An Energy Based Fatigue Lifing Method for In-Service Components and Numerical Assessment of U10Mo Alloy Based Fuel Mini Plates

DISSERTATION

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Abstract

An energy based fatigue life prediction framework has been developed for calculation of remaining fatigue life of in service gas turbine materials. The purpose of the life prediction framework is to account aging effect caused by cyclic loadings on fatigue strength of gas turbine engines structural components which are usually designed for very long life. Previous studies indicate the total strain energy dissipated during a monotonic fracture process and a cyclic process is a material property that can be determined by measuring the area underneath the monotonic true stress-strain curve and the sum of the area within each hysteresis loop in the cyclic process, respectively. The energy-based fatigue life prediction framework consists of the following entities: (1) development of a testing procedure to achieve plastic energy dissipation per life cycle and (2) incorporation of an energy-based fatigue life calculation scheme to determine the remaining fatigue life of in-service gas turbine materials. The accuracy of the remaining fatigue life prediction method was verified by comparison between model approximation and experimental results of Aluminum 6061-T6. The comparison has shown promising agreement, thus validating the capability of the framework to produce accurate fatigue life prediction. The second part of this dissertation is related to the development of a new Finite Element Methodology and lifing assessment for U10Mo Monolithic fuel mini plates subject to actual irradiation conditions. Three distinct stages were considered: (1) Fabrication induced residual stresses due to the high temperature exposure, (2) Blister annealing to
check the structural stability of the fabricated plates with consideration of the residuals; and finally (3) in-reactor mechanical behavior with the consideration of irradiation induced phenomena. As the Hot Pressing temperature during the fabrication process approaches the melting temperature of the cladding material, using material properties at these temperatures were crucial for the accuracy of the residual stress field and for the subsequent simulations. In addition, since solid mechanical and fluid properties are temperature dependent, inter-disciplinary simulations had to be considered. Once residuals and plastic strains due to HIP (Hot Isostatic Pressing) process were identified via elasto-perfectly plastic model, solution was used as initial condition for the subsequent simulations. For Blister simulation, thermo-elasto-plastic material model with thermally induced creep was constructed and residuals due to the fabrication process from Step 1 were implemented accordingly. For irradiation (in-service) simulation, coupled fluid-thermal-structural interaction had to be considered due to the thermal dependency of the mechanical, thermal and fluid properties. Once the thermal field was identified on plates, this field was used to simulate the stress field with the consideration of thermal creep of cladding, irradiation induced creep and swelling of the fuel foil. The analysis showed that the residual stresses dominate the mechanical response of the plate. Even though, irradiation induced creep caused stress relaxation on the plates, swelling acted as a counter effect.
Dedication

Dedicated to my mother
Acknowledgments

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Nomenclature

\begin{itemize}
\item \textbf{A} \quad \text{Power law multiplier}
\item \textbf{C} \quad \text{Scalar factor}
\item \textbf{C}_p \quad \text{Specific Heat}
\item \textbf{D} \quad \text{Elasticity matrix}
\item \textbf{E} \quad \text{Modulus of Elasticity}
\item \textbf{G} \quad \text{Shear Modulus}
\item \textbf{K} \quad \text{Thermal conductivity}
\item \textbf{m} \quad \text{Time exponent}
\item \textbf{n} \quad \text{Stress exponent}
\item \textbf{N} \quad \text{Number of cycle to failure}
\item \textbf{Q} \quad \text{Heat source}
\item \textbf{T} \quad \text{Temperature}
\item \textbf{t} \quad \text{Time}
\item \textbf{u}_i \quad \text{Displacement}
\item \textbf{W}_c \quad \text{Strain energy dissipated per cycle}
\item \textbf{W}_m \quad \text{Strain energy for monotonic case}
\item \textbf{\alpha}_{kl} \quad \text{Tensor for coefficient of thermal expansion}
\item \textbf{\beta}_0 \quad \text{Material parameter}
\end{itemize}
\( \beta_i \) Material parameter

\( \varepsilon \) True strain for monotonic case

\( \varepsilon_a \) True cyclic strain

\( \varepsilon_{ij} \) Strain Tensor

\( \varepsilon_f \) True strain at the fracture

\( \varepsilon_n \) True strain at the necking

\( \varepsilon_0 \) Material parameter

\( \bar{\varepsilon} \) Equivalent plastic strain

\( \dot{\varepsilon}^{cr} \) Equivalent creep strain rate

\( \sigma \) True stress

\( \sigma_a \) Alternating stress amplitude

\( \sigma_c \) Material parameter

\( \sigma_n \) True stress at the necking

\( \sigma_{ij} \) Stress tensor

\( \sigma_0 \) Material parameter

\( \sigma' \) Deviatoric Kirchhoff stress

\( \overline{\sigma} \) Equivalent deviatoric stress

\( \sigma_s \) Yield stress

\( \rho \) Density

\( \nu \) Poisson ratio
Chapter 1: Introduction

The research presented in this dissertation considers two topics. The first section is related to development of an energy based fatigue life prediction framework. The second section of this research is related to development of Finite Element Models for the evaluation efforts for the proposed monolithic and dispersion U-Mo alloy based fuel plates to be used in test and research reactors.

Part 1: Development of an Energy Based Fatigue Criteria

Overview

First portion of this work introduces a fatigue life prediction framework. Previously developed energy based fatigue life prediction framework has been improved for calculation of remaining fatigue life of in service materials. The framework for the prediction of fatigue life via energy analysis consists of constitutive law which correlates the cyclic energy to the amount of energy required to fracture a material. However, it is shown that the amount of energy dissipated per cycle depends on the operating frequency. Furthermore, due to the micro structural changes of the material, strain hardening, softening or ratcheting may occur during the life span. In order to accurately incorporate these phenomena into the mathematical constitution, a detailed experimental and analytical investigation was carried out. The energy-based fatigue life prediction framework consists of the following entities: (1) Development of a testing procedure to
achieve plastic energy dissipation, (2) Investigation frequency effects on dissipated energy per cycle and separation of anelastic components, (3) Investigation of microstructural transition during the fatigue loading and finally (4) incorporation of an energy-based fatigue life calculation scheme to determine the remaining fatigue life of in-service gas turbine materials. The accuracy of the remaining fatigue life prediction method was verified by comparison between model approximation and experimental results.

Significance, Scope and Objectives

For quite some time, scientific investigators have been intrigued by the relationship concerning energy conversion and the fatigue life of materials. In the 1940’s and 50’s, a number of attempts to relate strain energy per cycle to the number of cycles for failure were considered and resulted in minimal success [1]. Success in this area was achieved by introducing the hypothesis stating that under cyclic loading, there exists a critical energy value for which failure occurs [2]. This hypothesis was later justified by displaying agreement between the theoretical and the experimental results on an S-N curve. Further investigation of the assumption made in [2] led to the introduction of a more sufficient correlation between the fatigue life of a material and the strain energy dissipation during the process [3]. It is now understood that the strain energy required to fracture a material, monotonically, is the same as the strain energy during a cyclic fatigue procedure, thus iterating that the critical energy value for each material is the monotonic strain energy. Based on this theory, an improved energy-based criterion has been
developed to systematically determine fatigue life based on the amount of energy loss per fatigue cycle [4, 5].

To further improve the energy-based fatigue assessment of turbine blades, an improved energy-based framework has been developed for systematical determination of fatigue life by dividing critical strain energy with strain energy per cycle. This method consists of the following capabilities: uniaxial fatigue life at various stress ratios [4], tension/compression fatigue life prediction [5], bending fatigue life prediction [6], and shear fatigue life prediction [7]. The previously developed energy based fatigue model [4-7] assumed constant energy dissipation per cycle during the entire fatigue life. This assumption was adequate for most of the high cycle fatigue life but becomes questionable when internal micro-cracks are initiated during the final stage of the life.

Cyclic frequency has significant impact on hysteresis loop, and therefore, it should be studied in detail. Depending on the material, selecting right frequency for computation of actual plastic strain energy is very important. Even though there some, published work in this subject is still scarce. During cycling loading, depending on material microstructure, softening or hardening may be observed. For instance, Mayer and Laird studied frequency effect on a Copper specimen over its yield limit by using two different frequency levels (0.5 and 2 Hz) and observed different behavior at different stages [8]. Similarly, Zhang and Jiang studied initial stages of multi-axial cyclic loading of 1045 Steel under its yield limit, and reported 3 distinctive stages at single frequency level
(0.25Hz) before reaching to convergence [9]. Accordingly, it is important to note that especially in LCF, various regimes such as; cyclic hardening, cyclic softening or ratcheting may occur. Because energy based approaches rely on strain energy density per cycle and the strain energy could change due to the cyclic behavior of the material, it is crucial to examine and understand the cyclic response of the material in detail. A hysteresis loop recorded at a random cycle might not be accurate for plastic energy densities of other cycles; and consequently, using this plastic energy to estimate fatigue strength of the material might lead to inaccurate results and poor conclusions. Therefore, cyclic behavior and its evolution of the material during the whole fatigue process must be studied and well understood before postulating a physically accurate mathematical constitution.

Furthermore, it is known that in LCF, only a small portion of the applied mechanical energy contributes to the actual damage and rest is essentially converted into heat. In HCF, damage is created slowly by dislocation pile-ups due to the very small changes in the microstructure. Since the variety of engineering materials widely used in the aircraft industry, such as Aluminum, Titanium, high strength steels and Nickel alloys do not exhibit apparent fatigue limit at high cycle fatigue regime [10-11], relying on traditional stress based infinite-life criterion for design purposes would not be accurate for these materials. Therefore, a thorough analysis of hysteresis loops at various frequencies and cycles should be carried out to understand material cyclic behavior. By this mean, a true
representation of plastic strain energy per cycle which actually contributes to the damage can be computed.

