

1. Cavitation and Thermal Feedback in Annular Metallic Fuel

1.1 Introduction

Postirradiation metallography can reveal damage that is not apparent from measurements of overall fuel swelling. An example is the annular uranium–molybdenum fuel examined by Englander and coworkers [1]. A U–1.1 wt.% Mo specimen irradiated to 4000 MWd/t, with reported fuel temperatures spanning approximately 260–600 °C, developed a highly porous cylindrical band near the middle of the fuel wall (Figure 1.1). The band was about 1 mm wide, extended along the fuel, and was described as porous and friable. Englander reported a 10% decrease in density, corresponding to approximately 11% overall volume increase if mass is unchanged. The large local porosity was therefore accompanied by a relatively modest change in the overall fuel volume.



Figure 1.1: Transverse metallography of the annular U–1.1 wt.% Mo fuel examined by Englander and coworkers [1]. The specimen developed a localized porous and friable region near mid-wall after irradiation to 4000 MWd/t.

A porous band can substantially alter the radial heat-removal path. This possibility is particularly relevant to sodium-free metallic fuel systems, including gas- or helium-bonded annular designs, where bond sodium is unavailable to infiltrate interconnected cavities and improve their thermal conductivity. Temperatures calculated using the initial fuel geometry and properties may then underestimate the temperatures reached as damage develops. In the present reconstruction, the reported 260–600 °C range is treated as a nominal pre-cavitation temperature field; the report does not provide a measured time history of the local fuel temperatures.

The objectives are to develop an engineering-scale model of the cavitated region, explain its localization, and determine the consequences of the resulting porosity for fuel temperatures. The calculation also illustrates a practical use of metallography in model development: an observed microstructure identifies a physical mechanism and its spatial distribution, and a coupled model translates that information into consequences that can inform fuel design.

1.2 Methods

1.2.1 Empirical Model for Tearing-Induced Cavitation

The large cavities observed at relatively low burnup suggest that mechanical cavitation is an important contributor to the swelling. Leggett and coworkers [2] irradiated high-purity uranium over approximately 300–650 °C and found that fission gases contributed relatively little to the very large volume changes below 600 °C. Grain-boundary tears were observed at 400–500 °C, while crystallographically aligned internal porosity developed at 500–600 °C. McDonell [3] discussed how anisotropic irradiation growth produces internal stresses and how irradiation-produced voids can provide nuclei for the enlargement of cavities. The distinction between mechanical cavities and ordinary pressure-driven fission-gas bubbles, and the continuing importance of these mechanisms, are reviewed by Seshadri and coworkers [4].

The measurements summarized in Figure 1.2 show that the swelling response is strongly dependent on temperature and alloy composition. Small additions of alloying elements can greatly reduce the swelling coefficient and alter the temperature range over which substantial swelling occurs. These observations motivate an empirical temperature-dependent source for cavitation swelling. The source represents the net engineering-scale effect of tearing and related cavitation processes; individual grains and cavities are not resolved.

Let b denote burnup as a fraction of the initial heavy-metal atoms fissioned; thus, $b = 0.01$ denotes 1 at.% burnup. The cavitation source is written as

$$\dot{\epsilon}_V^{\text{tear}} = S(T)\dot{b}, \quad S(T) = S_{\max} \frac{g(T)}{g_{\max}}, \quad (1.1)$$

where

$$g(T) = L\left(\frac{T - T_1}{\Delta T_1}\right) L\left(\frac{T_2 - T}{\Delta T_2}\right), \quad L(x) = \frac{1}{1 + \exp(-x)}, \quad (1.2)$$

and $g_{\max} = \max_T g(T)$. The parameters T_1 and T_2 locate the transitions into and out of the active temperature interval, and ΔT_1 and ΔT_2 control the smoothness of those transitions. For the symmetric parameters used here, the maximum occurs at 500 °C. The volumetric source is applied isotropically, with one third of its increment assigned to each normal strain component.

The effective U–1.1Mo response used in the calculation is shown in Figure 1.3. It has a peak coefficient of 175, transitions centered at 450 and 550 °C, and transition widths of 15 °C. Because both volumetric strain and burnup are expressed as fractions, the numerical value of S is also the coefficient expressed as percent volumetric strain per at.% burnup. The effective curve is an adopted representation of the alloy's cavitation susceptibility, informed by the range of responses in Figure 1.2; it is not a direct measurement of U–1.1Mo.

