**TENSILE STRENGTH MODELING OF GLASS FIBER-POLYMER COMPOSITES AND SANDWICH MATERIALS IN FIRE**

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1 Introduction

One issue challenging the greater use of polymer composite materials in structural products traditionally made using metals is their high flammability. Composites are often considered a safety risk because they can soften, buckle and collapse in a fire.

Most of the research has focused on compressive failure of fiberglass laminates because of their use in structures supporting compression loads [1]. The failure of laminates during fire while under tension loading is significantly different to compression failure, and the time-dependent strength loss of the fiber reinforcement needs to be considered [2,3]. A model for tension was developed by the authors and validated for resin-infused GFRP laminates of high volume fraction [2]. The validation for sandwich composite failure under tension has previously not been addressed.

This paper presents a thermal-mechanical model to predict the temperature rise, decomposition, softening and failure of laminates and sandwich composites under combined one-sided heating by fire and tensile loading. The model is validated using data from fire structural tests performed on a fiberglass laminate and sandwich composite consisting of fiberglass laminate face skins and balsa core. Both the laminate and sandwich composites are used in structural applications.

2 Fabrication and experimental testing

The laminate and face skins to the sandwich composite were manufactured with hand lay-up using E-glass woven fabric (800 g/m²) and vinyl ester resin (Derakane 411-350). The resin did not contain flame retardant fillers or additives, and had a glass transition temperature (T_g) of 120°C. The fiber stacking sequence of the laminate and sandwich skins was [0/90]. The laminate had a fiber volume fraction of 0.50±0.01 and was 4 mm thick.

The core material for the sandwich material was Baltek® SB structural end-grain balsa (ρ_c = 150 kg/m³) with 6 mm thickness. The balsa grains were aligned in the through-thickness direction of the sandwich composite, which is normal to the direction of tension loading. Both fiberglass face skins were 2mm thick. The face skins had a slightly lower fiber volume content of V_f=0.44±0.05.

Iso-thermal tensile tests up to 600°C were performed on the laminate, glass fiber bundles, single glass fibers and the balsa core to determine the changes to their stiffness and strength properties with increasing temperature.

Fire tests were performed on both laminates and sandwich composites to generate experimental data to validate the model. The fire-under-load test involves pre-loading the specimens in tension while simultaneously heating one side using a radiant heater [2]. The specimen ends were rigidly clamped along a length of 80mm. A constant tensile stress between 10% and 90% of the room temperature
ultimate stress was applied to the specimen using a 250 kN MTS machine. The specimens were free to thermally expand during heat exposure. For the sandwich material, both skins were constrained in the test rig to experience the same strain during testing. Fire-under-load tests were performed for heat fluxes of 25, 35 and 50 kW/m\(^2\). The time taken for the specimen to rupture, called the time-to-failure, was measured. For the sandwich specimens, failure of the two skins occurred separately for some test conditions. In this case the time of first skin failure was also recorded. These values were used to determine the accuracy of the thermal-mechanical model.

3 Modeling procedure

3.1 Temperature modeling

Thermal analysis of the laminate and sandwich composite is performed using a thermal model developed by Feih et al. [1-3]. The thermal model analyses the three important energy transfer processes that occur in a decomposing material exposed to fire, namely conductive heat transfer; endothermic decomposition; and convective mass transfer of volatile products from the decomposing material to the hot composite surface. The temperature rise with time (\(\partial T / \partial t\)) is calculated using the following equations:

Laminate material:
\[
\frac{\partial h_s}{\partial t} = \rho \left( k_s \frac{\partial T}{\partial x} - M_o \frac{\partial h_{c,s}}{\partial x} \right) - M_o \frac{\partial h_{c,s}}{\partial x} - \frac{\partial}{\partial x} \left( Q_{f,s} + h_{c,s} - h_{o,s} \right)
\]

Balsa core:
\[
\frac{\partial h_c}{\partial t} = \rho \left( k_c \frac{\partial T}{\partial x} - M_o \frac{\partial h_{c,c}}{\partial x} \right) - M_o \frac{\partial h_{c,c}}{\partial x} - \frac{\partial}{\partial x} \left( Q_{f,c} + h_{c,c} - h_{o,c} \right)
\]

with the subscripts \(s\) and \(c\) referring to the composite skins and core, respectively. Modeling is performed using temperature dependent values for the thermal properties \((c_p, k)\) of the skins and core. The first term on the right-hand side of Eq. (1) relates to unsteady-state heat conduction through the skins and core. The second term defines the magnitude of the mass flux of volatile decomposition products through the composite towards the fire. The last term is the endothermic decomposition term that defines the decomposition reaction rate of the polymer matrix to the skins or the core. By solving equation (1) for increasing temperature and time (\(\partial T / \partial t\)), it is possible to calculate the temperature profile through a laminate and sandwich composite.

