Analysis of composite hydrogen storage cylinders subjected to localized flame impingements

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Abstract

A comprehensive non-linear finite element model is developed for predicting the behavior of composite hydrogen storage cylinders subjected to high pressure and localized flame impingements. The model is formulated in an axi-symmetric coordinate system and incorporates with various sub-models to describe the behavior of the composite cylinder under extreme thermo-mechanical loadings. A heat transfer sub-model is employed to predict the temperature evolution of the composite cylinder wall and accounts for heat transport due to decomposition and mass loss. A composite decomposition sub-model described by Arrhenius’s law is implemented to predict the residual resin content of thermal damaged area. A sub-model for material degradation is implemented to account for the loss of mechanical properties. A progressive failure model is adopted to detect various types of mechanical failure. These sub-models are implemented in ABAQUS commercial finite element code using user subroutines. Numerical results are presented for thermal damage, residual properties and profile of resin content in the cylinder. The developed model provides a useful tool for safe design and structural assessment of high pressure composite hydrogen storage cylinders.

1. Introduction

High pressure composite cylinders for hydrogen storage are typically made with a high molecular weight polymer or aluminum liner that serves as a hydrogen gas permeation barrier. A filament-wound, carbon/epoxy composite laminate over-wrapped outside of the liner provides the desired pressure load bearing capacity. Due to the flammable nature of polymer-reinforced composites, there is a high risk of failure of the composite hydrogen storage cylinder under accidental fire exposure. As the stiffness and strength of composites are temperature dependent, the high pressure (usually 34.5–70.0 MPa) in the cylinder will result in a catastrophic failure. Therefore, assessment of structural integrity of high pressure composite hydrogen storage cylinders subjected to fire exposure is necessary for safe cylinder design.

Many investigations have been conducted by various researches [1–4] on the behavior of high pressure composite cylinder under mechanical loadings. Using finite element method, numerous studies have been performed on high-pressure vessels analysis [5–7]. Compared to pure mechanical loading and pure thermal loading, there have been few studies on the composite cylinders subjected to the combined thermal and mechanical loads [8,9]. However, those models only consider the temperature range before the composite decomposition and fall short of capability to assess the cylinder behavior subjected to fire impingement. To study fire...
effect on composites, different one-dimensional thermal models have been developed to predict the temperature and the residual resin content as a function of time [10–12]. These models account for heat transfer by conduction, resin decomposition and the cooling effect of volatile products passing through the laminate. Therefore, to characterize the fire response of high pressure hydrogen composite cylinders, mechanisms of mechanical effect, thermal effect and decomposition have to be considered.

In this paper, a coupled thermo-mechanical finite element model has been developed to simulate the composite hydrogen storage cylinder subjected to high pressure and heat flux on the wall surface. The model is formulated in an axi-symmetric coordinate system which accounts for out-of-plane stresses and reduces computational cost dramatically. When the polymer materials are exposed to a sufficiently high temperature (200–300°C), decomposition reactions and thermo-chemical expansion begin to occur and the resin system degrades to form gaseous products and carbonaceous char. In order to incorporate this phenomenon, the thermal model is built to account for gas convection and heat generation of decomposition in addition to conduction of the composite, convection and radiation of surface. The fire source is modeled as a constant heat flux throughout the simulation. Inner pressure of the cylinder is dependent on temperature of hydrogen gas which is modeled as a sink temperature. The variation of material properties with temperature is significant for composites, especially at high temperatures. A temperature dependent material model has been developed and implemented. Hashin’s theory is used as progressive failure criterion to predict different types of failure (matrix cracking and fiber breakage). These models are developed and implemented in commercial finite element code ABAQUS, using user subroutines. A typical high pressure hydrogen composite cylinder is simulated using the developed model and results from the parametric study are reported.

2. Modeling of high pressure composite hydrogen storage cylinders

A strong sequentially coupled thermal-stress analysis approach is implemented for predicting the behavior of composite hydrogen cylinder subjected to flame impingements and internal high pressure. At each increment, the temperature profile is obtained using the thermal and resin reaction models. The temperature field is then imported to the mechanical model with material damage information from previous increment. The pressure of the inner cylinder is updated based on the heat absorption of hydrogen gas, which is computed from the temperature profile. The thermal and mechanical models are solved sequentially in each increment. As the wall consists of a large number of laminae, modeling each lamina will cause extraordinary computational cost, especially when thermal and damage models are also incorporated. Hence, a homogenization technique is used to smear several laminae to a sub-laminate as in Fig. 1. The equivalent moduli of the sub-laminate are used in the formulation of finite element model.