In this work, frequency effect was addressed in more details. It was shown that anelastic components become dominant at higher operating frequencies and convergence would be achieved at very low frequencies. In addition, the previous fatigue life prediction capabilities were addressed more extensively to determine the changes of the hysteresis plastic energy dissipation during the life time/cycles, especially, the final stage of the fatigue life. Transition of microstructure caused by cyclic loading was investigated in detail via SEM techniques. Finally, an improved energy-based cycle dependent fatigue criterion was presented.

Part 2: Development of Finite Element Models for U10Mo Fuel Plates

Overview

Second portion of the research relates to development efforts of structural simulations for proposed monolithic and dispersion U-Mo alloy based fuel plates to be used in research reactors. For this purpose, multiple Finite Element Models were developed. The detailed 3D thermo-mechanical Finite Element Models of the proposed monolithic fuel plates was used to benchmark the plates. Three distinct steps were considered and simulated: (1) Fabrication induced residual stresses due to the high temperature exposure, (2) Blister annealing to check the structural stability with consideration of the residuals; and finally (3) transient mechanical response during actual operating conditions with consideration
of irradiation induced phenomena. As the Hot Pressing temperature during the fabrication process approaches the melting temperature of the cladding, using material properties at these temperatures were crucial. In addition, since solid mechanical and fluid properties are temperature dependent, coupled physics simulations had to be considered. Once residuals and plastic strains due to HIP (Hot Isostatic Pressing) process were identified via parametric models, solution was used as initial condition for subsequent simulations. For Blister simulation, thermo-elasto plastic material model with thermally induced creep was constructed and residuals due to the fabrication process from step 1 were implemented accordingly. For irradiation (in-service) simulation, coupled fluid-thermal-structural interaction had to be considered. Once the thermal field was identified over the domain, this field was used to simulate the stress field with the consideration of thermal creep of cladding, irradiation induced creep and swelling of the fuel foil. The analysis showed that the residual stresses dominate the mechanical response of the plate. Even though, irradiation induced creep causes stress relaxation over the domain, swelling acted as a counter effect. It has been shown that stress gradients at the interface are significant.

Significance, Scope and Objectives

Initiated in the late 1970’s, the primary objective of the Reduced Enrichment for Research and Test Reactors (RERTR) program was to develop technology to minimize the use of Highly Enriched Uranium (HEU) for most civilian applications. The RERTR fuel development program aims to develop fuel types that would substitute highly enriched uranium with proliferation resistant, low enriched uranium (<20% $^{235}$U) for
research reactors [12]. However, lower densities of uranium result in a decrease of fission rate. Consequently, higher volume would be required to compensate for lower fission rate caused by lower densities. Many research reactors can be satisfactorily operated with silicon based (U₃Si₂-Al) dispersive fuels lower (up to 5 g/cm³) densities. However, several high performance reactors such as ATR (Advanced Test Reactor), HFIR (High Flux Isotope Reactor), MITR (Massachusetts Institute of Technology Reactor), NBSR (National Bureau of Standards Reactor), and MURR (Missouri University Research Reactor) require uranium densities of up to 10 g/cm³ in monolithic form. The major challenge associated with those types of fuel is stability. High density uranium fuel should remain stable during irradiation as well as during the fabrication process. Among several proposed alloys, U-Mo based fuels are considered to be the most promising candidate for fuels of higher densities. Molybdenum extends the stability of the gamma phase. This is desirable because this phase is known to be stable under typical irradiation conditions. U-Mo has a low neutron caption cross-section, good irradiation behavior, and acceptable swelling response [13, 14 and 15].

Monolithic fuel plates comprised of U-Mo alloy based foils encapsulated in aluminum alloy cladding are proposed for conversion of high performance research reactors to low enriched uranium fuel reactors. Three different fabrication techniques have been considered and evaluated at INL for the fabrication: Hot Isostatic Pressing (HIP), Friction Stir Welding (FSW), and Transient Liquid Phase Bonding (TLPB). Of these three
techniques, the HIP technique provides better mechanical behavior. As a result, this study focuses on monolithic plates fabricated via the HIP technique.

The process of fabricating monolithic fuel plates by HIP involves several steps. First, uranium and molybdenum feedstock is arc-melted in an inert atmosphere. It is then cast into thin coupons and hot rolled at 650 °C to the targeted foil thickness of 0.250 mm. The foils are then annealed at 650-675 °C for 30-120 minutes to eliminate residual stresses. The foil is placed between two layers of cladding material. For the RERTR fuel plates, Al6061-TO is used. The three layers are subjected to a HIP procedure conducted at a temperature of 560 °C and a pressure of 104 MPa for 90 minutes before being cooled to room temperature at a rate of 4.8 °C/min with diminishing pressure [16, 17 and 18].

The biggest concern and challenge associated with this new design is that multiple interfaces, seen in dispersive fuel types, are replaced with two large interfaces. In addition, during the irradiation, fuel foil is exposed irradiation induced creep and volumetric swelling. Similarly, Aluminum cladding is exposed to thermal creep. When irradiated, there is no efficient way to determine the stress-strain characteristics of fuel-cladding materials experimentally. Therefore numerical techniques were developed and proposed to compute stress-strain mapping of the irradiated plates.

To challenge the multiple cross disciplinary phenomena occur in the reactor, detailed multi-physics interactions were considered. Swelling formulation was implemented via
user define subroutine in Abaqus. Thermal creep formulation in both short term and long term was developed. Irradiation induced creep formulation was modified in the form of time hardening power law and implemented in to Finite Element formulation as well. The results presented in this document are critical for the fuel development efforts of the RERTR program.
Chapter 2: Fatigue Life Prediction Framework for In-Service Structural Components

Previous Research Scope

This improved energy-based criterion was developed from the stress-strain representation of the monotonic (Equation 2.1 & 2.2) and cyclic (Equation 2.3) material behavior shown in the following equations, where Equation 2.1 and Equation 2.2 represent the stress-strain relation prior to and after the necking phenomenon, respectively.

\[ \varepsilon = \frac{\sigma}{E} + \varepsilon_0 \sinh \left( \frac{\sigma}{\sigma_0} \right) \]  \hspace{1cm} (2.1)

\[ \sigma = \beta_1 \varepsilon + \beta_0 \]  \hspace{1cm} (2.2)

\[ \varepsilon_a = \frac{2\sigma_a}{E} + \frac{1}{C} \sinh \left( \frac{2\sigma_a}{\sigma_c} \right) \]  \hspace{1cm} (2.3)

The parameters displayed are defined as follows: \( \sigma_n \) is the value of stress at the surface of the specimen, \( \varepsilon \) is the strain corresponding to the stress; \( \beta_0, \beta_1 \) and \( \sigma \) are the respective slope and intercept of the stress-strain relationship in the necking region (From ultimate tensile to point of fracture), \( \sigma_a \) is the generalized stress value corresponding to the generalized cyclic strain \( \varepsilon_a \) (i.e. the minimum fully reversed point is observed as the origin), \( E \) is the modulus of elasticity, and the variables \( \sigma_0, \sigma_c, \varepsilon_0 \) and \( C \) are curve fit parameters described in [5]. These curve fit parameters for the cyclic and monotonic representations (Equation 2.1-2.3) are acquired by comparison between low cycle experimental results and experimental monotonic results, respectively.
Fatigue life is systematically determined for the improved criterion by taking the total monotonic strain energy density and dividing it by the strain energy density for one cycle. The total strain energy dissipated during a monotonic process is determined as the area underneath the curve constructed by Equations 2.1 & 2.2 and the strain energy density for one cycle is represented by the area within the hysteresis loop formed by Equation 2.3. The expression of the improved criterion for systematically determining fatigue life at a specified stress amplitude level is given as Equation 2.4.

\[
N = C \frac{\sigma_n \left( \varepsilon_n - \frac{\sigma_n}{E} \right) + \varepsilon_0 \sigma_0 \left[ \cosh \left( \frac{\sigma_n}{\sigma_0} \right) - 1 \right] + \frac{\beta_1}{2} \left( \varepsilon_f^2 - \varepsilon_n^2 \right) + \beta_0 \left( \varepsilon_f - \varepsilon_n \right)}{2 \sigma_c \left[ \frac{\sigma_a}{\sigma_c} \sinh \left( \frac{2 \sigma_a}{\sigma_c} \right) - \left[ \cosh \left( \frac{2 \sigma_a}{\sigma_c} \right) - 1 \right] \right]}
\]  

(2.4)

Where \( \sigma_n \) is the necking stress, \( \varepsilon_f \) is the fracture strain, and \( \varepsilon_n \) is the necking strain. The results from Equation 2.4 are compared with previously acquired fully-reversed axial test results and the corresponding linear regression curve on the S-N plot of Figure 1. The comparison shows that the criterion provides good life estimations for tension/compression fatigue results. To further validate the accuracy of the comparison, strain energy density per cycle is calculated from low cycle results and matched with the linear regression cycles to failure. These results are then compared with the energy-based predicted cyclic strain energy density and the related cycles to failure, which are all displayed in Figure 2. The results show that the experimental results match well with the predicted curve, thus further validating the criterion.
For the constitutive relation presented in figure 1 and figure 2, ASTM flat dog bone specimens made of AL6061-T6 were used. Dimensions of flat dog bone specimen are
shown in Figure 3a. Specimens were tested under fully reversed (R=-1) sinusoidal loading at an operating frequency of 40Hz. Tests were carried out under load control.

The energy-based method for fatigue life prediction was further extended to determine bending and tension/compression fatigue results for \( R \geq -1 \), the comparison between each respective mean-stress-effect life prediction process and corresponding uniaxial experimental results for Al 6061-T6 indicated that the energy-based method is an encouraging fatigue estimation tool for cyclic loading at various stress ratios [7]. Furthermore, a procedure for comparing the S-N behavior of Ti 6Al-4V materials with slightly different yielding stresses is presented with encouraging success [7, 19].

Experimental Details

To assess the fatigue life and strength of materials, three types of experiments were performed: (1) pure axial fatigue testing, (2) monotonic tension tests; and, (3) forward method. The forward method is a two-step procedure that utilizes axial fatigue for a certain number of cycles followed by monotonic tension until failure for simulation of in-service loading conditions. The failure criterion for all experiments was the fracture of the specimens in two parts.