The decomposition rate of the skins or core is expressed in the last term of eq. (1) by \(\partial M / \partial t\). When the skins or core decompose by a single-stage reaction process then \(\partial M / \partial t\) is calculated using the first-order Arrhenius relationship:

\[
\frac{\partial M}{\partial t} = -AM_o \left( \frac{M - M_{o,s}}{M_o} \right) e^{(-E_o/RT)}
\]

The model considers unit volumes, and therefore the density and mass are effectively identical. The matrix density is updated through the evaluation of Eq. (2) during the thermal analysis, and the density of the composite is calculated by rule-of-mixtures.

During the calculation, values for the thermal constants and the enthalpy are continually updated to allow for the effects of decomposition. The enthalpy is calculated on the basis of the instantaneous thermal constants and the temperature. \(M_o\), the mass flux of decomposition volatiles, is calculated from the change in density of the material and integrated through-the-thickness of the composite to account for the build-up of these gases. The thermal boundary condition on the hot face is assumed to be a constant uniform thermal flux. The cold face considers thermal insulation. All input parameters for the model are supplied in Table 1.

3.2 Laminate modeling

Both resin degradation and fiber strength loss need to be considered for the time-to-failure predictions of the monolithic laminate and laminate face skins, and these two mechanisms operate in different temperature and time regimes. A modeling flowchart for the prediction of times-to-failure for fiberglass laminates is shown in Figure 1. This modeling
approach was previously validated for laminates of high fiber volume fraction ($V_f = 0.55 - 0.6$) [2].

Figure 2 shows the strength loss of single glass fibers and fiber bundles as function of time and temperature. Fiber strength loss is significant in temperature regimes between 300 and 600°C (see Fig. 2). The magnitude of strength loss is higher for the fiber bundles, most likely due to the additional effect of friction between the fibers, which is believed to create fine-scale damage on the fiber surface [3].

The fiber strength loss is temperature and time-dependent, which needs to be considered in the model [2]. The \( \tanh \) function that describes the relationship between fiber bundle strength \( \sigma_{fb} \), temperature \( T \) and heating time \( t \) is:

\[
\sigma_{fb}(t, T) = \sigma_{fb(0)} - \sigma_{loss}(T) \tanh[k_{fb}(T)t]
\]

where \( \sigma_{fb(0)} \) is the tensile strength at 20°C, \( \sigma_{loss}(T) \) describes the strength loss, and \( k_{fb}(T) \) describes the rate of strength loss as a function of temperature. \( k_{fb}(T) \) is determined from the curve-fit temperature function

\[
k_{fb}(T) = k_1 e^{k_2 T},
\]

where \( k_1 \) and \( k_2 \) are curve fit constants. The strength loss function, \( \sigma_{loss}(T) \), occurs in a symmetric fashion around a temperature, \( T_{50\%} \), \( T_{50\%} \) is the temperature at which the fiber bundle loses 50% of its tensile strength for long-term heat exposure. The strength loss is determined using:

\[
\sigma_{loss}(T) = \frac{\sigma_{fb(0)}}{2} + \frac{\sigma_{fb(0)} \tanh[P_{fb}(T - T_{50\%})]}{2}
\]

with \( T_{50\%} \) and \( P_{fb} \) being curve-fit constants. Knowing the dependence of fiber strength on temperature and time from experimental data, it is possible to determine values for \( k_{fb}(T) \), \( T_{50\%} \) and \( P_{fb} \). The values for E-glass fibers (single and bundles) are shown in Table 2. It is believed that both degradation mechanisms are relevant for the present case: while the fibers are fully encapsulated in softened resin, they will degrade according to the single fiber strength loss. Once the resin has completely decomposed (residual resin content < 10%), the fiber bundle degradation is assumed to apply. Differences due to the atmospheric environment are neglected in this modeling approach.