1.2.2 Fission-Induced Creep and Porosity Evolution

The material response includes isotropic elasticity and fission-induced creep. The reference creep rate is taken to be proportional to the fission density rate, while the stress dependence

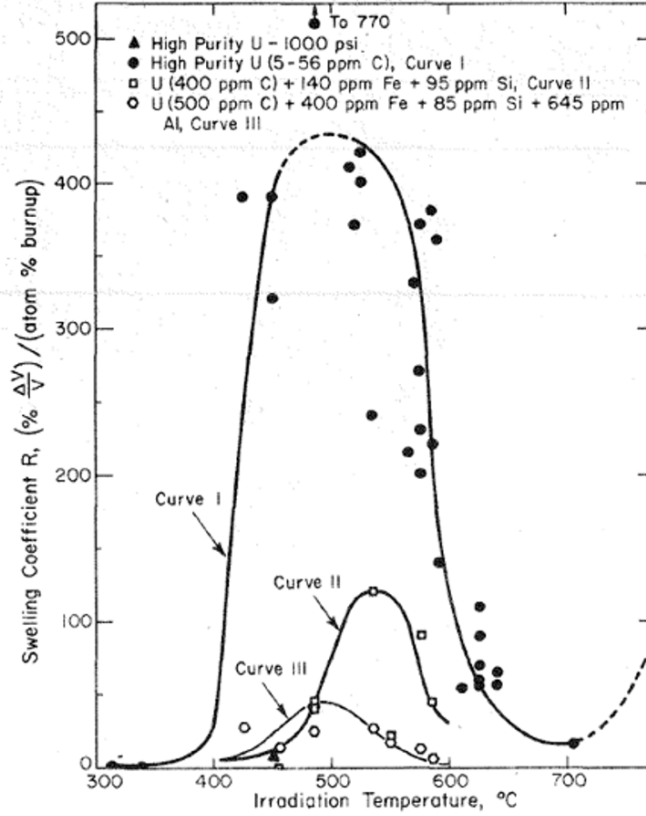


Figure 1.2: Temperature dependence of swelling in high-purity uranium and dilute uranium alloys. Reproduced from McDonnell [3], who attributes the underlying data to Leggett and coworkers (1965). The pronounced temperature dependence and sensitivity to alloy additions provide the basis for an empirical cavitation law.

is linear:

$$\dot{\epsilon}_{eq}^{matrix} = \dot{\epsilon}_0 \frac{\dot{F}}{\dot{F}_0} \frac{\sigma_{eq}^{matrix}}{\sigma_0}. \quad (1.3)$$

Here $\dot{F}$ is the fission density rate, and $\dot{\epsilon}_0$, $\dot{F}_0$, and σ_0 are the adopted reference parameters. The parameters provide a representative matrix creep response for the reconstruction; they are not presented as measurements on the Englander alloy.

The Duva–Crow porous flow description [5] incorporates the influence of pore fraction and hydrostatic stress. In the linear-creep form used here, the effective stress is

$$\Sigma^2 = A(v_f)\sigma_{eq}^2 + B(v_f)\sigma_H^2, \quad A(v_f) = \frac{1 + 2v_f/3}{1 - v_f}, \quad B(v_f) = \frac{3v_f}{4(1 - v_f)}, \quad (1.4)$$

where v_f is pore volume fraction, σ_{eq} is the macroscopic von Mises stress, and σ_H is the mean stress, positive in tension. No pore-pressure contribution is prescribed in this calculation. The associated creep flow is directed along $\partial\Sigma/\partial\sigma$, with its magnitude determined by the reference rate and Σ/σ_0 . This gives a volumetric creep response as well as deviatoric deformation, allowing stresses produced by nonuniform swelling and restraint to influence porosity evolution.

