperature. The applied BCs are shown in Figure 8. The heat flux distribution obtained from the optical model (\( Q_{ir} \)) was defined at the tape surface and the top of the substrate surface, i.e. \( z = 0 \) by mapping the intersection points of the laser rays with the thermal calculation points. More specifically, a mapping procedure was applied in order to translate the laser irradiation from the optical points to the control volumes in the thermal model. The energy of each incoming ray defined in the optical model was distributed among the four closest control volumes in the thermal model based on the distance between the intersection point of the ray and corresponding control volume where the temperature was calculated at the center. \( Q_r \) was defined as the absorbed energies from all rays and reflections (\( \phi_{ia} \) and \( \phi_{i, ref} \)).

A heat transfer coefficient (HTC) of \( h_{RT} \) with the
average roller temperature $T_{roller}$ was defined at the roller-tape interface contact, i.e. at $0 < x < \pi R_{R}/(180)$. The remaining surface of the tape, i.e. at $\pi R_{R}/(180) < x < L_{T}$, and the top of the substrate surface at $z = 0$ were exposed to the ambient air temperature $T_{\text{air}} = 20^\circ\text{C}$ with a HTC of $h_{\text{air}}$. Note that $T_{roller}$ was updated in the global thermal model in order to take the heating of the roller. The inlet temperature of the tape was set to $20^\circ\text{C}$ at $x = L_{T}$, i.e. line $B_{1} - B_{2}$. On the other hand, the inlet temperature distribution for the substrate was set to $T_{\text{incoming}}$ (defined as incoming material temperature in the local thermal model) at $x = L_{S}$, i.e. the surface containing the line $A_{1} - A_{2}$ in Figure 8. The heat transfer between the substrate and the air inside the pressure vessel was defined by $h_{\text{air}}$ with the average air temperature inside the vessel $T_{\text{incoming}}$ at $z = L_{S}$ where $L_{S}$ was the substrate thickness. By using the global thermal model $T_{roller}$ and $T_{\text{incoming}}$ were updated based on the global heat transfer at the tape-roller and substrate-liner interfaces. Adiabatic
thermal BC was applied to the remaining boundaries of the tape and substrate computational domains. The initial temperature condition was set to 20°C for both calculation domains including \( T_{\text{roller}} \), \( T_{\text{incoming}} \) and \( T_{\text{inner}} \).

The control volume based finite difference method was employed to solve the governing equations with the upwind implicit scheme as described in.\(^{35,36}\) A structured control volume based mesh was employed using the local coordinate system \((x, y, z)\). Total of \(40 \times 23\) control volumes were used in the \(x\)- and \(y\)-direction, respectively, for the tape and \(30 \times 23 \times 14\) control volumes in the \(x\)-, \(y\)- and \(z\)-direction, respectively, for the substrate.

**Global thermal model**

The global thermal model employed the predicted tape and substrate temperature distributions from the local thermal model. Accordingly, the cooling of the substrate as well as the heating of the roller and the air inside the pressure vessel were estimated. A schematic view of the global thermal model is depicted in Figure 9. The calculation domains are illustrated in Figure 9(left) during winding of a single layer which can also be seen in Figure 4(right). Each calculation point which was located on the liner had a unique temperature history. Heat conduction was allowed between the tape and substrate at the nip line \(N_1-N_2\) at which the calculated tape and substrate temperature distributions were combined. In order to make the global model computationally fast, in-plane heat conduction in the winding direction was neglected. The main reason for this was the fact that the through-thickness heat conduction and surface boundary conditions dominate the thermal problem as analyzed and assessed critically in.\(^{22}\) Besides, the heat conduction rate is much smaller than the rate at which the laser moves. The resulting 2D cross section is seen in Figure 9(right) and the corresponding governing equation is given as

\[
\rho C_p \left( \frac{\partial T}{\partial t} \right) = k_x \left( \frac{\partial^2 T}{\partial y^2} \right) + k_z \left( \frac{\partial^2 T}{\partial z^2} \right) \quad (6)
\]