2.1. Thermal model

The high temperature will cause the resin decomposition of the cylinder composite wall. The decomposition rate of the resin is usually represented by Arrhenius’s law and can be expressed as [13]

\[
\frac{\partial \rho}{\partial t} = -A \rho \exp\left(-\frac{E_A}{RT}\right)
\]

(1)

where \(\rho\) is the density, \(t\) is time, \(T\) is the temperature, \(A\) is the pre-exponential factor, \(E_A\) is the activation energy and \(R\) is the gas constant.

Material constants \(A\) and \(E_A\) are determined using differential scanning calorimetry. With the consideration of resin reaction, the heat transfer equilibrium equation has to include resin decomposition energy and vaporous migration energy. As the dimension in thickness direction is much less than those of hoop and axial directions, the vaporous gas are only assumed to transfer in thickness direction. The axi-symmetric heat diffusion equation can be written as [14]

\[
\frac{\rho C_p}{\partial t} \frac{\partial T}{\partial r} = \frac{\partial}{\partial r}\left(k_r \frac{\partial T}{\partial r}\right) + \frac{1}{r} \left(k_\theta \frac{\partial T}{\partial \theta}\right) + \frac{\partial}{\partial z}\left(k_z \frac{\partial T}{\partial z}\right) + \dot{m}_g C_{pg} \frac{\partial T}{\partial t} + \frac{\partial \rho}{\partial t}(Q - h_{\text{com}} - h_g)
\]

(2)

![Fig. 1 – Schematic of hydrogen cylinder.](image-url)
where \( C_p \) is specific heat of composite, \( k \) is thermal conductivity, \( r \) is displacement through the thickness, \( n_g \) is gas mass flux, \( C_{pg} \) is specific heat of gas, \( Q \) is heat of decomposition, \( h_{vap} \) is enthalpy of composite and \( h_b \) enthalpy of hydrogen gas.

The mass flow direction of the gas generated in the laminate is towards the hot face of the laminate. The total mass flux of the generated gas at a specific spatial location in the laminate is the sum of the gas mass flux generated from the inner face to this spatial location. The gas mass flux at any spatial location can be calculated as

\[
\dot{n}_g = \int_{r_0}^{r_h} \left. \left( \frac{\partial \rho_g}{\partial t} \right) \right|_s \, dr_h
\]

where \( r_h \) is distance from hot face and \( t_W \) is wall thickness of composite.

### 2.2. Sub-laminate model

The principle of sub-laminate (Fig. 1) homogenization is based on the assumption that the in-plane strains and the interlaminar stresses through the thickness are constant [15–18]. The lamina stress-strain relationship in global coordinate can be written as

\[
\begin{bmatrix} \sigma_{x} \\ \sigma_{y} \\ \tau_{xy} \end{bmatrix} = \begin{bmatrix} C_{oo} & C_{oi} & C_{oi} \\ C_{oi} & C_{ii} & C_{ii} \\ C_{oi} & C_{oi} & C_{ii} \end{bmatrix} \begin{bmatrix} \varepsilon_{x} \\ \varepsilon_{y} \\ \gamma_{xy} \end{bmatrix}
\]

where \( C_{oo}, C_{oi} \) and \( C_{ii} \) are sub-matrices of global stiffness matrix, \( C_{oo} \) is the transpose of \( C_{oi} \), \( \sigma_{x} \) and \( \varepsilon_{x} \) are out-of-plane and in-plane stresses, respectively, \( \varepsilon_{y} \) and \( \gamma_{xy} \) are out-of-plane and in-plane strains, respectively. \( () \) represents the term is constant through laminate.