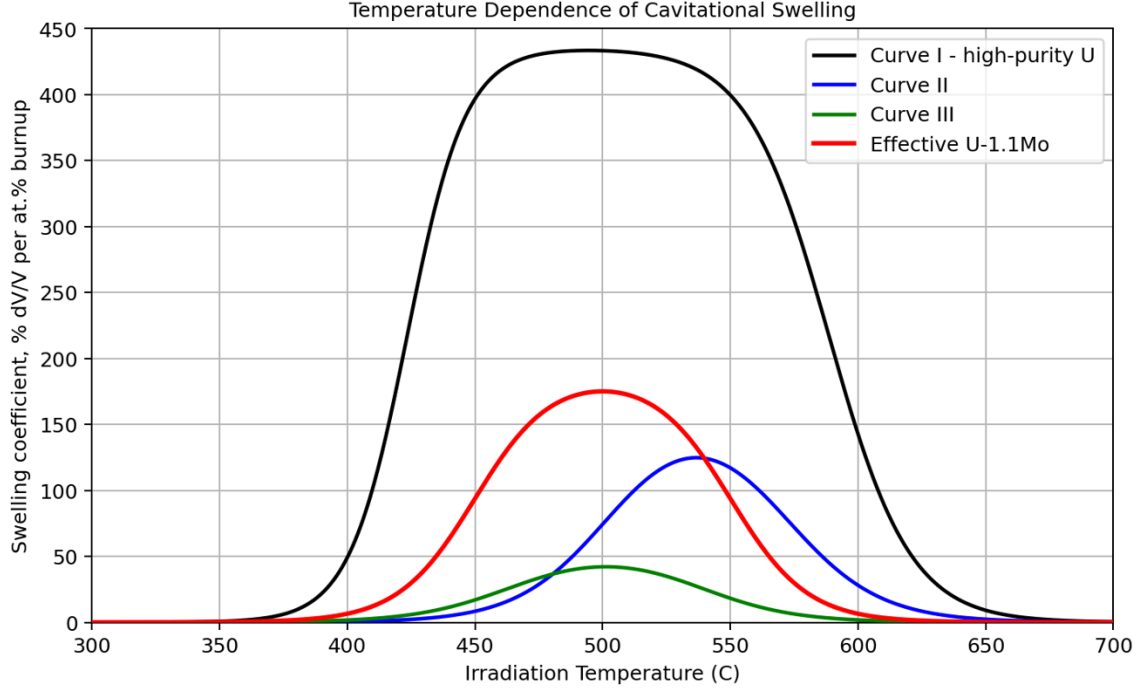


Figure 1.3: Representative empirical swelling responses and the effective U–1.1Mo cavitation law used in this calculation. The red curve is defined by Equations (1.1) and (1.2); the other curves illustrate the magnitude and temperature dependence of the historical responses.

Conservation of solid volume gives the porosity evolution relation

$$\dot{v}_f = (1 - v_f) (\dot{\epsilon}_V - \dot{\epsilon}_V^{\text{solid}}), \quad (1.5)$$

where $\dot{\epsilon}_V$ and $\dot{\epsilon}_V^{\text{solid}}$ denote the volumetric strain rates of the fuel/porosity composite and solid matrix, respectively. For the inelastic evolution used in this calculation, solid-matrix volumetric swelling and material transport are inactive, so

$$\dot{v}_f = (1 - v_f) [\dot{\epsilon}_V^{\text{creep}} + S(T)\dot{b}]. \quad (1.6)$$

The cavitation source therefore contributes once to the composite deformation and corresponding pore-volume increase. The starting pore fraction is 0.001.

1.2.3 Effect of Porosity on Thermal Conductivity

The description of the cavitated region as friable suggests a substantial interruption of the solid heat-conduction paths. An effective-medium relation is used to represent this effect over a broad range of pore fractions:

$$k(T, v_f) = k_{100}(T)(1 - v_f)^{3\epsilon/2}, \quad (1.7)$$

where k_{100} is the fully dense alloy conductivity and ϵ is an effective pore-morphology correction. With $\epsilon = 1$, the relation reduces to the differential-Bruggeman result for nonconducting

spherical pores. Bauer’s treatment of porous media [6] and the metallic-fuel work of Bauer and Holland [7] provide the effective-medium and experimental context for this approach.

The present reconstruction adopts $\epsilon = 1.72$, giving an exponent of 2.58, to represent the stronger interruption of heat flow by the cavitated morphology. A shape factor of 1.72 appears in metallic-fuel conductivity correlations that include irregular sodium-filled pores [8]; its use here is an adopted morphology correction for the cavitated region. It is not a measured conductivity parameter for the Englander specimen. For illustration, at $v_f = 0.45$, Equation (1.7) gives $k/k_{100} \simeq 0.21$, compared with about 0.41 for the spherical-pore baseline. The pores are treated as nonconducting relative to the alloy, and sodium infiltration is absent.