Each of the experiments was performed according to ASTM E606-04E1: Standard Practice for Strain-Controlled Fatigue Test [20], and ASTM E8/E8M: Standard Test Methods for Tension Testing of Metallic Materials [21]. The dimensions for strain-controlled fatigue specimen are given in Figure 3b.
The as-received AL6061-T6 rods were on cold finished; solution heat treated; quenched and artificially aged state. All specimens were machined from the same batch to minimize possible deviation in experimental data. The material, heat treatment and specimen geometry were identical for monotonic tension and cyclic tests. Heat treatment and mechanical properties for the material were in accordance with AMS 2772 [22] and ASTM B211-03-M [23]. The gauge section of the specimens was polished with fine sand papers up to grade 400 (by using 3M TRI-M-ITE Paper A wt. W2). After machining and
polishing the specimens, no further heat treatment was performed. Certified chemical composition of the material was presented in Table 1.

Table 1 Reported Chemical Composition

<table>
<thead>
<tr>
<th>Elements</th>
<th>Si</th>
<th>Mg</th>
<th>Fe</th>
<th>Cu</th>
<th>Mn</th>
<th>Zn</th>
<th>Cr</th>
<th>Ti</th>
<th>Al</th>
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</thead>
<tbody>
<tr>
<td>%</td>
<td>0.6145</td>
<td>1.0142</td>
<td>0.2124</td>
<td>0.0264</td>
<td>0.0645</td>
<td>0.1122</td>
<td>0.1541</td>
<td>0.0127</td>
<td>REM</td>
</tr>
</tbody>
</table>

The experiments were carried out with a MTS 810 Model 318.60 frame equipped with MTS 647 Hydraulic Wedge Grips, MTS 661.20E-3 100kN Force Transducer and Interlaken Alignment Unit. This set-up is shown in Figure 4. An extensometer with gage length 12.7 mm (0.5”) made by Interlaken Technology Corporation was used to collect strains for the monotonic tension tests and a strain gauge glued on the specimen surface was used for hysteresis loops. For all fatigue tests, strains were captured by Vishay CEA-05-062UW-350 gauges connected to a Vishay 2150 amplifier.

The remaining data such as load, stroke and cycle numbers were stored by the MTS controller - data acquisition system. The same specimen and setup were used for all tests, including forward testing.
Results and Discussion

Monotonic Tension Tests

Three monotonic tension tests with 0.005 mm/sec displacement rate (strain rate of 0.0002 s$^{-1}$ in elastic range) were performed on Al6061-T6 specimens. The acquired engineering results are given in Figure 5. In order to develop an accurate analytical model, the engineering stress-strain data was converted to true stress-strain values. The basic
conservation of volume law was used for this operation. The conversion is displayed on Figure 6. In the Figure, the true stress-strain relationship between necking and fracture is assumed to be linear due to the absence of the relevant experimental data.

Figure 5 Monotonic Tension Results

Figure 6 Monotonic Tension Curves (at 0.005 mm/sec)
After converting engineering data to true data, the toughness of each specimen was computed. An average of three was used to minimize experimental deviation. Average true energy absorbed by the specimen during testing was computed as 344 MJ/m$^3$.

**Axial Fatigue Tests**

All axial fatigue tests were performed at room temperature on the MTS Systems Corporation servo-hydraulic load frame introduced above. In order to reduce the effect of various frequencies on materials fatigue life, the frame was operated at 25 Hz for all the fatigue tests. Fully reversed sinusoidal loading ($R = -1$) was used for all specimens. All tests were carried out under load control. Experimental results of axial fatigue tests are shown on the stress amplitude versus the number of cycles plot (S-N curve) in Figure 7.

![Figure 7 S-N Curve for Al6061-T6 (R=-1)](image)
A two-step combined testing procedure was used for determining failure energy of in-service structural components. The procedure involved a load controlled uniaxial fully reversed cyclic test for certain amount of cycles followed by monotonic tensile test at low strain rate until rupture. A number of tests were carried out using this two step testing procedure. Each test was carried out to accumulate the same amount of total energy as a fully reversed Al 6061-T6 fatigue test at an alternating stress of 206 MPa. At 206 MPa cyclic loading, since a specimen experiences failure at approximately 60,000 cycles, the two-step method was conducted by performing the cyclic operation on specimens up to a specified cycle count: 10K, 20K, 30K, 40K, 50K, 52K, 54K and 56K cycles. Each cyclic operation was immediately followed by a monotonic tension loading with a displacement rate of 0.005 mm/sec (approximate strain rate of 0.0001 s$^{-1}$). The engineering results of these monotonic tests are shown in Figure 8.

Data in Figure 8 was further converted to true stress-strain values and given in Figure 9. From the results, it is interesting that the material still preserves its toughness after a significant amount of cyclic loading. Energy loss starts to become more noticeable after 40,000 cycles. Energy for 10K, 20K, 30K and 40K specimens seems larger. This is due to insensitivity in final diametral measurements. Obviously, in an ideal case, energies for 10-40K specimens should not be more than a pristine part. Since there are multiple parts with multiple stages, however, a small deviation is acceptable.
Figure 8 Comparison of Forward Tests

Figure 9 True Stress-Strain Data and Energy Loss
From the two-step testing procedure, the remaining energy required for failure versus number of cycles is shown in Figure 10. It was observed that material looses very small amount of energy during the majority of its life and still keeps its ductility. However, there is a sharp drop in remaining energy when close to its projected life. The results indicate that there should be a sudden increase in the dissipated energy as operation cycles reach the projected fatigue strength.

![Figure 10 Remaining Energy versus Cycle Number](image)

Figure 10 Remaining Energy versus Cycle Number

Figure 11 shows images of pure tension specimen (11a), tension specimens after 40000 cycles (11b), 50000 cycles (11c), and full fatigue specimen (11d). Notice that the tension specimen with 50000 cycles (and beyond) shows a loss of ductility and almost no necking; while 40000 cycles still shows some degree of ductility as computed and shown in Figure 10.
Figure 11 Loss of Ductility During the Fatigue Process

(a) Pure Tension, (b) Tension After 40000 Cycles Fatigue Loading, (c) Tension After 50000 Cycles Fatigue Loading, (d) Pure Fatigue

To better understand this phenomenon, SEM investigation was performed on several fracture surfaces of different specimens. Micro-structural transition due to fatigue process is shown in figure 12.
Figure 12 Fractographs

(a) (b) Pure Tension, (c) (d) Tension after 40000 Cycles Fatigue Loading, (e) (f) Tension After 50000 Cycles Fatigue Loading, (g) (h) (i) Tension After 56000 Cycles Fatigue Loading, (j) (k) (l) Pure Fatigue (Ruptured at 61568 Cycles)
Fracture surfaces of pure monotonic tension specimen are shown in Figure 12(a) and 12(b), low and high magnification respectively. As continual straining of the material increases, the microvoids tend to grow, coalesce and eventually form an incessant fracture face [24, 25]. Sample showed typical ductile failure occurred by formation of microvoids, void growth and finally coalescence.

Figure 12(c) and 12(d) show a fracture surface for a monotonic tension specimen after 40000 cycles fatigue loading. On the fracture surface, several developing fatigue cracks were observed (marked by arrows). Fractograph at higher magnification (Figure 12(d)) implies that material has similar microstructure (microvoids, characteristics of a ductile fracture) even after 40000 cycles fatigue loading. However, there are local crack developments (marked with arrow) resulted from cyclic loading. This reveals features of locally ductile and brittle mechanisms having mixed mode type of fracture [25]; which further supports the results presented in Figure 9, 10 and 11.

In Figure 12(e) and Figure 12(f), fracture surface for tension specimen after 50000 cycles fatigue loading is shown. Localized inhomogeneous trans-lamellar deformation of the crack surface was observed [25, 26]. It seems that cracks were developed during cyclic loading and coalesced during monotonic tension loading.

Figure 12(g), Figure 12(h) and Figure 12(i) present the fracture surface of the tension specimen after 56000 cycles fatigue loading. In Figure 12(g), localized inhomogeneous
slip of the crack surface and the crack planes (marked by arrows) developed during the cyclic loading were shown [25, 26]. In figure 12(h), a premature microcrack (marked by arrow) was observed. In figure 12(i), a trans-lamellar cleavage fracture mode as well as fully developed fatigue cracks can be seen. This implies nearly complete transition to a brittle state.

Finally, fractographs for full fatigue specimen are shown in Figure 12(j), Figure 12(k) and Figure 12(l). Figure 12(j) presents a typical fatigue surface of the Aluminum 6061-T6 specimen gathered from the edge. In figure 12(k), fully developed cracks on subsurface facets, and combined micro-voids, cleavage fracture mode in figure 12(l) were observed [25, 26 and 27].

In summary, microstructure experiences very slow transition in early stages. However, transition from ductile to brittle state is more significant and faster once the specimen approaches to its projected fatigue life.

Plastic Strain Energy

It is known that not all of the cyclic strain energy is related to the fatigue damage. In low-cycle fatigue, only a small portion of the applied mechanical energy will contribute to the damage. The remainder of the energy is essentially converted into heat [28]. Similarly, in high-cycle fatigue, damage is created slowly by dislocation pile-ups due to the small and gradual changes in the material microstructure [29].
In addition to the above, the hysteresis loops during the cyclic loading contains anelastic and plastic effects. It was shown that the anelastic portion of hysteresis loop has little or no effect on micro-structure of a material [30]. Hence, it has a negligible contribution to the actual structural damage. Therefore, in this study, a thorough analysis of hysteresis loops at various frequencies has been conducted to determine a proper representation of plastic strain energy per cycle which actually contributes to the damage.