Partially inverting Eq. (4) yields

\[
\begin{bmatrix} \varepsilon_{x} \\ \varepsilon_{y} \end{bmatrix} = \begin{bmatrix} C_{oo}^{-1} & -C_{oo}^{-1}C_{oi} \\ C_{oi} & C_{ii} \end{bmatrix} \begin{bmatrix} \sigma_{x} \\ \sigma_{y} \end{bmatrix}
\]

After averaging Eq. (5) can be expressed as

\[
\begin{bmatrix} \langle \varepsilon_{x} \rangle \\ \langle \varepsilon_{y} \rangle \end{bmatrix} = \begin{bmatrix} A & -B \\ B^T & D \end{bmatrix} \begin{bmatrix} \langle \sigma_{x} \rangle \\ \langle \sigma_{y} \rangle \end{bmatrix}
\]

where

\[
A = \sum_{k=1}^{N} \frac{t_k}{t} (C_{oo}^{-1})_{kk} \quad B = \sum_{k=1}^{N} \frac{t_k}{t} (C_{oo}^{-1}C_{oi})_{kk}
\]

\[
D = \sum_{k=1}^{N} \frac{t_k}{t} (-C_{oi}C_{oo}^{-1}C_{oi} + C_{ii})_{kk}
\]

\( t_k \) is the thickness of each lamina in the smeared sub-laminate and \( t \) is the total thickness of the smeared sub-laminate.

Partially inverting Eq. (6) yields the equivalent stiffness matrix \( Q_{eq} \) of the sub-laminate as

\[
\begin{bmatrix} \langle \sigma_{x} \rangle \\ \langle \sigma_{y} \rangle \end{bmatrix} = \begin{bmatrix} A^T & A^{-1}B \\ B^T A^{-1}B & D \end{bmatrix} \begin{bmatrix} \langle \varepsilon_{x} \rangle \\ \langle \varepsilon_{y} \rangle \end{bmatrix}
\]

The equivalent compliance matrix is given by

\[
Q_{eq}^{-1} = \begin{bmatrix} A^{-1} & A^{-1}B \\ B^T A^{-1} & B^T A^{-1}B + D \end{bmatrix} = [S_0]
\]

Equivalent engineering properties used for the simulation can be obtained from Eq. (8) as

\[
E_{11} = \frac{1}{S_{11}} \quad E_{22} = \frac{1}{S_{22}} \quad E_{12} = \frac{1}{S_{12}} \quad G_{12} = \frac{1}{S_{16}} \quad G_{13} = \frac{1}{S_{13}} \quad G_{23} = \frac{1}{S_{23}}
\]

\[
\begin{cases} \nu_{12} = \frac{-S_{45}}{S_{11}} \quad \nu_{13} = \frac{-S_{46}}{S_{11}} \quad \nu_{23} = \frac{-S_{46}}{S_{22}} \end{cases}
\]

After the global stresses and strains of sub-laminates are obtained, the stresses in each lamina are obtained in the global material coordinates as

\[
\begin{aligned}
\langle \sigma_{x} \rangle &= \langle \sigma_{x} \rangle \\
\langle \sigma_{y} \rangle &= \langle \sigma_{y} \rangle \\
\langle \sigma_{xy} \rangle &= \langle \sigma_{xy} \rangle \\
\end{aligned}
\]

where \( k \) is the \( k \)th lamina of the sub-laminate.

Finally, the stresses in local material coordinate system can be obtained by standard coordinate transformation.

### 2.3. Formulation of finite element model

Axi-symmetric formulation for transient analysis can be written as

\[
[M^g][\dot{\mathbf{A}}^t] + [K^g][\mathbf{A}^t] = \{F^t\} + \{F_{\text{eq}}^t\}
\]

where

\[
[M^g] = 2\pi \int_s \rho [N]^T[N] \eta \, ds, \quad [K^g] = 2\pi \int_s [B]^T[C][B] \eta \, ds,
\]

\[
[\mathbf{A}^t] = \{(u_t, u_0, u_z)\}^{T}
\]

\( \{F^t\} \) and \( \{F_{\text{eq}}^t\} \) are forces due to mechanical and thermal loads, respectively. In addition, \( N \) is interpolation function, \( B \) is strain displacement function, \( C \) is elasticity matrix, \( \rho \) is density and \( \{u_t, u_0, u_z\} \) are displacement components in cylindrical coordinate system.

By discretizing Eq. (2), the heat conduction formulation can be expressed as

\[
[C_T][\dot{T}] + [K_T][T] = [H_T]
\]

where

\[
[C_T] = \int_s \rho C_p N^T N \, dV, \quad [K_T] = \int_v [N]^T k N \, dV
\]

\[
[H_T] = \int_s N^T \eta_0 q_s^T \, ds + \int_v N^T \eta_t q_t \, dV
\]

where \( k \) is thermal conductivity, \( q_s \) is surface heat flux and \( q_t \) is the reaction heat due to the resin decomposition and vaporized migration.