1.2.4 Geometry, Conditions, and Numerical Model

The calculation uses a two-dimensional, plane-strain quarter-annulus with symmetry conditions on its radial edges. Structural and thermal fields are represented by four-node linear elements, with 33 uniformly spaced radial elements and eight elements over 90 degrees. The inner fuel surface is thermally insulated and the outer surface is held at 260 °C. The inner and outer surfaces have no prescribed mechanical traction; the model resolves the fuel and does not explicitly include a cladding layer. These conditions reconstruct the nominal radial temperature range and isolate the coupled response of the fuel.

The nominal heat source is specified per unit original fuel volume. As the fuel deforms, it is converted to a current-volume source using $Q = Q_0 V_0 / V_t$, where V_0 and V_t are the original and current material volumes. The thermal field satisfies

$$\rho c \dot{T} = \nabla \cdot [k(T, v_f) \nabla T] + Q. \quad (1.8)$$

The unit-valued density and heat-capacity coefficients in the input provide numerical thermal storage; the interpretation concerns the evolved temperature profiles on the much longer irradiation timescale. Burnup and fission density increase uniformly and linearly over the prescribed irradiation duration. Nonlinear geometry is active, and structural and thermal solutions are updated sequentially. Table 1.1 lists the input parameters and adopted conductivity correction.

Table 1.1: Parameters for the Englander reconstruction. Geometry and thermal conditions are reconstructed; the creep and cavitation coefficients are adopted effective properties. The morphology correction is the value adopted for the presented analysis.

Parameter	Value
Alloy composition	U–1.1 wt.% Mo
Inner and outer fuel radii	6.50 and 14.75 mm
Radial and circumferential elements	33 and 8 (quarter-annulus)
Initial pore fraction, v_{f0}	0.001
Outer-surface temperature	260 °C
Inner-surface thermal condition	Insulated
Nominal power density, Q_0	0.410296 W/mm ³
Fully dense conductivity, k_{100}	0.032 W/mm-K
Adopted morphology factor, ϵ	1.72
Young's modulus and Poisson's ratio	10 ⁵ MPa and 0.30
Cavitation coefficient, $S_{\max}$	175 per unit burnup fraction
Transition temperatures, T_1, T_2	450 and 550 °C
Transition widths, $\Delta T_1, \Delta T_2$	15 and 15 °C
Creep reference rate, $\dot{\epsilon}_0$	1.5 × 10 ⁻³ s ⁻¹
Creep reference fission rate, $\dot{F}_0$	2.0 × 10 ¹⁷ fissions/(mm ³ s)
Creep reference stress, σ_0	100 MPa
Final burnup fraction	$b_f = 0.0042629688$ (0.4263 at.%)
Final fission density	1.973715 × 10 ¹⁷ fissions/mm ³
Irradiation duration, t_f	1.541443 × 10 ⁷ s (178.4 days)

1.3 Results

Figure 1.4 compares the observed morphology with the predicted pore fraction and temperature at the end of irradiation. The calculation produces a narrow annular region of high porosity, with a maximum pore fraction of approximately 0.45–0.46. This reproduces the principal spatial feature of the metallography: substantial cavitation is concentrated within the fuel wall rather than at its hottest inner surface or throughout the entire section. The calculated interior temperature reaches approximately 815–820 °C, about 200 °C above the nominal pre-cavitation maximum.

The evolution shown in Figure 1.5 explains how the localized band develops. Initially, a relatively broad region lies within the temperature range susceptible to cavitation. Increasing pore fraction reduces its conductivity, raising temperatures in the fuel inward of the damaged region. As those material locations become hotter than the range of maximum cavitation susceptibility, their subsequent cavitation rate declines. Material farther outward remains within the susceptible interval and continues to accumulate porosity. The porosity peak consequently moves outward and sharpens as irradiation proceeds.

The temperature increases are concentrated inward of the porous band, which acts as an internal thermal barrier. Previously accumulated porosity persists on the inner flank; narrowing refers to the sharpening of the peak and localization of continuing rapid cavitation.