Figure 13 shows experimental results for fully reversed Al 6061-T6 hysteresis loops at an alternating stress of 206 MPa and frequencies ranging from 1-50 Hz.
Data was collected for all cycles but the points presented in this figure were selected from data immediately after the PID controlled load had reached stable state (i.e. immediately after initial shakedown period for high frequencies), not necessarily for stabilized energy state. Observing these results, it is believed that internal friction and damping in the test specimens become dominant at higher operating frequencies. These anelastic effects are the cause of the hysteresis loops in Figure 13 becoming significantly wider as the operating frequency increases.

Further analysis of the experimental hysteresis loops was conducted to determine how to separate plastic and anelastic components, thus allowing the means to generate sufficient strain energy per cycle representation. Experimental results were acquired from AL6061-T6 specimens with an alternating stress of 206 MPa and operating frequencies ranging from 0.005-50Hz. These results are displayed numerically in Table 2 and graphically in Figure 14.

<table>
<thead>
<tr>
<th>F [Hz]</th>
<th>0.005</th>
<th>0.01</th>
<th>0.05</th>
<th>0.1</th>
<th>0.25</th>
<th>0.5</th>
<th>1</th>
</tr>
</thead>
<tbody>
<tr>
<td>W [MJ/m$^3$]</td>
<td>0.00050</td>
<td>0.00050</td>
<td>0.00047</td>
<td>0.00093</td>
<td>0.00330</td>
<td>0.0072</td>
<td>0.014</td>
</tr>
<tr>
<td>F [Hz]</td>
<td>10</td>
<td>20</td>
<td>25</td>
<td>30</td>
<td>40</td>
<td>50</td>
<td></td>
</tr>
<tr>
<td>W [MJ/m$^3$]</td>
<td>0.14304</td>
<td>0.29857</td>
<td>0.39222</td>
<td>0.45610</td>
<td>0.58705</td>
<td>0.72037</td>
<td></td>
</tr>
</tbody>
</table>
This Figure shows that there is significant reduction in the hysteresis energy (i.e. anelastic strain energy density) as the frequency reduces from 1 to 0.005 Hz. Moreover,
the amount of energy dissipation starts to converge at 0.1 Hz and below. In other words, heat and anelastic components become negligible at these speeds. Therefore, it can be assumed that almost all of mechanical energy below 0.1 Hz is converted into plastic energy.

Based on the findings in Figure 14, experimental data was captured at an operating frequency of 0.05 Hz to plot the hysteresis loop. Then, this loop was used to formulate the strain energy per cycle representation of the energy-based model. Actual experimental data with 200 sample points was presented in Figure 15.

![Figure 15 Hysteresis Loop for 0.05 Hz](image-url)
Energy Dissipation History

The previously developed energy based fatigue models [4-7] assumed constant energy dissipation per cycle during the entire fatigue life. The assumption is adequate for most of the fatigue life but is questionable when internal micro-cracks are initiated during the final stage of the life.

It should be noted that characterization and understanding of the cyclic behavior of the studied material is a key point for understanding of cyclic energy evolution under fatigue loading. In order to investigate the variations in plastic energy dissipation, hysteresis energy was examined through the duration of a fatigue operation. The experimental hysteresis energy was acquired from Al 6061-T6 under fully reversed stress amplitude of 206 MPa. The hysteresis energy data sampling interval was initially 10K cycles, but the number of samples was increased after 50K cycles to provide a more sufficient final stage representation. These results are shown in Figure 16.

By calculating the area under each curve in Figure 16, the total energy dissipated (fatigue toughness) during the fatigue operation was determined. These results are displayed in Table 3. Since it was shown that 0.05 Hz is the optimal frequency for hysteresis construction, the total energy dissipated at 0.05 Hz was successfully compared with the monotonic to validate the understanding of the energy-based prediction theory proposed in this research.
Plastic energy dissipation history for 0.05 Hz versus the number of cycles is shown in Table 4. From this table, it can be seen that 36.9 MJ/m³ is dissipated between 0-54K cycles. This implies that the majority of the applied energy (approximately 90%) is consumed during the final stages of the specimen’s life. In other words, when sum of the
dissipated plastic energy reaches 10% of total monotonic energy (i.e. toughness), the in-service part should be replaced.

Table 4 Plastic Strain Energy Dissipation for 0.05 Hz

<table>
<thead>
<tr>
<th>Cycles</th>
<th>0-10K</th>
<th>0-20K</th>
<th>0-30K</th>
<th>0-40K</th>
<th>0-52K</th>
<th>0-54K</th>
<th>0-56K</th>
</tr>
</thead>
<tbody>
<tr>
<td>Energy [MJ/m³]</td>
<td>7.1</td>
<td>14.3</td>
<td>21.1</td>
<td>29.0</td>
<td>32.3</td>
<td>36.9</td>
<td>119.6</td>
</tr>
</tbody>
</table>

Development of an Energy Based Fatigue Criteria

An improved energy-based cycle dependent fatigue criterion is presented based on the previously developed energy-based fatigue life calculation scheme [4-7]. The following two entities make up the advancements to the previous energy-based fatigue life method: a model for hysteresis energy as a function of fatigue cycle that provides the capability for remaining fatigue life prediction, and the effect of micro cracks on hysteresis energy and fatigue life near rupture.

Cycle Dependent Fatigue Criterion

Based on the experimental results, the original energy-based prediction method of Equation 2.4 is modified to determine critical in-service life cycles ($N_{critical}$), which is roughly 90% of fatigue cycles. The modification is done by calculating the critical cycles required to dissipate 10% of the material toughness computed from monotonic tensile tests.
The true stress-strain representation of monotonic tension case is expressed by Equation 2.5;

\[ \sigma = \beta_1 \varepsilon + \beta_0 \]  

(2.5)

Further development of Equation 2.5 gives the total energy to failure for a monotonic case as presented in Equation 2.6,

\[ W_m = \sigma_n \left( \varepsilon_n - \frac{\sigma_n}{2E} \right) + \varepsilon_0 \sigma_0 \left[ \cosh \left( \frac{\sigma_n}{\sigma_0} \right) - 1 \right] + \frac{\beta_1}{2} (\varepsilon_n^2 - \varepsilon_0^2) + \beta_0 (\varepsilon_n - \varepsilon_0) \]  

(2.6)

where \( \sigma_n \) and \( \varepsilon_n \) are obtained from experimental monotonic fracture results and \( \sigma_0, \sigma_n, \varepsilon_0, \varepsilon_n, \beta_0 \) and \( \beta_1 \) are curve fitting parameters as explained in [7]. Similarly, cyclic stress-strain representation is expressed by,

\[ \varepsilon_a = \frac{2\sigma_a}{E} + \frac{1}{C} \sinh \left( \frac{2\sigma_a}{\sigma_c} \right) \]  

(2.7)

Further modification of Equation 2.7 provides the plastic energy dissipated per cycle until threshold,

\[ W_c = \frac{2\sigma_c}{C} \left\{ \frac{\sigma_a}{\sigma_c} \sinh \left( \frac{2\sigma_a}{\sigma_c} \right) - \left[ \cosh \left( \frac{2\sigma_a}{\sigma_c} \right) - 1 \right] \right\} \]  

(2.8)

From Table 4, 54K cycles is assumed as the threshold point. Experiments indicate that threshold point requires 10% of material total toughness computed from monotonic tension test. Therefore;

\[ N = \frac{0.1 \times \left\{ \sigma_n \left( \varepsilon_n - \frac{\sigma_n}{2E} \right) + \varepsilon_0 \sigma_0 \left[ \cosh \left( \frac{\sigma_n}{\sigma_0} \right) - 1 \right] + \frac{\beta_1}{2} (\varepsilon_n^2 - \varepsilon_0^2) + \beta_0 (\varepsilon_n - \varepsilon_0) \right\}}{2\sigma_c \times \left\{ \frac{\sigma_a}{\sigma_c} \sinh \left( \frac{2\sigma_a}{\sigma_c} \right) - \left[ \cosh \left( \frac{2\sigma_a}{\sigma_c} \right) - 1 \right] \right\}} \]  

(2.9)
Curve fit parameters are obtained through statistical analysis of experimental data [6]. Equation 2.9 is the critical cycle expression.

**Model Verification**

A hysteresis loop was constructed at an operating frequency of 0.05Hz. This loop was used to generate the strain energy per cycle representation of the energy-based model. Representative data for the hysteresis loop presented in Figure 15 is shown in Table 5.

<table>
<thead>
<tr>
<th>Local Coordinates</th>
<th>Generalized Coordinates</th>
</tr>
</thead>
<tbody>
<tr>
<td>Strain [mm/mm]</td>
<td>Stress [MPa]</td>
</tr>
<tr>
<td>Strain [mm/mm]</td>
<td>Stress [MPa]</td>
</tr>
<tr>
<td>-0.00327</td>
<td>-206.95</td>
</tr>
<tr>
<td>0</td>
<td>0</td>
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<tr>
<td>-0.00301</td>
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<tr>
<td>0.000255</td>
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<td>-18.28</td>
</tr>
<tr>
<td>0.002980</td>
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<tr>
<td>0.000945</td>
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</tr>
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<td>0.002046</td>
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<tr>
<td>0.005314</td>
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<td>0.002751</td>
<td>172.66</td>
</tr>
<tr>
<td>0.006019</td>
<td>379.61</td>
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<tr>
<td>0.003177</td>
<td>199.19</td>
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<td>0.006445</td>
<td>406.14</td>
</tr>
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<td>0.003317</td>
<td>207.85</td>
</tr>
<tr>
<td>0.006585</td>
<td>414.81</td>
</tr>
</tbody>
</table>
To determine the coefficients in Equation 2.8, representative data in Table 5 was used. As explained in [7], an iterative method was used to gather the curve fit parameters. Similarly, curve fit parameters for monotonic tension data presented in Figure 6 were computed. Numerical values for curve fit coefficients for both cyclic and monotonic cases are shown in Table 6.