The applied BCs are shown in Figure 9(right). Convective heat transfer was defined at the top surface of the cross-sectional domain \((z = 0)\) for \(i\) the roller from time \(t_{0,i}\) to \(t_{1,i}\), and \(ii\) for the ambient air from time \(t_{1,i}\) to \(t_{2,i}\), as indicated in Figure 9(right) for the \(i^{th}\) layer. Since all input process parameters were assumed constant in the current example, the sampling points indicated as \(i\) in Figure 9(left) were defined along the liner length seen in Figure 4. A total of 50 points was determined for which a total of 50 2D cross-sectional domains seen in Figure 9(right) were utilized. When the local thermal domain was at a global calculation point, \(T_{\text{incoming}}\) in the local thermal model was updated accordingly, e.g. the temperature distribution at \(t_{2,i}\) was used for \(T_{\text{incoming}}\) in the local thermal model. A total of \(23 \times 14\) control volumes was used in the \(y\)- and \(z\)-direction, respectively. The increase in the average air temperature inside the liner \((T_{\text{inner}})\) due to continuous heating of the liner during winding of three adjacent layers was estimated using the relation:\(^{37}\)

\[
\rho \text{air} \ \gamma \text{air} \ C_p \frac{\Delta T_{\text{inner}}}{\Delta t} = A_{\text{L}} h_{\text{air}} (T_{\text{liner}} - T_{\text{inner}}) \quad (7)
\]

where \(T_{\text{liner}}\) was the average temperature at the bottom of the substrate \((z = \text{th}_S + \text{th}_T)\), \(\Delta T_{\text{inner}}\) was the increase in \(T_{\text{inner}}\) during a time step of \(\Delta t\), \(\rho \text{air}\) was the air density, \(\gamma \text{air}\) was the inside pressure vessel volume as 40 liter, \(A_{\text{L}}\) was the contact area between air and the liner which was \(2\pi R_L L_L\) in the global model.

**Figure 9.** Representation of the calculation domains together with the side-view of time-position relation in the global thermal model (left) and the considered 2D cross-sectional domain with the employed boundary conditions (right).
and $C_{p}^{air}$ was the specific heat of the air inside the liner with the air at ambient pressure of 1 atm. Similarly, the increase in average roller temperature $T_{roller}$ was estimated by using the expression

$$\rho_{R} V_{R}^{R} C_{p}^{R} \frac{\Delta T_{roller}}{\Delta t} = A_{RT} h_{RT}(T_{tape} - T_{roller}) + Q_{R}^{p}$$

where $T_{tape}$ was the average tape temperature in contact with the roller, $A_{RT}$ was the area contact between tape and the roller which was equal to $W_{T} \pi R_{R} (\lambda/180)$, $\Delta T_{roller}$ was the increase in $T_{roller}$ during a time step of $\Delta t$, $\rho_{R}$ was the silicon roller density, $V_{R}$ was the roller volume, $C_{p}^{R}$ was the specific heat of the roller and $Q_{R}^{p}$ was the total power exerted on the roller due to the irradiating laser rays and reflections to the roller (see intersection of rays and reflections with the roller in Figure 7(c)). The effect of the roller cooling system was not significantly effective on the silicone part of the deformable roller as it was observed during the experiments.\textsuperscript{13} Thus, the cooling system effect on the roller temperature estimation was neglected in the process model.

### Process model parameters and case studies

The geometrical parameters used in the optical and thermal models were defined based on the experimental work in literature\textsuperscript{13} and are listed in Table 1. A wider substrate control volume than tape width was considered in the process model which was also the case in the thermographic images. The tape feeding velocity was 140 mm/s. The laser distribution at the laser source had a uniform power distribution with a total power of 900 W. The material properties utilized in the process modeling are given in Table 2. Since the temperature-dependent material properties of the HDPE tape and liner were not known and in order to clearly understand the continuous characteristic (location- and time-dependent) of the adjacent hoop LATW process, constant thermal material properties were considered as also used in literature.\textsuperscript{22,38} The refractive index of the prepreg tape was assumed as 1.8 as in literature.\textsuperscript{22} The refractive index of HDPE polymer liner was taken from literature\textsuperscript{39} as 1.5. The higher refractive index indicates a lower absorption of the incoming laser rays by the material. In addition, the effect of mandrel size on the process temperature evolution was studied for two other mandrel radii of 272 mm and 68 mm (default radius was 136 mm\textsuperscript{13}).

### Results and discussion

#### Process model predictions

The laser intensity distributions on the tape and substrate surfaces were first predicted using the optical model and the results are shown in Figure 10 for Round-1 and Round-2. The maximum power intensity was obtained far away from the nip line as indicated in Figure 10 for the selected incident angles of the incoming and reflected laser rays. The maximum value of the power intensity was estimated approximately as $3.2 \times 10^{-1}$ W/mm$^2$ for the tape and substrate. The total power absorbed by the pure HDPE liner (Round-1)
and substrate (Round-2) was calculated as 487.65 W and 465.7 W, respectively. The decrease in the power from Round-1 to Round-2 was due to the increase in the substrate refractive index from that of the liner to that of the wound UD tape. This resulted in a subsequent increase in the total power (272.85 W to 286.5 W) received by the tape due to the increase in laser reflections from the substrate to the tape. The power distribution of Round-3 was almost the same as Round-2 for the tape and substrate since the substrate refractive index remained the same.