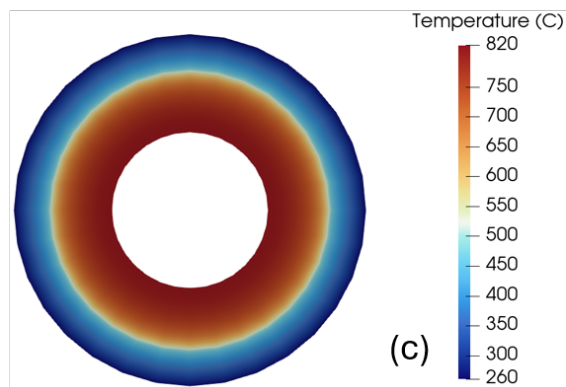
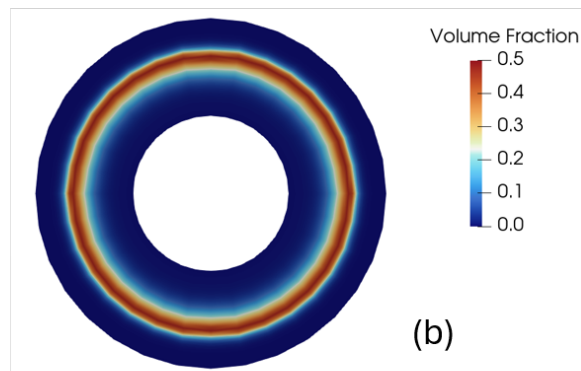
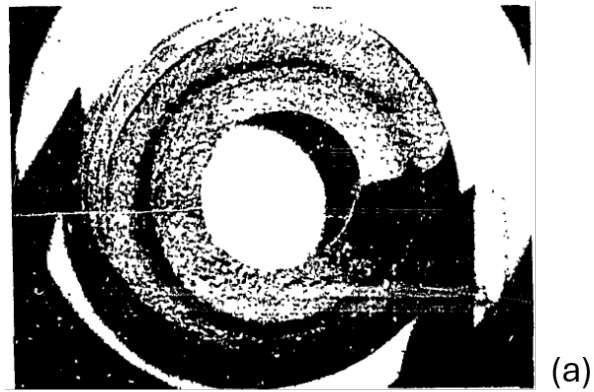


Figure 1.4: Observed and calculated annular cavitation: (a) Englander's metallography; (b) predicted pore volume fraction at the final burnup of 0.4263 at.%; and (c) corresponding temperature field. The localized porous band introduces a substantial thermal resistance between the hot fuel interior and the cooled outer surface.

