



Development of a used fuel cladding damage model incorporating circumferential and radial hydride responses



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ABSTRACT

At the completion of the fuel drying process, used fuel Zry4 cladding typically exhibits a significant population of δ -hydride inclusions. These inclusions are in the form of small platelets that are generally oriented both circumferentially and radially within the cladding material. There is concern that radially-oriented hydride inclusions may weaken the cladding material and lead to issues during used fuel storage and transportation processes. A high fidelity model of the mechanical behavior of hydrides has utility in both designing fuel cladding to be more resistant to this hydride-induced weakening and also in suggesting modifications to drying, storage, and transport operations to reduce the impact of hydride formation and/or the avoidance of loading scenarios that could overly stress the radial inclusions.

We develop a mechanical model for the Zry4-hydride system that, given a particular morphology of hydride inclusions, allows the calculation of the response of the hydrided cladding under various loading scenarios. The model treats the Zry4 matrix material as J_2 elastoplastic, and treats the hydrides as platelets oriented in predefined directions (e.g., circumferentially and radially). The model is hosted by the Albany analysis framework, where a finite element approximation of the weak form of the cladding boundary value problem is solved using a preconditioned Newton–Krylov approach. Instead of forming the required system Jacobian operator directly or approximating its action with a differencing operation, Albany leverages the Trilinos Sacado package to form the Jacobian via automatic differentiation. We present results that describe the performance of the model in comparison with as-fabricated Zry4 as well as HB Robinson fuel cladding. Further, we also present performance results that demonstrate the efficacy of the overall solution method employed to host the model.

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1. Introduction

There are numerous processes that can potentially impact fuel handling after the drying and long term storage of used nuclear fuel. Cladding damage can occur throughout the life of the fuel; this damage might affect the retrieval of the fuel assemblies from the storage and transport casks given a loading event of sufficient intensity. As discussed in an Electric Power Research Institute (EPRI) summary article [27], the formation of radial hydrides in the cladding during drying may be one of the more important processes to consider. Further, as fuel storage is a recent endeavor, it is not possible to experimentally examine fuel after a period of long term storage. Thus, computational investigation of the impact of hydriding may be uniquely suited to best understanding the phenomena.

During reactor operation, water coolant is in continual contact with the Zircaloy (Zry4) fuel cladding under elevated temperatures

and pressures in a pressurized water reactor (PWR). A small amount of water decomposes, the disassociated oxygen attacks the outside surface of the Zry4 to form an oxide layer that is typically less than 100 μm thick (c.f. Fig. 2-2 in [27]). Some of the hydrogen goes into solid solution within the Zry4, the amount of which depends on the Zry4 temperature and the diffusion rate. There is often a gradient in the amount of hydrogen in solution, the amount typically decreases with distance from the coolant. Thus, the amount of hydrogen contained in Zry4 is typically discussed in terms of maximum wall thickness average (MWTA) values that tend to increase with burnup (c.f. Fig. 2-3 in [27]).

The solubility of hydrogen in Zry4 increases with temperature [7]. Discharged high-burnup cladding can contain up to 800 ppm hydrogen, some of which is typically precipitated as circumferentially oriented hydride platelets located in a rim near the outer surface of the fuel (when examined at room-temperature). During drying processes (held to a maximum of 400 °C peak fuel temperature), there is roughly 200 ppm of hydrogen in solution in the Zry4 that is available for precipitation as a radial hydride given an appropriate hoop stress. The cooling strategy following the peak

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drying temperature appears to affect the formation of radial hydrides. Hong and Lee [17] show that holding an applied hoop stress load at 300 °C for 7 h prior to cooling promoted extensive formation of radial hydrides.

Fig. 2–5 in [27] develops a description of the cooling rate for fuel in traditional storage,

$$T(K) = 517.48 + 155.52 \exp(-0.0877t) - 0.6922575t. \quad (1)$$

Such curves are constructed by considering a hypothetical fuel rod within a cask [13,28], consisting of an idealized heat transfer calculation where the thermal conductivity in the fuel and cladding is used to approximate the heat flux through these components to calculate an effective thermal conductivity of the cask and cask environment. A source term is used to generate the decay heat in the fuel. Fig. 5-1 in [28] shows a fit to the decay power (in Watts per assembly) over a 30 year storage history. The cask, remainder of the assembly, and the inert gas surrounding the fuel rod are modeled using an effective heat conduction value representative of the homogenized materials. The outer boundary of the cask is assumed to maintain a fixed temperature that is taken to be the maximum temperature seen in the surrounding storage environment (typically 40 °C).

The thermal conductivities of both irradiated fuel and cladding are well characterized. One may calculate a homogenized overall conductivity value by noting that the fuel temperature is limited to 400 °C during drying. Given the decay heat of the fuel, this maximum fuel temperature, the conductivities of the fuel and the clad, and the outside temperature of the cask, one may calculate an effective thermal conductivity for the environment such that the maximum fuel temperature is not exceeded during drying. Employing the resultant conductivity during the entire storage period results in a fuel temperature description Eq. (1). Note that this approach overpredicts the fuel temperature during storage, as the post-drying storage cask environment would transfer heat significantly more effectively than the partial vacuum present during drying. Fig. 1 attempts to approximate the increased conductivity of the helium-filled cask environment at one atmosphere backfill pressure.