<table>
<thead>
<tr>
<th>Table 6 Curve Fitting Parameters</th>
</tr>
</thead>
<tbody>
<tr>
<td>Monotonic True Stress-Strain (Equation 2.6)</td>
</tr>
<tr>
<td>$\beta_0$</td>
</tr>
<tr>
<td>$\beta_1$</td>
</tr>
<tr>
<td>$\varepsilon_0$</td>
</tr>
<tr>
<td>$\varepsilon_n$</td>
</tr>
<tr>
<td>$\varepsilon_f$</td>
</tr>
<tr>
<td>$\sigma_0$</td>
</tr>
<tr>
<td>$\sigma_n$</td>
</tr>
<tr>
<td>Cyclic True Stress-Strain (Equation 2.8)</td>
</tr>
<tr>
<td>$\sigma_c$</td>
</tr>
<tr>
<td>$E$</td>
</tr>
<tr>
<td>$C$</td>
</tr>
</tbody>
</table>

The accuracy of the fatigue life prediction method was verified by comparison between model approximation and experimental results of AL6061-T6 as shown in Figure 17.
Two distinct predictions were presented: critical number of cycles and full number of cycles to complete failure. Once critical number of cycles was computed by using the proposed model presented in Equation 2.9, interpolation was used to estimate number of cycles to complete failure. The comparison shows promising agreement, thus validating the capability of the framework to produce accurate fatigue life prediction.

Figure 17 Model Prediction

Conclusion

Two important findings are presented in this study. Not only a testing procedure was proposed for optimizing hysteresis plastic energy identification at operating frequencies lower than 0.05Hz, it was also validated with good accuracy that hysteresis plastic energy
per cycle increases exponentially as cycles close to the projected fatigue life. In addition, based on the previously developed energy-based fatigue criterion [4, 5 and 7] improvements were made to provide the criterion with the capability of estimating fatigue life of in-service parts. Using the exponentially plastic energy model, an improved energy-based life criterion was developed for remaining fatigue life prediction of in-service parts. The comparison between the life prediction and the experimental results indicates that the criterion provides promising life estimations. Further investigations have to be carried out for assessing the fatigue life of specimens and/or components under multiaxial stress strain states.
Chapter 3: Residual Stresses on U10Mo Mini Plates During HIP

Overview

This chapter presents an assessment of the residual stresses on U-10wt% Mo alloy based monolithic fuel plates and the elasto-plastic response to thermo-mechanical processing. Monolithic, plate-type fuel is a new fuel form developed by Idaho National Laboratory (INL) for research and test reactors, to achieve higher uranium densities within the reactor core and to allow multi-fold reduction of fuel enrichment. Understanding the three-dimensional residual stress field is critical for benchmarking and understanding the overall performance of these plate-type fuels. To define fuel-cladding stress-strain characteristics, a new thermo-mechanical finite element model was developed. Because the hot pressing temperature approaches the melting temperature of the cladding, variance of material properties at elevated temperatures and plasticity were incorporated to improve the accuracy of the model. By using elasto-thermo-plastic material models, it was determined that the cladding material (AL6061-TO) is subjected to tensile stresses that exceeds its proportional limits. The fuel foil however, is subject to compressive stresses and remains below yield. The residual stresses in the plates are significant, and therefore, should not be neglected. In particular, the simulations indicate the presence of high stress gradients at the fuel/cladding interface, thus emphasizing the need for a high quality bond.
Introduction

Initiated in the late 1970’s, the primary objective of the Reduced Enrichment for Research and Test Reactors (RERTR) program was to develop technology to minimize the use of Highly Enriched Uranium (HEU) for most civilian applications. The RERTR fuel development program aims to develop fuel types that would substitute highly enriched uranium with proliferation resistant, low enriched uranium (<20% $^{235}$U) for research reactors [12]. However, lower densities of uranium result in a decrease of fission rate. Consequently, higher volume would be required to compensate for lower fission rate caused by lower densities. Many research reactors can be satisfactorily operated with silicon based (U$_3$Si$_2$-Al) dispersive fuels lower (up to 5 g/cm$^3$) densities. However, several high performance reactors such as ATR (Advanced Test Reactor), HFIR (High Flux Isotope Reactor), MITR (Massachusetts Institute of Technology Reactor), NBSR (National Bureau of Standards Reactor), and MURR (Missouri University Research Reactor) require uranium densities of up to 10 g/cm$^3$ in monolithic form. The major challenge associated with those types of fuel is stability. High density uranium fuel should remain stable during irradiation as well as during the fabrication process. Among several proposed alloys, U-Mo based fuels are considered to be the most promising candidate for fuels of higher densities. Molybdenum extends the stability of the gamma phase. This is desirable because this phase is known to be stable under typical irradiation conditions. U-Mo has a low neutron caption cross-section, good irradiation behavior, and acceptable swelling response [13, 14 and 15].
Monolithic fuel plates comprised of U-Mo alloy based foils encapsulated in aluminum alloy cladding are proposed for conversion of high performance research reactors to low enriched uranium fuel reactors. Three different fabrication techniques have been considered and evaluated at INL for the fabrication: Hot Isostatic Pressing (HIP), Friction Stir Welding (FSW), and Transient Liquid Phase Bonding (TLPB). Of these three techniques, the HIP technique provides better mechanical behavior. As a result, this study focuses on monolithic plates fabricated via the HIP technique.

The process of fabricating monolithic fuel plates by HIP involves several steps. First, uranium and molybdenum feedstock is arc-melted in an inert atmosphere. It is then cast into thin coupons and hot rolled at 650 °C to the targeted foil thickness of 0.250 mm. The foils are then annealed at 650-675 °C for 30-120 minutes to eliminate residual stresses. The foil is placed between two layers of cladding material. For the RERTR fuel plates, Al6061-TO is used. The three layers are subjected to a HIP procedure conducted at a temperature of 560 °C and a pressure of 104 MPa for 90 minutes before being cooled to room temperature at a rate of 4.8 °C/min with diminishing pressure as described in [16, 17 and 18]. Final dimensions of the plate are shown in Figure 18.

The biggest concern and challenge associated with this new design is that multiple interfaces, seen in dispersive fuel types, are replaced with two large interfaces. To precisely model the irradiation response of the plates, understanding of the residual stresses and stress gradients at the interface is especially important. Improperly defined
residual stresses would result in poor accuracy of the model. This section aims to provide an in-depth understanding of the thermo-mechanical behavior of the plates during the HIP fabrication. The results presented serve as an initial condition for subsequent simulations (i.e. blister and in-reactor transient response) of the RERTR fuel development program.

Figure 18 Dimensions [mm] of a Mini-plate

Finite Element Model

A general purpose commercial finite element solver, ABAQUS, and computational clusters at the Ohio Super Computing Center (OSC) were utilized for computational modeling. Due to symmetry, only one-half of the plate (cut through the mid-plane) was modeled. The C3D8RT element in ABAQUS, an 8-node thermally coupled brick, tri-linear displacement and temperature with reduced integration, was used. The fuel was
represented by using 13500 hexahedral and 18724 nodes in 3 layers, while the cladding contained 44100 hexahedral and 53826 nodes in 8 layers (totaling 57600 elements with 72550 nodes). Nodal points at the foil/cladding interface were connected with a tie constraint. On the mid-plane, a symmetry condition was assigned to corresponding nodes. The node at the volumetric center was fixed to prevent rigid body motion. The resulting finite element discretization is shown in Figure 19.

![Figure 19 Half Symmetric Finite Element Model](image)

U10Mo material properties were assigned to the fuel foil region and Al6061-TO material properties were assigned to the cladding region. Elasto-thermo-perfectly-plastic material constitution was chosen for both cladding and fuel materials to incorporate plasticity. Material yield was defined according to Von-Mises flow criteria. Because fuel foils are
fabricated separately from the cladding and subjected to an annealing treatment, the foil is modeled as initially stress free. Because the amount of time under temperature is small and plate motion is constrained in the HIP frame, thermal creep was assumed to be negligible. Furthermore, it was assumed that complete interfacial bonding would be achieved during the hold period of HIP at 560 °C, and was treated as the reference point. The plate cooling process was simulated for 112 minutes (corresponding to a cooling rate of 4.8 °C/min from 560 °C to 21 °C). The transient results were stored at five minute intervals.

Mathematical Preliminaries

Material properties were assumed to be independent of direction but dependent on temperature. Hence, the modulus of elasticity, Poisson’s ratio and shear modulus were defined as,

\[ E_x = E_y = E_z = E(T) \]  \hspace{1cm} (3.1)

\[ \nu_{xy} = \nu_{yx} = \nu_{xz} = \nu_{zx} = \nu_{yz} = \nu_{zy} = \nu(T) \]  \hspace{1cm} (3.2)

\[ G_{xy} = G_{yx} = G_{xz} = G_{zx} = G(T) \quad \text{and} \quad G(T) = \frac{E(T)}{2[1+\nu(T)]} \]  \hspace{1cm} (3.3)

The linearized form of the strain tensor can be expressed as,

\[ \varepsilon_{ij}^{\text{total}} = \frac{1}{2} \left( \frac{\partial u_i}{\partial u_j} + \frac{\partial u_j}{\partial u_i} \right) \]  \hspace{1cm} (3.4)
where, $\varepsilon_{ij}$ and $u_{ij}$ are strain tensor and displacements respectively. At each point in the plate, the total-strain components $\varepsilon_{ij}^{total}$ are represented as the sum of the corresponding components of the elastic, plastic, thermal, creep and initial strains, and is defined as,

$$
\varepsilon_{ij}^{total} = \varepsilon_{ij}^{el} + \varepsilon_{ij}^{pl} + \varepsilon_{ij}^{th} + \varepsilon_{ij}^{cr} + \varepsilon_{ij}^{in} 
$$

(3.5)

