Application of a Viscoplastic Damage Model in a 3D FSI Analysis of a Rocket Nozzle

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This paper presents the application of a recently developed unified viscoplastic damage model in a fluid-structure-interaction (FSI) analysis between the hot gas flow and the rocket thrust chamber structure. The constitutive modelling is motivated by the extension of the well-known rheological model of Armstrong-Frederick kinematic hardening elastoplasticity to a viscoplastic model. The coupling with isotropic ductile damage is performed following the concept of effective stress and the principle of strain equivalence. Upon successful numerical validations the model has been incorporated in a coupled FSI computation environment developed at the Institute of Aircraft Design and Lightweight Structures at Braunschweig University of Technology. The FSI analysis utilizes a loosely coupled fluid-structure-thermal interaction algorithm, in which the fluid and structural domains are addressed by well established individual solvers, namely the DLR-TAU code and the ABAQUS FE software. The individual domains of the rocket thrust chamber are modelled by a 3D parameterized approach. Results of the structural computations as well as the coupled FSI analysis between the hot gas flow and the thrust chamber structure will be presented.

1. Introduction

Regeneratively cooled nozzle structures of a Reusable Launch Vehicle (RLV) belong to the most critical components of a space shuttle main engine. During the operation it is subjected to extreme thermomechanical loadings leading to the thinning and bulging of the cooling channel wall. These typical deformations have been experimentally observed to cause the so-called ‘dog-house’ failure mode [1]. Within the initiative of the German Research Foundation to develop new technologies for the future transportation system, a material model enabling adequate lifetime prediction of the combustion chamber wall has been developed [2–4].

The material modelling is motivated by extending the well-known rheological model of Armstrong-Frederick kinematic hardening elastoplasticity into a viscoplastic model. The coupling with the isotropic ductile damage is performed following the principle of strain equivalence. The resulting constitutive equations are discretized in time using the implicit backward Euler integration scheme. The material model is implemented in the user subroutine UMAT for structural computations using the ABAQUS finite element software. Upon successful numerical validations, a fitting of experimental data available in literature is performed. Results from the thermomechanical analysis show the strong
influence of the kinematic hardening on the cyclic damage evolution. These topics are discussed in Sec. 2, Sec. 3 and Sec. 4.

This project aims at an efficient computational method enabling a reliable lifetime prediction for a rocket trust chamber. For this purpose, a realistic modelling of the cyclic thermomechanical processes using a coupled fluid-structure-interaction (FSI) analysis is needed. Coupled interactions which take place between the hot gas flow, the coolant flow and the thrust chamber structure need to be considered. The potential of the unified viscoplastic damage model and the whole FSI computation environment for failure prediction purposes will be discussed in Sec. 5. Results from a 3D FSI analysis between the hot gas flow and the trust chamber structure will be presented.

2. The viscoplastic damage model

This section discusses briefly the applied viscoplastic damage model. A detailed description of the model has been presented by the authors in [5]. As mentioned, the material modelling begins by extending the classical rheological model for elastoplasticity with Armstrong-Frederick kinematic hardening (see Fig. 1(a)) to include rate-dependent effects. This is done by adding a dashpot element with the viscosity parameter $\eta$ as shown in Fig. 1(b). In the small strain regime, a triple additive decomposition of the total strain $\varepsilon$ into elastic and inelastic parts, i.e. $\varepsilon = \varepsilon_e + \varepsilon_{pe} + \varepsilon_{pd}$ is employed.

2.1. The viscoplastic damage model within small strains formulation

Upon successful numerical validations in one-dimensional problems [4], the constitutive equations are generalized for three dimensions [5]. The stress-strain relationship is defined as $\sigma = C_1 [\varepsilon - \varepsilon_p]$, $C_1 = (1 - D) C_1$, where $C_1$ is the fourth-order elasticity tensor and $D$ is the isotropic ductile damage variable. The back stress tensor is defined as $X = C_2 [\varepsilon_{pe}]$, where $C_2$ is another elasticity tensor. The components of both elasticity tensors depend on the elastic moduli $E_1$, $E_2$ and the Poisson’s ratios $\nu_1$, $\nu_2$, respectively. The loading function takes the form