The corresponding predicted temperature distributions by the local thermal model are depicted in Figure 11 for Round-1, Round-2 and Round-3. Note that the square boxes indicated on the tape and substrate were the locations at which the temperature was measured during the process in literature.13 These boxes were defined at the centerline of the tape and substrate and 10 mm prior to the visible nip line M1–M2. It is seen that the substrate temperature was approximately 150°C, 180°C and 200°C at the visible nip line (M1–M2) for Round-1, Round-2 and Round-3, respectively. The increase in the substrate temperature

| Parameters                          | Symbol | Values         | Unit   |
|-------------------------------------|--------|----------------|--------|
| Tape thermal conductivity           | \(k_x\) | 0.9840         | [W/(mK)] |
|                                    | \(k_y = k_z\) | 0.72          | [W/(mK)] |
| HDPE liner thermal conductivity     | \(k_{laser}\) | 0.45441       | [W/(mK)] |
| Tape density                        | \(\rho\) | 1710.60        | [kg/m^3] |
| HDPE liner density                  | \(\rho\) | 955.41         | [J/(kgK)] |
| Tape specific heat                  | \(C_p\) | 1180.60        | [J/(kgK)] |
| HDPE liner specific heat            | \(C_p\) | 2200.61        | [J/(kgK)] |
| Air specific heat                   | \(C_p\) | 718.37         | [J/(kgK)] |
| Air density inside liner            | \(\rho\) | 1.225          | [kg/m^3] |
| Silicone roller density             | \(\rho\) | 2180.42        | [kg/m^3] |
| Silicone roller specific heat       | \(C_p\) | 710.43         | [J/(kgK)] |
| Roller refractive index             | \(n_R\) | 1.4325         | –      |
| (G/HDPE) tape prepreg refractive index | \(n_{tape}\) | 1.8425     | –      |
| HDPE liner refractive index         | \(n_{substrate}\) | 1.5993      | –      |
| Air HTC                             | \(h_{air}\) | 10^15         | [W/(m^2-K)] |
| Roller HTC                          | \(h_{RT}\) | 500^17       | [W/(m^2-K)] |

HTC: heat transfer coefficient.

Figure 10. Simulation contour of absorbed power intensity distribution (optical model output) on the tape and substrate in adjacent hoop winding at: (a) Round-1, (b) Round-2. The total absorbed energy by the tape and the substrate are indicated as well. (for interpretation of the references to color in this figure legend, the reader is referred to the web version of this article).
from Round-1 to Round-2 was mainly due to the previously wound hot tape and increase in air temperature inside the liner. Similarly, the tape had a higher temperature near the visible nip line M1-M2 which was mainly due to the increase in roller temperature and a larger contribution of laser reflections from the substrate going from Round-1 to Round-2 as explained above. Further tape and substrate temperature increases in Round-3 were due to continuing heat accumulation in the liner and roller. The evolution of the substrate temperature at the middle of the actual nip line N1-N2 was used as schematically illustrated in Figure 12 to evaluate the temperature history of the previously wound tape. The general trends of the cooling behavior of the center point of the actual nip line seen in Figure 12 are depicted in Figure 13 for points O1, O2 and O3 as an example. It is seen that when the laser head came back to point O1 at $t = 270$ s, the previously wound tape temperature was approximately $50^\circ$C which was higher than the initial temperature of $20^\circ$C due to the increase in the air temperature inside the liner. Accumulation of the thermal energy from the wound tapes of the previous round was the reason for this temperature increase. In addition, there was an approximately $200^\circ$C increase in substrate temperature for point O2 at $t = 135$ s and approximately $190^\circ$C for point O1 at $t = 270$ s due to the winding of two consecutive layers from Round-1 to Round-2 and from Round-2 to Round-3, respectively. The difference between the temperature increase, i.e. increase of $200^\circ$C for point O2 versus $190^\circ$C for point O1, was due to the different substrate thickness in Round-1 and Round-2. The exemplary temperature distributions predicted in the global thermal model for point O3 at $t = 68$ s when the laser head just passed and at $t = 201$ s just before the laser head passed are also given in Figure 13. A detailed analysis of the process temperature distribution and evolution is given in the following.