The thermal strain tensor $\varepsilon_{ij}^{th}$ is expressed by,

$$
\varepsilon_{ij}^{th} = \alpha_{ij}(T) \times \Delta T
$$

(3.6)

where the $\alpha_{ij}(T)$ tensor governs the coefficient of thermal expansion, and $\Delta T$ is the change in temperature. The creep strain tensor, $\varepsilon^{cr}$ can be expressed using a power law relationship as follows,

$$
\dot{\varepsilon}^{cr} = A \tilde{\sigma}^n t^m
$$

(3.7)

where $\dot{\varepsilon}^{cr}$ is the uniaxial equivalent creep strain rate; $\tilde{\sigma}$ is the equivalent deviatoric stress; $t$ is the total time, and $A$, $n$, $m$ are material parameters. The values of the parameters were obtained by a curve fit. The creep model assumes that some inelastic deformation will occur whenever the stress in the material is nonzero. Finally, the stress/strain relation of the plate can be expressed as,

$$
\sigma_{ij} = D_{ijkl} \times \varepsilon_{kl}^{el} + \sigma_{ij}^{in}
$$

(3.8)

where $D_{ijkl}$ is 6x6 temperature dependent elasticity matrix, $\varepsilon_{kl}^{el}$ elastic strain tensor, and $\sigma_{ij}^{in}$ is the initial stress (i.e. residual stress). Substituting elastic strains from (3.5) into the relation in (3.8) yields the following equation for stress,
\[ \sigma_{ij} = D_{ijkl} \times (\varepsilon_{kl}^{\text{total}} - \varepsilon_{kl}^{\text{pl}} - \varepsilon_{kl}^{\text{sh}} - \varepsilon_{kl}^{\text{cr}} - \varepsilon_{kl}^{\text{in}}) + \sigma_{ij}^{\text{in}} \]  

(3.9)

The plastic flow is defined according to Von-Mises criteria as follows,

\[ F_d(\sigma_{ij}, \bar{\varepsilon}) = \frac{1}{2} \sigma_{ij}' \sigma_{ij}' - \frac{1}{3} \sigma_{i}^2(\bar{\varepsilon}) \]  

(3.10)

where \( \sigma_{ij}' \) is the deviatoric part of Kirchhoff stress, \( \bar{\varepsilon} \) represents the equivalent plastic strain and \( \sigma_{i} \) is the yield stress of the material. The governing equation for pure conductive heat transfer is expressed as;

\[ \rho C_p \frac{\partial T}{\partial t} + \nabla \cdot (-k \nabla T) = Q \]  

(3.11)

Where \( \rho \) is the density, \( C_p \) is the specific heat capacity at constant pressure, \( T \) is absolute temperature, \( k \) is the thermal conductivity and \( Q \) is the non-viscous heat source.

**Material Properties**

When the data is limited, it is common to make approximations or assumptions for especially high temperature properties. As a result, the effects of temperature on a given property might be quite large, and ignoring these effects may lead to erroneous results. Because temperatures approach the melting temperature of the cladding material, thermo-mechanical properties at these temperatures are especially important for accurate simulations. Material properties used in the simulations are presented here, while experimental details and more comprehensive information can be found elsewhere [31-40].
Table 7 Material Properties for Cladding (Al6061-TO)

<table>
<thead>
<tr>
<th>Temperature [°C]</th>
<th>21</th>
<th>93</th>
<th>149</th>
<th>204</th>
<th>260</th>
<th>316</th>
<th>371</th>
<th>427</th>
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<tr>
<td>Modulus [GPa]</td>
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<td>63.43</td>
<td>59.63</td>
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<td>43.51</td>
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<td>260</td>
<td>316</td>
<td>371</td>
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<td>Poisson’s [-]</td>
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<td>371</td>
<td>560</td>
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<td>55.15</td>
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<td>17.92</td>
<td>12.41</td>
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<td>227</td>
<td>327</td>
<td>426</td>
<td>526</td>
<td>581</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Conductivity [W/m·K]</td>
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<td>189</td>
<td>191</td>
<td>190</td>
<td>185</td>
<td>179</td>
<td>175</td>
<td></td>
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<tr>
<td>Temperature [°C]</td>
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<td>227</td>
<td>327</td>
<td>427</td>
<td>527</td>
<td>581</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Specific Heat [J/kg·K]</td>
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<td>942</td>
<td>988</td>
<td>1034</td>
<td>1080</td>
<td>1126</td>
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<tr>
<td>Temperature [°C]</td>
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<td>300</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td>21</td>
<td>260</td>
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<tr>
<td>Expansion (E-6) [1/K]</td>
<td>23.60</td>
<td>24.3</td>
<td>25.40</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td>2702</td>
</tr>
<tr>
<td>Density [kg/m³]</td>
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<td></td>
<td></td>
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</table>
Table 8 Material Properties for the Fuel Foil (U10Mo)

<table>
<thead>
<tr>
<th>Temperature [°C]</th>
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<th>100</th>
<th>200</th>
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<td>200</td>
<td>300</td>
<td>400</td>
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<td>600</td>
</tr>
<tr>
<td>Yield Stress [MPa]</td>
<td>780</td>
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<td>655</td>
<td>527</td>
<td>474</td>
<td>427</td>
<td></td>
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<td>Temperature [°C]</td>
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<td>100</td>
<td>200</td>
<td>300</td>
<td>400</td>
<td>500</td>
<td>600</td>
</tr>
<tr>
<td>Density [kg/m³]</td>
<td>16750</td>
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<td>16310</td>
<td>16230</td>
<td>16140</td>
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<td>100</td>
<td>200</td>
<td>300</td>
<td>400</td>
<td>500</td>
<td>600</td>
</tr>
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<td>Conductivity [W/m-K]</td>
<td>11.30</td>
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<td>500</td>
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<tr>
<td>Specific Heat [J/kg-K]</td>
<td>143</td>
<td>143</td>
<td>144</td>
<td>148</td>
<td>155</td>
<td>165</td>
<td>167</td>
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<tr>
<td>Temperature [°C]</td>
<td>21</td>
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<td></td>
<td></td>
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<tr>
<td>Modulus [GPa]</td>
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<tr>
<td>Poisson’s Ratio [-]</td>
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<td></td>
<td></td>
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<td>0.35</td>
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</tbody>
</table>
Results and Discussions

The plate cooling process was simulated from 560 °C to 21 °C with a cooling rate of 4.8 °C/min, totaling 112 minutes with 23 sub steps. Even though computing the stress field of the final product is the goal of this simulation, transient graphs for the maximums with respect to temperature were presented as well.

Transient Results

Figure 20 presents the calculated displacements and equivalent stress during the HIP process. Figure 20a shows that the fuel exhibits 1.13 mm total displacement while the cladding displaces 1.50 mm. Figure 20b shows the calculated maximum and minimum equivalent stress response with respect to temperature. As seen in the figure, cladding reaches its proportional limit and exhibits plastic deformation during the entire cooling process.

The calculated equivalent stress is 55.2 MPa for the cladding, while it is approximately 320 MPa for the fuel. Between 560 °C and 300 °C, the fuel and cladding exhibit similar trends. However, after the HIP temperature drops below 300 °C, the calculated stresses for the fuel and the cladding begin to diverge. This divergence would generate stress gradients along the interface. Unless a high degree of bonding is achieved above 300 °C, the stress gradients at the interface would hinder further strengthening.
While equivalent stresses are used for ease of comparison, for completeness and to provide better understanding, normal and shear stress histories are extracted and presented as well. Figures 21a and 21b show normal stress history for the foil and the
normal stress history of the cladding respectively. The cladding is exposed to tensile fields, while the foil experiences a compressive field.

Figure 21 Normal Stress Histories for (a) Fuel Foil (b) Cladding

Figure 21 indicates that normal stresses follow a similar trend as observed with equivalent stresses. Stresses along the length ($\sigma_{xx}$), thickness ($\sigma_{yy}$) and width ($\sigma_{zz}$) directions of the fuel foil are in Figure 21a. The normal stresses show a small increase at approximately 300 °C. Additionally, stresses in the length and width directions are approximately five times higher than the stress in the thickness direction. It is noted that stress magnitude along the thickness direction of the fuel is similar in magnitude to the equivalent stress magnitude of the cladding. This implies that stress gradients at the interface are dominated by the normal stress components of the fuel along the width and length directions. Figure 21b shows normal stress components for the cladding. The normal stresses of the cladding are tensile with the highest components, which are along the width and length directions of the cladding, contributing more to equivalent stress.
Figure 22 Shear Stress Histories for (a) Fuel Foil (b) Cladding

Figure 22 shows the shear components of the cladding and the foil. Due to higher stiffness, the fuel is subject to higher shear components compared to the cladding. The shear components have lower magnitudes and therefore are less concerning than normal components.

End of HIP (Fabricated Plate) Results

While transient normal, shear and equivalent stresses are important to note, the goal of the HIP simulation is to quantify the residual stresses of the fabricated plates. Stress contours for the plates at room temperature (21 °C) are shown in Figure 23. Contours of total displacement are shown for the foil (Figure 23a) and the cladding (Figure 23b). The total displacement for the fuel is 1.13 mm (0.56 mm with respect to volumetric center) and 1.50 mm (0.75 mm with respect to volumetric center) for the cladding.
Figure 23 Total Displacement [mm] Contours for (a) Foil (b) Cladding

Figure 24 presents the equivalent stress distribution for the fuel foil (Figure 24a) and the cladding (Figure 24b). In Fig 24a, especially close to fuel foil ends, higher stresses can be seen. The maximum equivalent stress was computed to be approximately 320 MPa for this region. Along the fuel edges, the equivalent stresses were computed to be much lower in magnitudes (approximately half of the maximum) compared to those on the bonding faces.
The stress magnitude differs greatly with respect to the location and it varies between approximately 160 MPa to 320 MPa. It is important to note this finding, since there are efforts to measure the residual stresses experimentally. These efforts should consider multiple locations as residual stresses were found to vary significantly. Similarly, Figure 24b shows an equivalent stress field on the cladding. The calculated maximum equivalent
stress is approximately 55 MPa. Cladding material over the fuel region exhibits complete yielding (showed by red). It can be implied that residual stresses on the plate are not dominated by cladding due to its compliant mechanical properties. Because the foil will be subject to swelling and irradiation induced creep during operation, further investigation of the normal stress components of the foil would lead to a better understanding of the mechanical behavior. Figure 25 presents the normal stress components for the foil. All three components are compressive.