$$\Phi = \| \tilde{\sigma}^D - X^D \| - \sqrt{\frac{2}{3}} (\sigma_y + R)$$

(2.1)
where \((\cdot)^D\) represents the deviatoric term of the tensor. For the isotropic hardening function, the saturation hardening model of Voce

\[ R = Q_0 (1 - \exp(-\kappa \alpha)) \]  

is applied, where \(\alpha\) is the internal variable for isotropic hardening and \(Q_0\) and \(\kappa\) are material parameters. The evolution equations of the plastic strain \(\varepsilon_p\), the inelastic part of the plastic strain \(\varepsilon_{pi}\), and the accumulated plastic strain \(\alpha\) are given as follows:

\[ \dot{\varepsilon}_p = \frac{\lambda}{1 - D} \left( \frac{\tilde{\sigma}^D - X^D}{\tilde{\sigma}^D - X^D} \right), \quad \dot{\varepsilon}_{pi} = \lambda b \varepsilon_{pe}, \quad \dot{\alpha} = \sqrt{\frac{2}{3}} \lambda \]  

The isotropic damage variable \(D\) is assumed to evolve following the work of Lemaitre et al. [8]:

\[ \dot{D} = \sqrt{\frac{2}{3}} \frac{\lambda}{1 - D} \left( \frac{Y}{S} \right)^k H(\alpha - p_D), \quad Y = \frac{1}{2} \varepsilon_e \cdot C_1 [\varepsilon_e] \]  

Here \(p_D\) is the damage threshold and \(Y\) is the strain energy density release rate. The step function \(H\) is zero for \(\alpha < p_D\) and is equal to one for \(\alpha \geq p_D\). The rate of damage evolution is influenced by the material parameters \(S\) and \(k\). The plastic multiplier is defined to take the form

\[ \dot{\lambda} = \left( \frac{\Phi^m}{\eta} \right), \quad \Phi = \left( \frac{\tilde{\sigma}^D - X^D}{\sqrt{2} (\sigma_y + R)} \right) - 1 \]  

where \(m\) is a material parameter. In total the model has 13 parameters: \(E_1, \nu_1, E_2, \nu_2, \sigma_y, Q_0, \kappa, b, S, k, p_D, \eta, m\). The Poisson’s ratios \(\nu_1\) and \(\nu_2\) are assumed to be constant with respect to temperature. Other parameters depend on temperature.

3. Fitting with experimental data

The temperature dependency of the material parameters are defined by fitting the model response to tensile stress-strain curves of the NARloy-Z copper alloy taken from Esposito et al. [9]. The yield stresses and the kinematic hardening parameters are defined from these curves. The temperature dependency of these parameters is assumed...
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\[ \sigma_y \quad E_2 \quad b \quad \theta \]

\[
\begin{array}{cccc}
190 & 7.47 & 155 & 27.6 \\
158 & 7.84 & 150 & 294.3 \\
130 & 7.49 & 170 & 533.1 \\
90 & 7.71 & 225 & 810.9 \\
\end{array}
\]

**Table 1.** The value of the yield stress and the kinematic hardening parameters fitted at the test temperatures.

to be linear. The result of the fitting is shown in Tab. 1 and Fig. 2. It is assumed that damage has not taken place yet and viscous effects are neglected. The tests were performed at a strain rate of 0.002 s\(^{-1}\). Other mechanical and physical properties for the material such as the modulus of elasticity, the Poisson’s ratio, the density, specific heat, thermal conductivity and the coefficient of thermal expansion are also obtained from Esposito et al. [9]. Limitation on the availability of experimental data does not allow the identification of the damage and viscosity parameters, \(p_D\), \(S\), \(k\) and \(\eta\) and \(m\), respectively. For the following computations the values \(p_D = 0.0\), \(S = 5000\) MPa, \(k = 0.5\) and \(m = 1.0\) are applied.