Figure 3 shows the tensile strength and stiffness loss due to resin softening as a function of temperature for the laminate. The stiffness loss is attributed to the ability of the woven fibers to straighten as the matrix softens. Strength loss is attributed to the loss of the stress-transfer mechanism between fibers as the resin softens. The figure also show the fitted data curves as described by the \( \tanh \) function fit:

\[
P(T) = \frac{P_{(0)} + P_{(R)}}{2} - \frac{P_{(0)} - P_{(R)}}{2} \tanh[k_{m}(T - T_k)]
\]

It is seen in Figure 3 that both stiffness and strength loss occur around a similar temperature range (fitting parameters \( k_m \) and \( T_k \) are the same). At 200°C, a loss of 50% to both properties is recorded. This property loss is higher than the loss reported in previous work [2] due to the lower fiber volume fraction.

A rule-of-mixtures approach has been shown to successfully predict the residual strength of a monolithic laminate for a given temperature-time exposure [2]. This approach separates the temperature-dependent resin softening and time and temperature-dependent fiber strength loss and considers these two events independently. For the sandwich composite, the difference in stiffness for each skin is considered as an additional factor in this process. The large temperature variation leads to an un-symmetric sandwich structure, where the front skin carries significantly less force than the back skin as both skins are experiencing the same strain under load. The strengths of the front and back skins are therefore compared separately to the respective stresses to determine the time-to-failure.

4 Results and Discussion

4.1 Temperature predictions

Figure 4-6 show the experimental (data points) and calculated (solid lines) temperature-time profiles for
the laminate, exposed to the heat fluxes of \( q = 25, 35 \) and \( 50 \text{ kW/m}^2 \), which heated the front face to about 400, 530 and 630°C, respectively. Profiles are shown for the temperatures at the heated (front) face, middle and unheated (back) face of the laminate. It can be seen that the higher heat fluxes caused a spike in temperature due to internal ignition. This ignition was incorporated into the thermal model predictions through an increase in external heat flux at the experimental ignition time. The ignition time was not predicted. The ignition process was terminated in a similar fashion based on experimental results. It can also be seen that the higher heat flux leads to earlier ignition and earlier extinguishment of the laminate due to faster depletion of the resin material, which is the main fuel source of combustion as the fibers do not decompose.

Figures 7-9 show the corresponding temperature profiles and predictions for the sandwich material. Comparing laminates and sandwich materials at the same heat flux, the back face temperatures for the sandwich materials are generally lower due to the low thermal conductivity of the balsa wood, which acts as a thermal insulator.

There is no ignition for the heat flux of \( q = 35 \text{ kW/m}^2 \) for the sandwich composite. This is attributed to the front skin of the sandwich material being equivalent to half the thickness of the laminate, thereby resulting in less fuel (decomposition gases being released by the polymer matrix) to contribute to the combustion process. Internal ignition at the heat flux of \( q = 50 \text{ kW/m}^2 \) is further contributed via decomposition of the balsa core (rather than the skins), resulting in core temperatures exceeding the front face temperature. The ignition was incorporated into the thermal model predictions through an increase in external heat flux at the experimental ignition time in the same manner as undertaken for the laminate predictions. Again, the ignition time was not predicted. The ignition was terminated in a similar fashion based on experimental results.