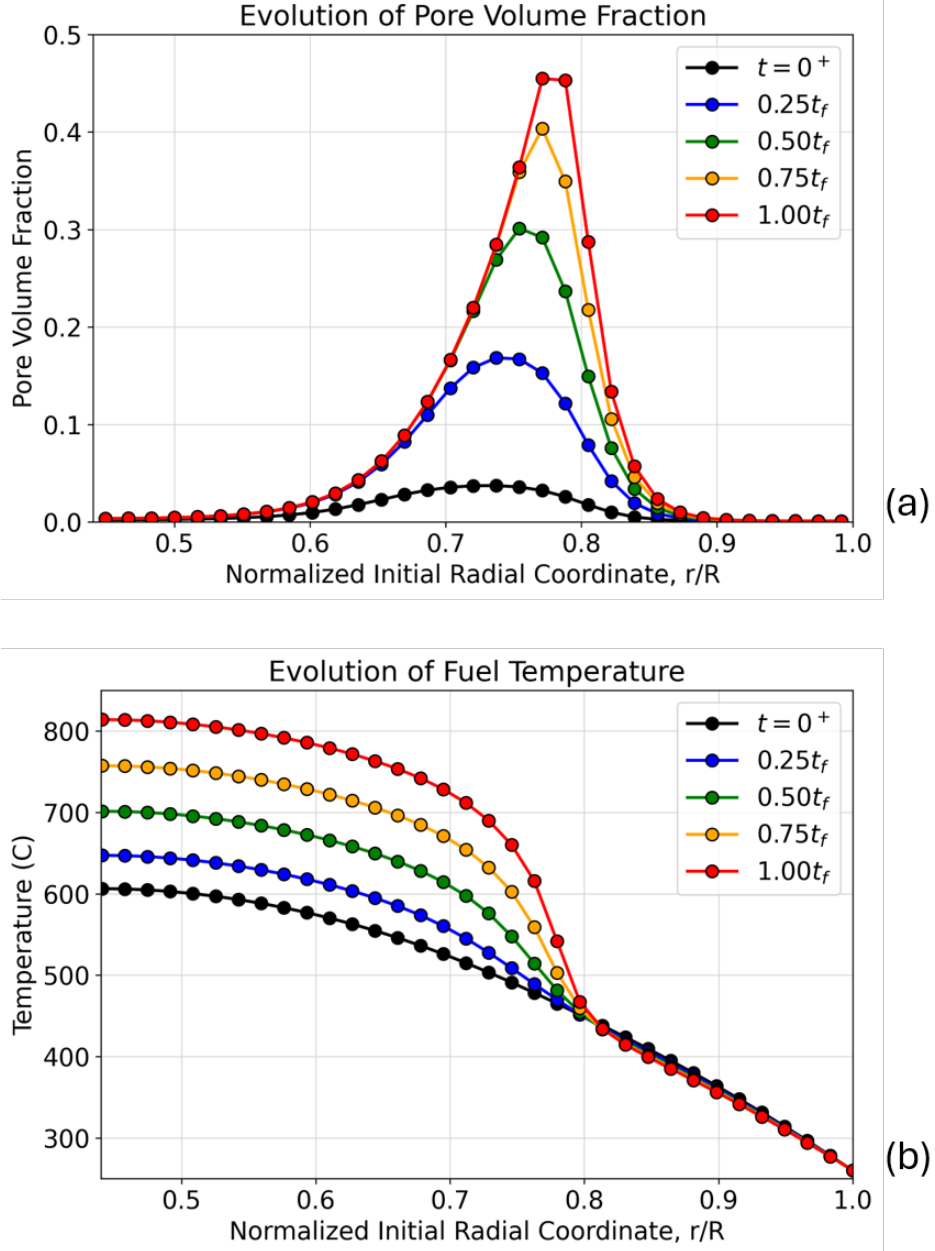


Figure 1.5: Evolution of pore volume fraction (upper panel) and fuel temperature (lower panel) versus the initial radial coordinate normalized by the initial outer fuel radius, R . The profiles labelled $t = 0^+$ represent the earliest saved powered state, which already contains a small accumulated cavitation increment. The remaining profiles are at 0.25, 0.50, 0.75, and 1.00 of the final irradiation time. Increasing porosity raises interior temperatures and concentrates subsequent cavitation toward the outer part of the fuel.

1.4 Discussion

The agreement in morphology supports tearing-induced cavitation as an explanation for the Englander observation. More importantly for fuel design, the coupled calculation shows that the cavitated region can produce a substantial change in the operating temperature distribution. An overall density or swelling measurement does not specify where the damage occurs. Its radial distribution determines how much of the generated heat must pass through the degraded material, and therefore how strongly that damage affects the fuel interior.

The broad nominal 260–600 °C temperature range places the susceptible region within the fuel wall. For a sodium-cooled fast reactor whose fuel-surface temperature lies within the cavitation-susceptible range, the corresponding region can occur near or at the fuel/cladding interface. That is a particularly unfavorable location for an insulating band because heat generated throughout almost the entire fuel section must cross it. Cavitation near this interface can therefore be consequential even when overall dimensional changes remain modest. Sodium bonding can partially offset the conductivity loss when sodium enters interconnected pores [7, 8]; this mechanism is unavailable to a sodium-free bond.

Related observations in modern metallic fuel reinforce the need to examine spatially heterogeneous porosity. Carmack [9], for example, documented radial compositional zones and high porosity in irradiated U–10Zr fuel. Such observations provide a basis for evaluating cavitation susceptibility in the local phase and composition of an intended fuel, rather than treating the nominal alloy composition as a complete description of its irradiation response.

In the present calculation, a pore fraction approaching one half and approximately 200 °C of additional interior heating develop by only 0.4263 at.% burnup. Fast-reactor fuel is intended to operate to substantially greater burnup; the U–10Zr specimens discussed by Carmack reached approximately 10 at.%. Continued irradiation gives susceptible material additional opportunity to accumulate damage, although the evolving temperature and stress fields can alter where that damage occurs. The result identifies a design risk at an early stage of fuel life and warrants explicit consideration of localized cavitation in sodium-free fuel models.

The calculation uses an empirical cavitation source and representative material properties. Its principal engineering value is the connection between an observed microstructure, a plausible physical mechanism, and a consequential thermal response. Dedicated measurements of cavitation and irradiation creep would improve the coefficients, but design work can already use the model to investigate changes in alloy, fuel geometry, and heat removal paths that reduce the risk. The calculated temperatures above the original reported range also identify where phase dependent properties will matter in subsequent design analyses.

This example demonstrates why linking metallography and fuel modeling is important for qualification. The microstructure reveals a localized damage process; the coupled model shows its effect on operating conditions and provides a basis for mitigating the risk before committing to a lengthy irradiation campaign.

References

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