The reaction heat \( q_t \) can be expressed as

\[
q_t = \dot{m}_g C_{pg} \frac{\Delta T}{\Delta t} + \frac{\Delta P}{\Delta t}[Q + (C_{pg} - C_p) \cdot (T - T_\infty)]
\]

where \( C_{pg} \) is the specific heat of gas, \( T_\infty \) is ambient temperature and \( Q \) is heat of decomposition.

Combining Eqs. (11) and (12), the sequentially coupled thermal-stress equation can be written as

\[
\begin{bmatrix} M & 0 & 0 \\ 0 & C_T & 0 \\ 0 & 0 & K_T \end{bmatrix} \begin{bmatrix} \{\dot{\mathbf{A}}^t\} \\ \{T\} \end{bmatrix} + \begin{bmatrix} 0 & 0 & 0 \\ 0 & C_T & 0 \\ 0 & 0 & K_T \end{bmatrix} \begin{bmatrix} \{\mathbf{A}^t\} \\ \{T\} \end{bmatrix} = \begin{bmatrix} \{F^t\} + \{F_{\text{eq}}^t\} \\ \{\mathbf{H}_T\} \end{bmatrix}
\]
3. Material models

3.1. Temperature dependent material properties

Mechanical and thermal properties of fiber reinforced composites vary significantly with temperature. As the carbon/epoxy laminate carries the pressure loading from the hydrogen gas, the effect of temperature on its material properties cannot be ignored. However, the full required data of temperature dependent properties are not available in literature. Assumptions and curve fittings are necessary to obtain the material property data based on limited experimental data that are available. Numerous studies have been done on the curve fitting of temperature dependent properties. Gibson et al. [19] show that hyperbolic tan (tanh) function is capable of giving excellent fit to experimental data of material moduli and strength. The equation can be expressed as

\[
P(T) = \left[ \frac{P_U + P_R}{2} - \frac{P_U - P_R}{2} \tanh(k(T - T_g)) \right] C
\]

(15)

where \( P(T) \) is temperature dependent material property, \( P_U \) is the unrelaxed (low temperature) value of that property, \( P_R \) is the relaxed (high temperature) value of that property, \( k \) is a constant describing the width of the distribution, \( T \) is temperature, \( T_g \) is the mechanically determined glass transition temperature and \( C \) is a constant.

The experimental data of carbon/epoxy are taken from literatures [20–22]. Moduli are fitted using Eq. (15). The curve fitting parameters are listed in Table 1. The thermal properties of carbon/epoxy are listed in Tables 2 and 3. Properties for resin reaction model are listed in Table 4.

The strength of the composite is dependent on temperature and resin content. It has been assumed that the temperature variation of the ultimate longitudinal, transverse and shear strengths of carbon/epoxy follow the same pattern as the longitudinal, transverse and shear moduli, respectively, and resin content only affects the transverse and shear strengths. The virgin strengths used this investigation are listed in Table 5.
3.2. Composite failure criteria

As the composite wall of the hydrogen cylinder experiences combined mechanical and thermal loads, a variety of failure types would occur in the process. A progressive failure model is employed in this study to identify the failure types based on failure criterion and predict the safety state of the cylinder. Hashin’s failure criterion [23], accounting for four possible modes of ply failure, is used for this purpose.

1. Matrix tensile or shear cracking \((\sigma_{zz} + \sigma_{yy} > 0)\)
   \[
   I_{\text{mt}} = \frac{(\sigma_{zz} + \sigma_{yy})^2}{(F_{1}^L)^2} + \frac{\sigma_{12}^2 + \sigma_{13}^2 + \sigma_{23}^2 - \sigma_{22}\sigma_{33}}{(F_{1}^{LT})^2} \tag{16}
   \]

2. Matrix compressive or shear cracking \((\sigma_{zz} + \sigma_{yy} < 0)\)
   \[
   I_{\text{mc}} = \frac{1}{F_{1}^L} \left( \frac{F_{1}^L}{2F_{1}^{LT}} \right)^2 \left( \frac{1}{(F_{1}^{LT})^2} \right) (\sigma_{zz} + \sigma_{yy}) + \frac{(\sigma_{zz} + \sigma_{yy})^2}{4(F_{1}^{LT})^2} + \frac{\sigma_{12}^2 + \sigma_{13}^2 + \sigma_{23}^2 - \sigma_{22}\sigma_{33}}{(F_{1}^{LT})^2} \tag{17}
   \]