![Figure 25 Normal Stress [MPa] Contours for the Fuel Foil](image)

(a) $\sigma_{xx}$ -Along the Length  
(b) $\sigma_{yy}$ -Along the Thickness  
(c) $\sigma_{zz}$ -Along the Width

The smallest stress component is in the thickness direction with a value of -56 MPa. The normal component along the length direction is -312 MPa, and -324 MPa for the width.
direction as shown in Figure 25a and Figure 25c respectively. Having compressive residual stresses will be beneficial since tensile stresses are expected during operation.

Evaluation of the Interface

Although the computed residual stresses in the foil are high, the stresses are still well below the yield level of U10Mo. This makes rupture unlikely according to the Maximum Stress Theory or Von-Mises Yield Theory. Similarly, cladding failure is not anticipated according to Maximum Strain Theory. Even though the cladding material is subjected to stresses above its yield strength, it has a high elongation to failure. For the simulation presented in this chapter, the equivalent strain for the cladding material was found to be 1.29%. Elongation to failure of Al6061-TO is 25% (AA, Typical, 1.6 mm sheet, as defined in [38]). Both the foil and cladding materials should not rupture during the cooling process.

The major concern of this plate design is the strength and endurance of the interface. The bond between the cladding and the fuel foil should not separate during cooling and also during irradiation. To ensure the quality of the interface, the plates are checked carefully for defects after fabrication. Even though interfacial strength has not been determined experimentally, many plates have undergone various irradiation experiments revealing that the bond holds its integrity. This implies that the strength of the interface is higher than the calculated stress of the model. The maximum and minimum equivalent stress
during the cooling process and residual stresses after cooling of the interface are shown in Figure 26 and Figure 27 below.

Figure 26 shows equivalent stress results and their changes with respect to temperature. The maximum label in the figure represents the data taken from the fuel side and the minimum label represents the data extracted from the cladding side. Calculated equivalent stress is approximately 55 MPa for the cladding side, while it is approximately 320 MPa for the fuel side. A complete yielding trend can be seen in the cladding side. The fuel side has approximately five times higher equivalent stress compared with the cladding side at room temperature.

Figure 27 shows the equivalent stresses extracted from the bond region. The fuel has a maximum of 318 MPa at the interface near the edges of the long transverse. These
stresses diminished significantly towards the outside edges. Similarly, in Figure 27b, the maximum equivalent stress at the interface on the cladding side is shown. Two smaller regions of low stress are below the yield strength, the majority of the cladding at the interface exceeds it.

Figure 27 Equivalent Stress [MPa] Contours of the Interface

(a) Fuel Foil (b) Cladding Material
Chapter Summary

The thermo-mechanical response of the monolithic fuel plates during cooling was investigated. A new finite element model was developed and fuel-cladding stress-strain characteristics were defined. The analysis demonstrated that the residual stresses are significant, and therefore, cannot be neglected. Differences in coefficient of thermal expansion between the cladding and the foil and high stiffness of the U10Mo foil are the primary causes of these residual stresses. It was shown that the cladding material is subjected to tensile stresses that exceed its proportional limits. The fuel foil however, is subject to compressive stresses and remains below yield. In particular, the simulations indicate the presence of high stress gradients at the fuel/cladding interface, thus emphasizing the need for a high quality bond.
Chapter 4: Computational Evaluation Mini Plates Subject to Thermal Cycling

Overview
This chapter presents evaluation of U10Mo monolithic fuel mini-plates during thermal cycling process. Thermal cycling so called “Blister Annealing” is a standard practice for the plate-type fuel elements. Identification of the stress field during this process is important for benchmarking the plates and estimating the possible spots for Blisters. To evaluate fuel-cladding stress-strain characteristics during this process, a new thermo-mechanical finite element model was developed. Two steps were considered: (1) Residual stresses as initial state (2) Mechanical behavior due to thermal cycling. A new thermal creep formulation covering short term behavior was developed. For blister simulation, elasto-perfectly-plastic material model was used, thermal creep was considered and residual stresses as initial state were implemented. Material properties at high temperatures were considered carefully to improve the accuracy, as temperatures approach the melting level of the cladding. It was found that at the end of the thermal cycling process, the cladding material (AL6061-TO) is exposed to a tensile field, while the fuel foil (U10Mo) would be under compressive stresses. It was found that there exists a critical temperature. Above this temperature, compressive stresses of the fuel change to tensile. Furthermore, a slight stress relaxation caused by thermally induced creep was found.
Introduction

Fabrication of the miniplates was discussed in previous chapter. After the fabrication, to ensure the bonding quality, the miniplates are checked by UT (Ultrasonic Transmission) Scanning technique for the regions with poor bonding. To satisfy the quality and safety during the irradiation conditions, plates should meet the standards and perform well in reactor. To ensure this, the fabricated plates are exposed to rigorous testing procedures. Among many, thermal cycling so called “Blister Annealing” is a standard practice for the plate-type fuel elements which sets a safety margin for research and test reactors. During the test, a specific threshold temperature, the temperature at where the specimen would produce blisters, is searched. To determine this temperature, specimens are exposed to several steps. For this, plates are heated in a furnace and visually examined for indication of any blisters. If there are none, then, plates are returned back to the furnace at even higher temperatures. Same procedure is repeated until the plate produce blisters. This temperature is accepted as Blister or threshold temperature. Figure 28 (taken from [41]) illustrates this phenomenon. In figure, a plate with a single Blister can be seen.

Figure 28 A Miniplate (a) Blister Formed (b) Section Cut
Determining this threshold temperature is important, since occurrence of blisters at any part of the plate (i.e. in the foil or at the interface) would alter the thermo-mechanical behavior of the plates, especially under actual operation conditions. Therefore, Blister temperature is treated as an evaluation mechanism for a failure prediction.

Thermal cycling practice can be applied to either irradiated or un-irradiated plates depending on the purpose of the characterization. For blister annealing of an irradiated plate, if it is blistered, the specimen can be assumed to be at breakaway point, because these pores would be filled and swell with gaseous products of nuclear reaction. Irradiation tests of U10Mo plates have implied that blister temperature of the foil drops as the fission densities increase. In other words, more time spent by fuel under irradiation leads to the lower blister (threshold) temperature. For un-irradiated plates, similar experimental techniques are applied to determine the threshold temperature.

There are efforts to describe the stress-strain characteristic of the mini-plates during this thermal cycling process. However, there is no efficient way to determine the stress field of fuel-cladding materials experimentally, especially at these high temperatures. Therefore, stress field of the domain during this thermal cycling needs to be identified via simulation techniques. This chapter aims to explain the plate behavior during the Blister Annealing via Finite Element simulations. Currently, mechanical properties of irradiated U10Mo have not been characterized experimentally, and therefore, properties are somewhat unknown. Because of this reason, only un-irradiated plates were considered for
this section. However, similar techniques and approaches can be utilized for the blister simulation of the irradiated plates, once the accurate mechanical data becomes available.

Finite Element Model

Blister simulation accounts all three stages of the process; namely, heating stage (in 100 minutes from 21 °C to 500 °C with 4.8 °C/min heating rate), holding stage (60 minutes at steady 500 °C) and finally cooling stage (total 100 minutes from 500 °C to 21°C with 4.8 °C/min cooling rate). Simulation treats the residual field from the HIP simulation (presented in Chapter 3) as initial condition. Same methodology, mesh and material properties are used for this step as well. To activate the creep response, constraint used in HIP simulation was removed. In other words, plate was free to move in all directions except a single node at the volumetric center of the fuel to prevent rigid body motion. Since the plate is exposed to elevated temperatures with high stress field, thermal creep formulation was included. To capture the behavior in short term (time in seconds), a new creep relation in the form of power law was formulated and implemented accordingly.

As explained in Chapter 3, due to symmetry, only one-half of the plate (cut through from the mid-plane) was modeled. The C3D8RT element in Abaqus, an 8-node thermally coupled brick, tri-linear displacement and temperature with reduced integration was used. The fuel was represented by using 13500 hexahedral and 18724 nodes in 3 layers, while the cladding contained 44100 hexahedral and 53826 nodes in 8 layers (totaling 57600 elements with 72550 nodes). Nodal points at the interface regions between the fuel foil
and cladding were connected with a tie constraint. On the mid plane (cut through from the plate mid-section), a symmetry condition was assigned to corresponding nodes. One node at the volumetric center of the foil was anchored to prevent rigid body motion. U10Mo properties were assigned to the fuel foil region and AL6061-TO properties were assigned to the cladding region. Elasto-thermo-perfectly-plastic material constitution was chosen for both cladding and fuel materials to include the material plasticity. Material yield was defined according to Von-Mises flow criteria.

**Development of Thermal Creep Relation**

Typically secondary creep models have a power law form. The time hardening power law has the following form,

\[
\dot{\varepsilon}^{cr} = A \cdot \sigma^n \cdot t^m
\]  

(4.1)

where \( \dot{\varepsilon}^{cr} \) is the uniaxial equivalent creep strain rate; \( \sigma^n \) is the equivalent deviatoric stress; \( t \) is the total time, and \( A, n, m \) are material parameters. The creep model assumes that some inelastic deformation will occur whenever the stress in the material is nonzero. The inelastic creep deformation is purely deviatoric.

Table 9 (details are in [38]) presents the thermal creep property of Al6061-TO for various temperatures. There is no direct way to implement the material data directly into the solver. An appropriate creep model must be constructed first.
Secondary creep on the plate was defined according to power law and time hardening creep model was formulated (Equation 4.1) by using the property presented in Table 9.