4. Thermomechanical structural analysis of the combustion chamber segment

Prior to implementation in the coupled 3D FSI analysis, the applicability of the model for failure prediction purposes is investigated by means of structural thermomechanical analyses. First of all, a transient thermal analysis of a typical combustion chamber segment is performed to obtain the temperature history of the entire chamber segment. The temperature history is then used as input for a series of static analyses.

4.1. Transient thermal analysis

Figure 3(a) shows a typical rocket combustion chamber. The chamber cross section is depicted schematically in Fig. 3(b). Figure 3(c) shows the modelled segment. The outer shell is made out of the Nickel alloy Inconel 600, the cooling channel wall is made out of the NARloy-Z copper alloy. Convective thermal boundary conditions are employed at the inner and outer radii as well as in the cooling channel similar to the work of Riccius et al. [10]. The left and right sides have zero flux boundary conditions to ensure symmetry of the thermal field. The thermal cycle in Tab. 2 is applied in the analysis. Figure 4 shows some snapshots of the temperature field.

4.2. Static analyses

The temperature history obtained from the transient thermal analysis is used as input for a series of static analyses. For these analyses 8-node brick elements with reduced integration formulation were applied. The damage distribution obtained from the analyses are shown in Fig. 5. The pressure cycle in Tab. 3 is used for the static analyses. The damage distribution shows that the damage grows from the coolant side of the cooling
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(a) A typical rocket combustion chamber [11].
(b) A schematic cross section of a typical rocket combustion chamber.
(c) Modelled segment of a thrust chamber at the throat.

**FIGURE 3.** Schematic cross section of a typical rocket combustion chamber and the modelled segment.

<table>
<thead>
<tr>
<th>Phase</th>
<th>Time [s]</th>
<th>$T_{\text{hot gas}}$ [K]</th>
<th>$T_{\text{coolant}}$ [K]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Pre-cooling</td>
<td>0 - 2</td>
<td>40</td>
<td>40</td>
</tr>
<tr>
<td>Hot run</td>
<td>3 - 602</td>
<td>950</td>
<td>40</td>
</tr>
<tr>
<td>Post-cooling</td>
<td>603 - 604</td>
<td>40</td>
<td>40</td>
</tr>
<tr>
<td>Relaxation</td>
<td>604 - 620</td>
<td>293.15</td>
<td>-</td>
</tr>
</tbody>
</table>

**TABLE 2.** Thermal cycle applied for the transient thermal analysis.

<table>
<thead>
<tr>
<th>Phase</th>
<th>Time [s]</th>
<th>$T_{\text{hot gas}}$ [K]</th>
<th>$T_{\text{coolant}}$ [K]</th>
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<td>40</td>
</tr>
<tr>
<td>Relaxation</td>
<td>604 - 620</td>
<td>293.15</td>
<td>-</td>
</tr>
</tbody>
</table>

**FIGURE 4.** Temperature distribution at different phases of the assumed operational cycle: (a) pre cooling (b) hot run (c) post cooling (d) relaxation.

channel wall into the middle of the wall. The evolution of the damage variable at the element where the maximum damage occurs is shown in Fig. 6(a). Figure 6(b) shows the cyclic decrease of the equivalent von Mises stress corresponding to the same element. From this result it can be concluded that the cooling channel wall fails after 12 engine cycles. So far no bulging of the cooling channel wall has been observed. The viscoplas-
TABLE 3. Pressure cycle applied for the static analyses.

<table>
<thead>
<tr>
<th>Phase</th>
<th>Time [s]</th>
<th>$P_{\text{hotgas}}$ [MPa]</th>
<th>$P_{\text{coolant}}$ [MPa]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Pre-cooling</td>
<td>0 - 2</td>
<td>0</td>
<td>2</td>
</tr>
<tr>
<td>Hot run</td>
<td>3 - 602</td>
<td>10</td>
<td>14.5</td>
</tr>
<tr>
<td>Post-cooling</td>
<td>603 - 604</td>
<td>0</td>
<td>2</td>
</tr>
<tr>
<td>Relaxation</td>
<td>604 - 620</td>
<td>0</td>
<td>0</td>
</tr>
</tbody>
</table>

Figure 5. The damage distribution at the cooling channel wall.