4.2 Laminate failure predictions

Figure 10 shows the experimental time-to-failure for the glass/vinyl ester laminate tested at the three heat fluxes. As expected, the failure times increased when the heat flux or applied tensile stress are reduced. The solid curves show the calculated failure times determined using the thermal-mechanical model. Four stages of failure are predicted: (I) softening of the resin (shear lag effect), (II) stable stage of fully decomposed resin and intact fibers, (III) fiber strength degradation, and (IV) residual strength stabilization at steady-state temperature distribution. Within the softening section (> 50% load, t<100s), the model slightly over-predicts the failure times. This may be caused by small variations in the temperature profile predictions as the resin softening model is very sensitive to the initial temperature predictions. For loads <50% (fiber softening regime), the model under-predicts the failure times (conservative). The discrepancy between predictions and experimental failure times is less once the resin has fully decomposed (high heat flux, longer failure times). In this state, mostly bare fiber bundles are tested under load which is the same test condition used initially to derive the time- and temperature-dependency of the fiber strength loss. The largest discrepancy between predictions and experimental failure times is observed for the lowest heat flux of 25kW/m², which leads to minimal resin decomposition. Fibers for these heating conditions are still encapsulated in a fully softened resin matrix. Fiber strength loss is a surface-controlled event due to the growth of surface flaws. The rate of strength loss is sensitive to the atmospheric environment [3]. It is postulated that the protection of the fiber surface from environmental moisture exposure during heating delays the strength degradation process.

4.3 Sandwich failure predictions

Figure 11 shows the experimental time-to-failure for the sandwich composite for the three heat fluxes. Again, the failure times increase with decreasing heat flux and applied stress. Failure occurs in two stages due to the separate events of resin softening and fiber strength loss. Furthermore, two distinctive failure events can be distinguished for lower loads: front face and back face failure occur as separate events due to the significant temperature differences between the two skins. This is discussed later with the modeling approach.
Comparing Figure 10 and Figure 11, the laminate generally fail earlier for the same applied stress than the sandwich material. This is due to the unsymmetric load distribution as the front skin loses stiffness earlier (and thereby experiences lower stress than the back skin). The balsa core therefore prolongs the time-to-failure when compared to a laminate of the same total skin thickness of 4mm.

The current mechanical failure model has been evaluated to account for unsymmetric strength and stiffness loss of the front and back face skins. The balsa core does not contribute significantly under tension as its strength is low. Balsa strength is therefore neglected in the strength modelling approach. The stresses in both skins can then be evaluated separately according to

\[
\sigma_1 = \frac{E_1 F}{A_{	ext{skin}}(E_1 + E_2)} \quad \text{and} \quad \sigma_2 = \frac{E_2 F}{A_{	ext{skin}}(E_1 + E_2)},
\]

where the index 1 denotes the front skin (exposed to the heat source) and 2 indicates the stress in the back skin. \(A_{	ext{skin}}\) denotes the area of each composite skin, and \(F\) is the applied load.

Progressive failure of the two skins is considered. Upon failure of either skin (first failure), the applied load effectively doubles on the other skin. If the applied load is sufficiently low to be carried by the remaining skin alone, two separate failure events are predicted.

Figure 12 – 14 show the resulting predictions for the different heat fluxes. It can be seen that the model is able to accurately predict the initial strength loss during resin softening accurately. \(E_1 < E_2\) applies during this stage, and the stress experienced by the front skin will therefore be lower than the stress in the back skin when the strains in both skins are the same. For short exposure times, this un-symmetric stress distribution then leads to first failure of the back skin, which has been indicated in the figures.

The second stage of fiber softening is generally under-predicted for first failure, and over-predicted for the combined failure of the front and back skins. This is currently under investigation. The following statements are made at this point: (1) Similar to the laminates, the fibers will be protected from environmental degradation by the softened resin for the lowest heat flux of 25kW/m². (2) One observed problem is that the temperature profiles during tensile loading actually differ from the temperature profiles captured without load. This is attributed to the cracks within the balsa core opening up as the sandwich material undergoes tensile failure. As a result, a larger amount of decomposition gases is released, leading to internal ignition and therefore higher temperatures especially in the back skin for the heat flux 35kW/m². This discrepancy will affect the predictions for time-to-failure for the back skin at longer exposure times (lower loads).

**Conclusions**

The model shows that tensile failure of GFRP composites and sandwich composites is controlled by both resin softening and fiber strength loss. The balsa core has a significant effect on prolonging the time-to-failure for sandwich composites compared to laminates of the same skin thickness due to unsymmetric softening of the two skins.