**Analysis of the process temperature - comparison with experiments**

The predicted local temperature distribution shown in Figure 11 was compared with the thermographic images measured by the thermal camera in literature which are depicted in Figure 14. A good agreement between the measured and predicted local temperature distribution was obtained for Round-1 and Round-2. The maximum difference in mean temperature between the experiments and simulations was found to be approximately 3% and 10% for tape and substrate, respectively. A more detailed comparison between the predicted and measured temperature distributions was made by extracting the temperature values along the length and width direction as seen in Figure 15 and Figure 16, respectively. A total of 20 thermographic frames from the experimental measurements at the middle of Round-1 and Round-2 was extracted of which the mean values and corresponding standard deviations were shown. The extracted frames were obtained from the thermographic recorded video with 50 frames per second (50 Hz) to investigate the main trends of the temperature history as described in literature. The temperature distributions at the centerline along the length or winding direction are depicted in Figure 15. Overall a good agreement was found between the predicted and measured temperature
The increase in the tape and substrate temperature at the center of the nip line (M1-M2) was captured by the proposed process model. More specifically, the tape temperature at the nip line increased approximately by 15°C from Round-1 to Round-2 and by 7°C from Round-2 to Round-3. A larger increase in substrate temperature was found at the center of the nip line which was approximately 42°C from Round-1 to Round-2 and 30°C from Round-2 to Round-3. The temperature distributions along the width direction at the nip line are plotted in Figure 16. Overall, the process model predictions were found to be close to the measured temperature range and profiles for Round-1 and Round-2. The predicted substrate temperature for Round-2 was found to be lower than the measured temperature which could be due to the simplifications made in the process model such as employing constant optical-thermal material properties and neglecting the geometrical disturbances that existed in the winding process as described in literature. It is seen that there was a temperature gradient across the width directions which was mainly due to the winding angle. The temperature difference for
tape and substrate across the width at M1-M2 was approximately 10°C.

In addition to the temperature distributions, the evolution of the average temperatures within the measurement box (see Figure 11 and Figure 14) for tape and substrate are shown in Figure 17. It is seen that there was a good match between the predicted and measured temperature for tape and substrate. The tape temperature followed a relatively simple profile since the heat convection in the control volume of the tape was constant through the process except for the heat transfer with the roller. The roller temperature calculated in the global thermal model was found to increase from 20°C to approximately 130°C at the end of Round-3 which slightly increased the tape temperature. The mean tape temperature was approximately 125°C in Round-1 and 145°C at the end of Round-3. A small jump in the tape temperature occurred at point O2 of approximately 10°C that can be ascribed to the increase in reflected energy from the substrate related to the difference in the substrate refractive index of the HDPE liner (Round-1) and the wound tape (Round-2) as explained before. The developed process model also captured the overall experimental trend and peak values of the substrate temperature very well. A steady substrate temperature at around 125°C was predicted for Round-1 in line with the experimental trend. The effects of the accumulated heat of previously
wound tapes, the increases of the roller temperature and the air temperature inside the liner as well as the contribution of global cooling were more pronounced in Round-2 and Round-3. There were jumps in the substrate temperatures at the beginning of Round-2 and Round-3 in both experiment and simulations with an amount of approximately 80-120°C. These temperature jumps are very important since high temperatures could potentially degrade the material and deteriorate the final product quality. The reason for the temperature jumps was the longer exposure to the laser heating due to reversal of the translational movement of the winding head (see kinematics in Figure 4) at the end of each round, inherent to the consecutive nature of the process. A gradual substrate temperature decrease was observed within Round-2 and Round-3 due to heat loss to the ambient through convection as also seen in Figure 13. The heat transport to the air inside the liner led to an increase of the air temperature up to approximately 65°C at the end of Round-3, which explains why the substrate temperature developments of subsequent rounds move up to higher temperature values. The predicted substrate temperature reached approximately 160°C at the end of Round-2, i.e. approximately a 50°C decrease from the peak temperature of 210°C. This decrease in temperature was with approximately 40°C (from approximately 240°C to 200°C) in Round-3 about 10°C smaller than in Round-2, which can be ascribed to the higher air temperature within the liner in Round-3 decreasing the effectiveness of the heat loss. The higher liner air temperature along with the incorporated heat of previously hot wound tapes (the substrate from Round-1 and Round-2) were also the main reasons for a slight increase in the predicted substrate temperature of approximately 5°C at point O2 from $t = 135$ s at the start of Round-2 to $t = 405$ s at the end of Round-3.