(a) The cyclic evolution of damage over time. 
(b) The decrease of the equivalent von Mises stress over time.

Figure 6. Cyclic evolution of the damage and the equivalent von Mises stress over time.

tic damage model can so far be utilized for conservative lifetime prediction purposes. Incorporation of anisotropic damage and crack closure effect are necessary to describe the "dog-house" failure mode [12].

Figure 7(a) and Fig. 7(b) show cross sections of combustion chambers made out of NARloy-Z copper alloy and OFHC (oxygen free copper) alloy respectively. It is concluded by Hannum et al. [13] that depending on the liner material, the causes of the failure are different. In a NARloy-Z combustion chamber, the hot gas side wall does not bulge so much towards the chamber. Failure is then caused by low cycle fatigue. In an OFHC combustion chamber, the wall becomes significantly thinner. Cracking occurs after a necking phenomenon. These different failure causes could only be investigated taking
into account different liner materials. Experimental work with CuCr1Zr copper alloy is within the plan of the next phase of this project.

5. FSI analysis of a cooled subscale thrust chamber

Upon successful implementation for the structural thermomechanical analyses, the viscoplastic damage model is incorporated in the coupled 3D FSI computation environment. The simulation environment uses a partitioned approach, where each of the computational domains are analysed using individual codes. For the fluid simulation of the hot gas flow, the DLR TAU-Code is used. For the structural heat transfer as well as stress/displacement analyses of the cooling channel structure, the ABAQUS FE software is used. The coupled simulation environment has been validated for different code combinations as presented in [14–16].

For the fluid simulation, reservoir pressure inflow conditions were computed with the preliminary design tool Rocket Propulsion Analysis (RPA) [17] and served as inlet conditions for the DLR TAU-Code. The temperature of the hot gas reaches 3502 K at a pressure level of 9.35 MPa. The simulated rocket thrust chamber is a 40 kN subscale thrust chamber defined by Astrium Space Transportation GmbH, Propulsion & Equipment, which consists of 80 cooling channels in the combustion chamber and 160 cooling channels in the nickel based nozzle extension. The material setup is the same as shown in Fig. 3(c).

To reduce the computational cost, symmetry conditions are used in the 3D parameterized modelling approach, which models half of a cooling channel segment. The geometry of the whole nozzle structure as well as the grid of the computational domains has been generated by means of a full parametric approach, as described by the authors in [4–7]. For the coupled transient FSI analysis, the complete engine cycle consisting of the pre cooling, hot run, post cooling and relaxation phases are defined in Tabs. 2 and 3, except that in this analysis the boundary conditions of the hot gas side are replaced by the FSI coupling approach.

The fluid-structure interaction considered in this work is assumed to be transient for the structural heat transfer problem and steady state for the hot gas and the structural response. A two way coupled formulation accounts for the heat transfer and stress/displacement problem between the hot gas and the cooling channel structure. In each equilibrium iteration step the structural domain is solved in a sequentially coupled ther-
momechanical analysis, in which first the heat flux computed on the hot gas side serves as input for the structural thermal analysis and second the structurally computed temperature distribution together with the hot gas pressure loads is applied in a subsequent static stress / displacement analysis. The deformation results of the structural analysis are transferred to the fluid domain in order to perform a grid deformation in each iteration step. Furthermore, the surface temperatures computed by the structural solver are interpolated to the fluid side serving as new input in the equilibrium algorithm. The solution of the coupled problem is obtained by the Dirichlet-Neumann iteration, which is applied in each time step until convergence of the interface conditions is reached.

The three computational domains as well as the coupling surfaces are depicted in Fig. 8. The structural domain is represented by $\Omega_s$. The hot gas flow and the coolant flow domains are represented by $\Omega_{hf}$ and $\Omega_{cf}$ respectively. The coupling surface $\Gamma_{s,hf,full}$ enables the data transfer between the hot gas and the structural domain. In this study the conduction of the cold fluid domains is accounted for by the definition of film coefficients at the structural boundary.