3. Fiber tensile fracture \((\sigma_{11} > 0)\)
   \[
   I_{\sigma} = \frac{\sigma_{11}}{F_{1}^L} \left( \frac{1}{(F_{1}^{LT})^2} \right) (\sigma_{22} + \sigma_{33}) \tag{18}
   \]

4. Fiber compressive fracture \((\sigma_{11} < 0)\)
   \[
   I_{\sigma c} = - \frac{\sigma_{11}}{F_{1}^L} \tag{19}
   \]

where \(F_{1}^L, F_{1}^T, F_{1}^S, F_{1}^{LT}\) and \(F_{1}^{LS}\) are longitudinal tensile strength, longitudinal compressive strength, transverse tensile strength, transverse compressive strength and shear strength of unidirectional ply, respectively.

Once a specific type of failure is identified in the laminate, the reduction in its load-carrying capacity is accounted for by a reduction in its elastic moduli based on Tan’s assumption [24]: (a) when matrix tensile or shear cracking occurs, properties \((E_{11}, G_{12}, G_{22})\) are taken as 0.2 times the original values. (b) when matrix compressive or shear cracking occurs, properties \((E_{11}, G_{12}, G_{22})\) are taken as 0.4 times the original values. (c) when fiber tensile fracture occurs, \(E_{11}\) is taken as 0.07 times the original values. (d) when fiber compressive fracture occurs, \(E_{11}\) is taken as 0.14 times the original values.

3.3. Model for hydrogen gas

The hydrogen gas in the cylinder absorbs energy and increases the internal pressure. In the present study, the hydrogen gas effect is modeled as a sink whose temperature is updated at each increment based on the amount of heat flux going through the inner cylinder surface. The internal pressure of cylinder is computed from the state equation of hydrogen. The experimental data of hydrogen gas are taken from literature [25]. The pressure and temperature relationship at constant density of 23.59 kg/m\(^3\) (when temperature is 20 °C and pressure is 34.5 MPa) is curve fitted as shown in Fig. 2. The fitted equation is employed to compute cylinder internal pressure.

\[
\text{Equation: } P = 1.2 T + 320
\]

Table 6 – Design parameters for the cylinder

<table>
<thead>
<tr>
<th>Parameters</th>
<th>Values</th>
</tr>
</thead>
<tbody>
<tr>
<td>Outside diameter (m)</td>
<td>0.462</td>
</tr>
<tr>
<td>Liner thickness (mm)</td>
<td>2.54</td>
</tr>
<tr>
<td>Composite thickness (mm)</td>
<td>11.3</td>
</tr>
<tr>
<td>Thickness ratio (helical/hoop)</td>
<td>0.65</td>
</tr>
<tr>
<td>Stress ratio (helical/hoop)</td>
<td>0.8</td>
</tr>
<tr>
<td>Operation pressure (MPa)</td>
<td>34.5</td>
</tr>
<tr>
<td>Factor of safety</td>
<td>2.25</td>
</tr>
</tbody>
</table>

Fig. 2 - Curve fitting for hydrogen gas state equation at constant density.

Fig. 3 - Finite element model of the cylinder.

4. Finite element simulation procedure

The cylinder dimensions used in present study are based on the design proposed by Mitlitsky et al. [26] and are summarized in Table 6. An axi-symmetric finite element model is built in ABAQUS as shown in Fig. 3. The length of cylinder part is taken as \(L_c = 0.3\) for analysis and the dome curve follows a...
geodesic path. The flame source is modeled as a constant heat flux (75 kW/m²) applied on the area with length Ls throughout the analysis. The radiation, as well as the convection, is also considered at the cylinder outside surface. Pressure is imposed on the inner liner surface. A symmetric boundary condition is applied at the end of cylinder to reduce the model to half of the cylinder. The model uses SAX8RT element accounting for both deformation and heat transfer. The cylinder wall consists of inner aluminum liner and six sub-laminates of carbon/epoxy (Fig. 4). Each hoop (90°) and helical (±10°) sub-laminate consists of many laminas.