**Effect of pressure vessel size**

The effect of pressure vessel radius ($R_L$) on the evolution of process temperature was analyzed using the proposed process model. The developments of the average temperature within the measurement box for $R_L = 68$ mm, $R_L = 136$ mm and $R_L = 272$ mm are shown in Figure 18. A slight increase in the tape temperatures as a function of the liner diameter can be observed, for example, from 0 to 60 s in Figure 18(left). The increase can be ascribed to a stronger contribution of laser light reflection by the less curved substrate. The tape temperature gradually increased during continuous winding of three rounds for every liner radius due to the continuous increase in the roller temperature. At the end of Round-3 the tape temperature was already close to 170°C.

The substrate temperature decreases as a function of the liner diameter as can be observed in Figure 18(right), see for example the range from 0 to 60 s. The larger the substrate radius the lower the temperature level at which the substrate temperature develops over time. When more laser light is reflected from the substrate to the tape, as described above, less heat remains for the substrate, explaining the altered temperature developments. However, two more aspects play a role here explaining the lower substrate temperatures. Firstly, the volume of the vessel liner increases with the liner radius leading to a slower heating of the air inside the vessel. Secondly, a larger vessel needs more time to be covered by tape, which means that the cooling time is increased if the tape winding speed.
is maintained constant. The peak temperatures at point O2 at the end of Round-1 and at point O1 at the end of Round-2 were also strongly affected by the liner radius. More specifically, the average peak temperature at point O1 went up to approximately 254°C for \( R_L = 68 \) mm at the indicated box location which was prior to the nip line (see also Figure 11). This indicated that the temperature at the nip line was even larger than 254°C as can also be inferred from the substrate temperature development for \( R_L = 136 \) mm in Figure 15(\( \text{right} \)), where the temperature maintains a raising trend towards the nip line N1-N2. On the other hand, the substrate peak temperature was approximately 209°C for \( R_L = 272 \) mm. Thus, the process parameters need to be optimized based on the local geometry of the liner to keep the temperature within a desired range.

Conclusions and outlook

This study was performed in order to critically assess the temperature evolution during the continuous LATW process. A fully coupled local and global process models was developed based on the type-IV pressure vessel manufacturing experiment performed in literature.\(^{13}\) Unidirectional G/HDPE tapes were continuously wound through adjacent hoop winding on an HDPE liner. The laser power intensity distributions were calculated by the 3D optical model for the tape and substrate illuminated by the laser. Differences in the refractive index of the substrate’s HDPE liner and the wound tape had a significant influence on the power intensity distributions. The local temperature distribution near the nip point was captured by the thermal model. The global cooling and heating of the pressure vessel was estimated by the global thermal model. The predictions of the process temperature distributions and evolution on the tape and substrate surfaces were found to agree quite well with the experimentally measured temperatures in literature.\(^{13}\)

The effects of the heat accumulated during winding of the vessel liner by a number of layers and the gradual increase in roller and air temperature inside the liner on the process temperature were found to be very important to predict the continuous temperature evolution and possible temperature peaks. The variation in the substrate temperature was found to be more severe than the tape temperature mainly due to local overheating of the substrate at locations where the translational movement of the winding head changed direction, i.e. from Round-1 to Round-2 and from Round-2 to Round-3. Here, a temperature increase of approximately 80 – 120°C was found to take place for the substrate when two layers of prepreg tape were wound consecutively. This increase was much lower for the tape with values up to 10°C. Besides the studied LATW process simulations based on the experimental work in literature,\(^{13}\) the influence of pressure vessel size on the tape and substrate temperatures was investigated for different liner radii. The peak substrate temperature was found to increase approximately 45°C by reducing the radius of the pressure vessel from \( R_L = 272 \) mm to \( R_L = 68 \) mm. The temperature increase was mostly caused by (i) the shorter time it took to complete the winding process of a smaller vessel, leaving less time for cooling and (ii) the smaller volume of air inside the vessel that was heated up more easily.

It was observed that even with constant input parameters, a complex processing temperature evolution was obtained as a function of time, location and
local liner geometry. Thus, the heat flux obtained from the optical model needs to be optimized via the laser power distribution as a future work to achieve a desired process temperature in each round. In addition, the development of a more comprehensive statistical ray tracing approach is considered as a future work. To have an even more accurate process model, the experimental characterization of the roller deformation, optical and thermal material properties as a function of surface topology, temperature and fiber orientation is required. Nevertheless, the fulfilled work within this paper has already the potential to eliminate expensive trial and error based experiments through the physics-based process simulations that are well capable of predicting the major trends in the time and spatial temperature distributions of the tape and the substrate as a function of process conditions.

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Note
a. Since there was no thermographic image data recorded during manufacturing of Round-3 in literature, only the simulation results were represented for this round.

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