**Acknowledgements**

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**References**


Tables and Figures

Table 1. Thermal properties of the laminate skins and balsa core

<table>
<thead>
<tr>
<th>Property</th>
<th>Skin</th>
<th>Core</th>
</tr>
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<tbody>
<tr>
<td>Rate constant, $A$ [1/s]</td>
<td>5.6E13</td>
<td>6.7E7</td>
</tr>
<tr>
<td>Activation energy $[J/mol]$</td>
<td>212705</td>
<td>116488</td>
</tr>
<tr>
<td>Heat of decomposition $[J/kg]$</td>
<td>378800</td>
<td>556000</td>
</tr>
<tr>
<td>Specific heat glass/vinyl ester $[J/(kg K)]$</td>
<td>890+2.4<em>T-0.003</em>T²</td>
<td>1420 + 0.68*T</td>
</tr>
<tr>
<td>Specific heat char $[J/(kg K)]$</td>
<td>890+2.4<em>T-0.003</em>T²</td>
<td>3194 + 1.33*T</td>
</tr>
<tr>
<td>Specific heat gas $[J/(kg K)]$</td>
<td>2387</td>
<td>1009</td>
</tr>
<tr>
<td>Thermal conduct virgin $[W/(m K)]$ (60-300°C)</td>
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<td>0.2</td>
</tr>
<tr>
<td>Thermal conduct char $[W/(m K)]$ (300-500°C)</td>
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<td>0.008 + 2.22e-6*T¹.⁸⁹</td>
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<tr>
<td>Density $[kg/m³]$</td>
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<td>150</td>
</tr>
<tr>
<td>Remaining Resin Mass Fraction [%]</td>
<td>3</td>
<td>15</td>
</tr>
<tr>
<td>Fiber volume fraction</td>
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<td>Moisture content [wt%]</td>
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<td>8</td>
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Table 2. Fitted data for glass fiber strength reduction (from [3])

<table>
<thead>
<tr>
<th>Values</th>
<th>Fiber bundles</th>
<th>Single fibers</th>
</tr>
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<tbody>
<tr>
<td>$T_{50%}$ [°C]</td>
<td>347.6</td>
<td>403.1</td>
</tr>
<tr>
<td>$p_{0}$ [°C⁻¹]</td>
<td>5.83E-3</td>
<td>6.60E-3</td>
</tr>
<tr>
<td>$k_1$ [s⁻¹]</td>
<td>1.81E-6</td>
<td>8.63E-6</td>
</tr>
<tr>
<td>$k_2$ [°C⁻¹]</td>
<td>1.45E-2</td>
<td>1.17E-3</td>
</tr>
</tbody>
</table>

Fig. 1. Flow chart of laminate modeling (from [2])

Fig. 2. (a) Single fiber and (b) fiber bundle strength as functions of time and temperature (from [2])
TENSILE STRENGTH MODELING OF GLASS-FIBER LAMINATES AND SANDWICH MATERIALS IN FIRE

Fig. 3. (a) Strength loss and (b) stiffness loss of the laminate with increasing temperature

Fig. 4. Temperature profiles for the laminate exposed to a heat flux of $q=25\text{ kW/m}^2$

Fig. 5. Temperature profiles for the laminate exposed to a heat flux of $q=35\text{ kW/m}^2$

Fig. 6. Temperature profiles for the laminate exposed to a heat flux of $q=50\text{ kW/m}^2$

Fig. 7. Temperature profiles for sandwich composite exposed to a heat flux of $q=25\text{ kW/m}^2$

Fig. 8. Temperature profiles for sandwich composite exposed to a heat flux of $q=35\text{ kW/m}^2$
Fig. 9. Temperature profiles for sandwich composite exposed to a heat flux of $q=50\text{ kW/m}^2$.

Fig. 10. Time-to-failure for laminate for different heat fluxes. Solid lines are predictions, and the data points indicate experimental times.

Fig. 11. Time-to-failure for GFRP sandwich materials (2mm skin thickness) for different heat fluxes. Solid lines are lines of best fit, and the data points indicate experimental times.

Fig. 12. Time-to-failure for sandwich composite at a heat flux of $q=25\text{ kW/m}^2$. Solid lines are predictions, and the data points indicate experimental times.

Fig. 13. Time-to-failure for sandwich composite at a heat flux of $q=35\text{ kW/m}^2$. Solid lines are predictions, and the data points indicate experimental times.

Fig. 14. Time-to-failure for sandwich composite at a heat flux of $q=50\text{ kW/m}^2$. Solid lines are predictions, and the data points indicate experimental times.