The implementation procedure of the developed models in ABAQUS is summarized as shown in Fig. 5 and the names of interface subroutines are listed in parenthesis. At each increment, the material properties are updated (using subroutine USDFLD) based on the information of current temperature, failure type and resin content. The mechanical loading vector is computed from the hydrogen gas model (in the subroutine FILM). The hydrogen gas model provides sink temperature from which the internal pressure of the cylinder is computed (from subroutine DLOAD) using gas state equation. For thermal loading vector, the heat flux from flame source, resin decomposition and mass flux of burning gas contribute collaboratively (calculated in subroutine HETVAL). The coupled thermo-mechanical equations are then solved to get the global stresses and temperature field. The lamina stresses are retrieved from global stresses and imported into the failure model to compute failure indices. At the next increment, all sub-models (resin sub-model, hydrogen gas sub-model and material strength sub-model) are solved based on the temperature field and failure type from the previous increment. The procedure iterates until the specified time is met. In the present study, this transient analysis is carried out for 1000s.

5. Results and discussion

The heat exchange rate between the hydrogen gas and aluminum liner affects the increase in sink temperature (temperature of hydrogen gas) when the cylinder is subjected to flame impingement. In the simulation, the heat exchange rate is represented by a film coefficient (H). A parametric study is conducted on H ranging from 10 to 10,000 W/m²°C. The results are shown in Fig. 6. The sink temperature increases rapidly with H at the beginning and it flattens out at higher values of H. At higher H values, the sink temperature is not sensitive to the heat exchange rate. In real situations, this value can vary over a large range with variation of hydrogen flow rate. H is taken as 1000 W/m²°C.

![Fig. 4 – Stacking sequence of the cylinder wall.](image)

![Fig. 5 – Finite element implementation schemes.](image)

![Fig. 6 – Variation of sink temperature with film coefficient.](image)
for convenience throughout the analysis. Fig. 7 plots the internal pressure variation with time for different flame impingement sizes. The non-dimensional $L$ is defined as $L = L_d/L_c$ (Fig. 3). It can be seen that at the beginning the internal pressure increases very slowly and is much higher after 200 s. With increasing size of the flame area, the pressure goes up. For the rest of analysis, results are reported at $L = 0.1$.

Fig. 8 shows the temperature distributions of liner internal surface at different times. The temperature of flame impingement center is higher than the adjacent areas. Fig. 9 shows the temperature distributions of composite through the thickness direction at the flame center. The temperature of the outmost sub-laminate 6 increases to 520 °C in the first 200 s and then continues to go up very slowly during the rest of time. The innermost sub-laminate 1 increases slowly during the entire flame impingement process. Fig. 10 shows the residual resin content in the composite through the
thickness at the flame center. In the outmost sub-laminate 6, resin is depleted totally in the first 100 s and in the innermost sub-laminate 1, the resin content keeps almost constant throughout the entire process.

Stress distributions through composite thickness at the end of simulation time are reported in Figs. 11 and 12. Uneven stress ($S_{11}$) distribution is observed in fiber direction for hoop sub-laminates. This can result in the breakage of fibers in the inner hoop layers. The stresses in transverse direction ($S_{22}$) and thickness direction ($S_{33}$) are plotted in Fig. 12. The shear stresses are not reported as they are negligible.

Failures due to fiber fracture, matrix cracking and resin depletion are presented in Figs. 13 (a–c). Higher fiber fracture index is observed for the inner layer. This is because the fibers of outer layers cannot bear much mechanical load as the resin is either depleted or softened due to the high temperature. Matrix cracking is observed at the inner half of the composite wall. However, the matrix failure index is very low at the outmost layers, since there is not much matrix (resin) left. Hence, no significant mechanical loading can be carried in this part. This can be observed in Fig. 13(c).

6. Conclusions

A comprehensive finite element model has been developed to analyze composite hydrogen storage cylinders subjected to high pressure and flame impingements. The model considers the resin reaction, heat exchange between the hydrogen gas and liner and progressive failure process. A sub-laminate model has been developed to reduce computational time. A temperature dependent material model and failure model have been developed and implemented to accurately predict various types of failure for the hydrogen storage cylinder. All the models are formulated in an axi-symmetric coordinate system and implemented in ABAQUS through user subrou-
tines. A typical high pressure composite hydrogen cylinder under fire exposure is conducted by using the developed model. Parametric studies are done on heat exchange rate (between hydrogen gas and liner) and the sizes of flame source. Stresses, temperature profile and failure types are reported. The developed model can be used to accommodate various types of thermal and mechanical loading, lamina stacking sequence and lamina thickness to establish safe working conditions and design limits for hydrogen storage cylinders. In addition, this simulation process can be used for both type 3 and type 4 hydrogen cylinders, as both have the similar structure except the type of liner.

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REFERENCES