Once the thermal profiles of the irradiated fuel and cladding are obtained, one can then estimate the hydride concentration and morphology. To investigate the mechanical response, it is then necessary to develop a mechanical model that describes the hydrided materials when they are subjected to loading. A series of EPRI reports [27,30,29], as well as [31], consider a damage-based multi-phase metal/hydride mixture model. The model is based on the cladding being composed of volume fractions of four distinct

components; base undamaged Zry4 metal, damaged (cracked) Zry4 metal, undamaged hydride platelet material, and damaged (cracked) hydride platelet material. In [11], the hydrided material is modeled using an anisotropic Gurson model, where the effect of hydrides is incorporated with a damage parameter that considers the behavior of the hydride inclusions in the material. In [36], an analytical model is proposed for calculating a criterion for crack growth in hydrided materials.

The mechanical model proposed in this paper explicitly considers the Zry4 matrix containing multiple hydride inclusions. Unlike previous work, the current model begins with a single stored strain-energy for the hydrided material, where the stress response is derived in a thermodynamically consistent manner, that results in a large deformation hyperelasto-plastic framework. The Zry4 matrix is modeled as an elastoplastic material, while the hydrides are represented as inclusions with preferred directions. The model has the flexibility to specify the volume fraction and damage evolution for each hydride inclusion. In this study, the volume fractions of the radial (f_r) and circumferential (f_c) hydride phases are specified independently, from an estimate of the general morphology of the specimen. For both orientation, the volume fractions are typically given in parts per million (ppm). The volume fraction of the matrix material is then $f_m = 1.0 - f_c - f_r$.

The remainder of the paper is structured as follows: Section 2 introduces the formulation for the underlying nonlinear boundary value problem that is the basis of the study, Section 3 presents some background information concerning finite deformation elastoplasticity, Section 4 details the model formulation, Section 5 addresses implementation issues, Section 6 presents benchmark cases where analytical solutions are used to verify model implementation, and finally Section 7 presents numerical examples and compares numerical simulations with available experimental data.

2. Nonlinear boundary value problem in elastoplasticity

This investigation begins by deriving a nonlinear elastoplasticity boundary value problem. We consider an elastoplastic body occupying a region $D \in \mathbb{R}^3$ in the reference configuration with a boundary $S = \partial D$. The boundary is assumed to admit the partitioning $S = S_\varphi \cup S_T$, where $S_\varphi \cap S_T = \emptyset$, and S_φ and S_T correspond to Dirichlet and Neumann boundaries, respectively. Material points in the reference configuration are labeled by their position vector $\mathbf{X}$. A deformation, $\boldsymbol{\varphi} : D \rightarrow \mathbb{R}^3$, is defined as an injective mapping that establishes a spatial, or deformed, configuration where material points have the new position vector $\mathbf{x} = \boldsymbol{\varphi}(\mathbf{X})$. The gradient of the deformation mapping $\mathbf{F} = \partial \boldsymbol{\varphi}(\mathbf{X}) / \partial \mathbf{X}$, is termed the deformation gradient. We further assume there exists a functional $I[\boldsymbol{\varphi}]$ known as the potential energy of the elastoplastic body, defined as

$$I[\boldsymbol{\varphi}] = \int_D [W(\mathbf{F}) - R B_i \varphi_i] dV - \int_{S_T} \bar{T}_i \varphi_i dS, \quad (2)$$

where $W(\mathbf{F})$ refers to the strain energy density of the body, B_i is the body force vector per unit mass, R is the material density in the reference configuration and $\bar{T}_i$ is the prescribed traction on the Neumann boundary. Taking variations of this functional yields the standard Euler–Lagrange equations corresponding to the strong form of the static elastoplastic governing equations

$$\text{Div } \mathbf{P} + \mathbf{R}\mathbf{B} = \mathbf{0} \text{ in } D,$$

$$\boldsymbol{\varphi} = \bar{\boldsymbol{\varphi}} \text{ on } S_\varphi,$$

$$\mathbf{P}\mathbf{N} = \bar{\mathbf{T}} \text{ on } S_T.$$

(3)

Here, $\mathbf{P} = \partial W(\mathbf{F}) / \partial \mathbf{F}$ is the first Piola–Kirchhoff stress, and $\mathbf{N}$ refers to the normal to the boundary in the reference configuration.

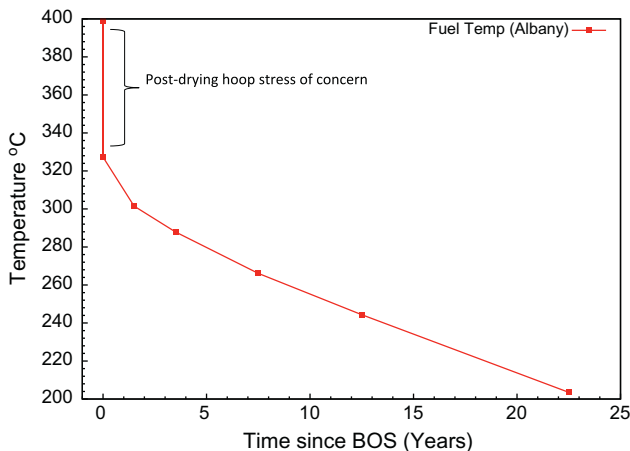


Fig. 1. Approximate fuel temperature for dry cask storage after helium backfill.

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