The simulated hot gas heat flux acting on the chamber wall is transferred as boundary condition on the coupling surface $\Gamma_{s,hf}$. Constant film coefficients of $h_{f,NE} = 30$ kW/(m²K) and $h_{f,CC} = 150$ kW/(m²K) are applied for the combustion chamber coolant circuits and the nozzle extension circuits respectively. For both circuits the sink temperature is defined as $40K$.

The normalized temperature distribution of the coupled transient analysis at $t = 2.3$ s is shown along the thrust chamber contour in Fig. 9. The maximum temperature niveau at the combustion chamber inlet can be explained due to the assumption of finalized combustion at the inlet, where the hot gas reaches the wall without a fully developed boundary layer. Aware of this assumption one can determine the critical temperature peak in the combustion chamber section just in the vicinity upstream of the nozzle throat. The structural response by the applied heat flux results in a temperature rise up to $810.34$ K on the coupling surface at the throat region. The second temperature peak is reached at the position of the manifold, where the integral built copper combustion

![Figure 8. Software concept to model parametrized thrust chambers.](image-url)
chamber including 80 cooling channels is connected to the INCONEL nozzle extension with its 160 cooling channels cooled by a smaller hydrogen mass flux.

A cut through the structural temperature distribution at the throat region gives information about the heating influence inside the structure during the precooling and startup phase shown in Fig. 10. Starting at ambient the points A through C reach a stationary temperature level of about $40 \, \text{K}$ during the two second precooling phase. After 2 seconds the hot gas run starts heating the structure in 0.3 seconds up to a stationary temperature distribution. When a stationary level is reached, the temperature gradient between the points A and B reaches $286 \, \text{K}$ for the 1 mm thin copper wall.

Figure 11(a) shows the front view of the temperature distribution at the thrust chamber wall. The maximum temperature reaches $850 \, \text{K}$ upstream of the throat.
Figure 11. Temperature distribution of the thrust chamber obtained from a coupled 3D FSI analysis, taken at 602 seconds.

Figure 12. Thrust chamber contour deformation at steady state of the hot run.

shows the temperature distribution at the cross section of the thrust chamber taken at the throat.

The deformed state of the subsequent static stress/displacement analysis is shown in Fig. 12 and highlighted in red. The upstream axial deformation of the thrust chamber contour can be explained by the pressure difference between the hot gas side and the ambient side, where vacuum was assumed for the stress/displacement analysis. Figure 13 shows where the maximum damage is located just downstream of the throat, which is in the critical area.

Extension of the material modelling to include crack closure effect as well as incorporation of the coolant flow simulation are underway. With these, both the unified viscoplastic damage model and the FSI simulation environment can be fully utilized for failure prediction of the thrust chamber structure.

6. Conclusions and Outlook

In this work two main results have been presented. The first result concerns the application of the viscoplastic damage model for lifetime prediction of the thrust chamber wall. The assumption of pure kinematic hardening leads to the correct damage evolution pattern at the cooling channel wall. Damage starts to develop from the coolant side of the wall to the middle of the wall. Using isotropic ductile damage, a conservative lifetime prediction can be achieved. The second result concerns the application of the viscoplas-
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Figure 13. Side view of the damage distribution at the thrust chamber liner. The snapshot is taken at 602 seconds.

Viscoplastic damage model in an FSI analysis of a cooled 40 kN LOX/H₂ subscale thrust chamber, which has been performed for one typical liquid rocket engine cycle. The FSI computation shows the critical area of the thrust chamber structure just downstream of the throat where damage starts to occur, which is plausible if constant film coefficients are applied to model the coolant. This result confirms the potential of the loosely coupled approach in combination with a viscoplastic damage model for failure prediction purposes.

In the ongoing research we are focused on efficient transient analysis of complete engine cycles at in-service conditions in order to address the limiting lifetime factors of cooled combustion chambers. In this context different acceleration techniques concerning the coupling approach will be studied, e.g. quasi-Newton methods and the extraction of film coefficients for convergence acceleration. Furthermore, the cooling fluid domains analysed by RANS model will be integrated in the presented fully coupled approach. For the material modelling, the viscoplastic damage model will be extended to include anisotropic damage as well as crack closure effect.

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References