To increase the fidelity, each temperature level was treated separately.
Figure 29 Thermal Creep - Time Hardening Power Law - Model for 177 °C
Power Law – Time Hardening Coefficients: $A = 7.36E-20$, $n=9$, $m=-0.9$, R-square: 0.976

Figure 30 Thermal Creep - Time Hardening Power Law - Model for 205 °C
Power Law – Time Hardening Coefficients: $A = 3.46E-17$, $n=8$, $m=-0.8$, R-square: 0.984
Figure 31 Thermal Creep - Time Hardening Power Law - Model for 260 °C
Power Law – Time Hardening Coefficients: $A = 1.48 \times 10^{-14}$, $n=7$, $m=-0.7$, R-square: 0.987

Figure 32 Thermal Creep - Time Hardening Power Law - Model for 315 °C
Power Law – Time Hardening Coefficients: $A = 3.24 \times 10^{-14}$, $n=6$, $m=-0.6$, R-square: 0.907
Figure 33 Thermal Creep - Time Hardening Power Law - Model for 370 °C

Power Law – Time Hardening Coefficients: A = 2.45E-10, n=5, m=-0.5, R-square: 0.820

Table 10 presents the calculated coefficients of time hardening power law formulated in Equation 4.1 by using the thermal creep property presented in Table 9.

Table 10 Coefficients for Time Hardening Power Law

<table>
<thead>
<tr>
<th>A</th>
<th>n</th>
<th>m</th>
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Results and Discussions

Because plates have initial stresses caused by the fabrication process, results of the HIP simulation (presented in Chapter 3) were considered. All stress components ($\sigma_{xx}$, $\sigma_{yy}$, $\sigma_{zz}$, $\tau_{xy}$, $\tau_{xz}$, $\tau_{yz}$) for both the cladding and the fuel foil were implemented in to thermal annealing simulation. Three distinct stages were considered for annealing simulation. The heating stage increases the temperature from 21°C to 500 °C in 100 minutes (heating rate of 4.8 °C/min), the holding stage keeps the temperature steady at 500 °C for 60 minutes; and finally, the cooling stage drops the temperature to 21°C in 100 minutes (cooling rate of 4.8 °C/min). In order to increase the accuracy of the results, thermal creep was included for all three stages.

Transient Results

Figure 34 shows the calculated displacements and equivalent stress histories during the thermal cycling process. From figure 34a, it can be seen that fuel exhibits approximately 1.05 mm total displacement (0.526 mm with respect to volumetric center) while cladding displaces approximately 1.37 mm (0.689 mm with respect to volumetric center) when the temperature reaches to its peak.

In Figure 34b, the calculated equivalent stress response with respect to temperature and time is shown. It can be seen that the fuel foil compromises an equivalent stress of 320 MPa and the cladding has an equivalent stress of 55 MPa as an initial state (discussed in Chapter 3). Both cladding and fuel materials exhibit stress relaxations during the thermal
loading. It seems, from 21 °C to approximately 300 °C, relaxation of foil is more rapid compared to relaxation of cladding. This trend starts to be linear at around 300 °C. When the annealing temperature reaches to 500 °C, the fuel foil manifests an equivalent stress of 39.4 MPa and the cladding exhibits an equivalent stress of 8.64 MPa.

Figure 34 Transient Results for (a) Displacement (b) Equivalent Stresses
During the hold period, the effects of thermal creep become more evident. The maximum equivalent stress of the fuel drops to 32.9 MPa, while equivalent stress of cladding is reduced to 6.69 MPa. During the cooling period, fuel and cladding materials show similar trend with the heating period. By the end of the cooling period, the fuel foil gives an equivalent stress of 285 MPa. This implies that the thermal cycling process causes approximately 10% reduction on the fabrication induced residuals. Furthermore, from the figure, the cladding material reaches to its proportional limit and exhibits plastic deformation. From the figure, it can be seen that trend of the cladding follows yield curve of AL6061-TO.

The principal stress results in Figure 35 show the tension-compression state of the components during the various stages of the annealing process. Figure 35a shows the evolution of the maximum and the minimum principal stresses of the foil, while Figure 35b is for the cladding. In order to accurately identify the dominating principal stress, both maximum and minimum principal stresses should be studied. Considering just the first principal stress and disregarding the third principal stress might lead to deleterious conclusion, especially if there is a compression state over the mechanical domain. Even though the first principal stress implies a tension state for the fuel during the entire process, the magnitude of the third principal stress is much higher and the compression state dominates on the heating and cooling period. Third principal stress nearly vanishes during the hold period and the first principal stress becomes the dominating one. At this state, direction of the first principal stress implies that the foil would be under tensile
state. After 400 °C, it seems third principal stress (compressive state) starts to lose its dominancy and firsts principal stress (tensile state) starts to be dominant. It may be implied that the fuel foil would be more susceptible to crack growth above this temperature, if blisters develop.

Figure 35 Principal Stresses for (a) Fuel Foil (b) Cladding
Figure 36 present the normal stresses during the thermal cycling. Stresses along the length ($\sigma_{xx}$), thickness ($\sigma_{yy}$) and width ($\sigma_{zz}$) directions of the fuel foil are shown in Figure 36a.

It can be seen that all three components are in compression state until the peak temperature is reached. However, once the peak temperature is reached (hold period), stress component along the length direction becomes tensile and other two components
nearly vanish. The same figure indicates that stresses along the length and width directions are much higher compared with stress along the thickness direction. Stress magnitude along the thickness direction of the fuel is approximately similar to stress magnitudes for the cladding. Figure 36b shows normal stress components for the length ($\sigma_{xx}$), thickness ($\sigma_{yy}$) and width ($\sigma_{zz}$) directions of the cladding. It seems all three normal stresses on all three directions are tensile. Slight stress relaxation during the hold period can be seen as well.

Solutions for End of Heating Period

The following solutions are for the end of the heating stage (500 °C) just before the holding stage. Figure 37 shows the calculated total displacements on the foil (in Fig 37a) and the cladding (in Fig 37b). The total displacement for the fuel is 1.05mm (0.526 mm with respect to volumetric center) expansion and 1.37 mm expansion (0.689 mm with respect to volumetric center) for the cladding.

![Figure 37 Total Displacement [mm] Contours for (a) Foil (b) Cladding](image-url)
Figure 38 presents the equivalent stress distribution for the fuel foil and the cladding (Figure 38a and 38b respectively). In Figure 38a, especially close to foil corners and edges, a higher stress concentration was observed. The maximum equivalent stress was computed to be approximately 40 MPa for these hot spots. The bonding faces along the thickness (thin sides), the equivalent stresses were computed to be lower in magnitudes (approximately 8 MPa) compared to those on the bonding faces (top and bottom). Similarly, figure 38b shows the equivalent stress mapping of the cladding. The calculated maximum equivalent stress is approximately 9 MPa.

Figure 38 Equivalent Stress [MPa] Contours for (a) Foil (b) Cladding
Figure 39 shows contour plots for the calculated principal stresses for the fuel foil (Figure 39a) and for the cladding (Figure 39b). Maximum principal stress implies that there is a fully tension state over the foil and compression state over the cladding.

In figure 39a, lowest stress is 14 MPa in tension and it is located close to the foil ends. Especially close to the foil corners, four spots with high stress concentration can be seen.

Figure 39 Principal Stress [MPa] Contours for (a) Foil (b) Cladding

In figure 39a, lowest stress is 14 MPa in tension and it is located close to the foil ends. Especially close to the foil corners, four spots with high stress concentration can be seen.
Additional regions of high stresses are located slightly inside the long transverse edge of the foil running roughly its entire length. The magnitude of the maximum principal for these areas is approximately 41 MPa in tension. It is suspected that these regions might be possible initiation points. Furthermore, existence of the tensile state over the fuel might cause a foil separation if a crack develops. It seems cladding material (Figure 39b) is stress free around free sides, but has compression state over the bond region. The calculated minimum principal stress is approximately -9 MPa for this region.

Solutions for End of Holding Period

Hold period keeps the temperature at constant 500 °C for 60 minutes. Even though the temperature of the plates is kept constant, thermal creep might cause stress relaxation. During the holding period possible effect of thermal creep was investigated. It was found that there are slight changes in stress magnitudes at the end of the holding period. However, magnitudes of the displacements did not change during the hold period. Total displacement for the fuel was computed to be 1.05 mm and 1.37 mm for the cladding with respect to reference configuration.

Figure 40 presents the equivalent stress distribution for the fuel foil (figure 40a) and the cladding (figure 40b). It seems that thermal creep causes slight stress relaxation on the foil as shown in figure 40a. The maximum equivalent stress was computed to be 39.3 MPa before the hold period, and, it is now reduced to 32.9 MPa by the end of the holding period. In addition, stress concentrations on several locations were clearly visible on the
fuel foil (Figure 39a) at the end of the heating period. It seems stresses on the regions (slightly inside of the long transverse edge) are relaxed. Though there is a reduction in magnitude, four spots close to the foil corners still have stress concentrations. Even though stress relaxation is not much, similar trend can be seen for the cladding. The calculated maximum equivalent stress is reduced to 6.69 MPa from 8.64 MPa.

Figure 40 Equivalent Stress [MPa] Contours for (a) Foil (b) Cladding
Similar trend can be seen for the calculated principal stresses as shown in Figure 40. Contours for the principal stresses imply that compression-tension state of the plates does not change during the holding period.

![Figure 41 Principal Stress [MPa] Contours for (a) Foil (b) Cladding](image)

It can be seen that the fuel foil (figure 40a) is still in tension state and the cladding (figure 40b) preserves its compression state. For the fuel foil maximum principal stress is
reduced to approximately 34 MPa from 41 MPa during 60 minutes hold period. Four spots close to the foil corners have still stress concentrations. Similarly, cladding material (figure 40b) preserves its compression state at the end of the holding period. Cladding material is almost stress free around the free sides, but has still compression state over the bond region. There is a slight decrease in the calculated minimum principal stress. It seems minimum principal stress for the cladding is reduced to approximately -7.5 MPa (from 8.64 MPa for the beginning of the heating period).

Solutions for End of Cooling Period

Cooling period reduces the temperature of the plates from 500 °C to the room temperature in approximately 100 minutes. It was computed that there is a slight elongation caused by thermal cycling. Final residual elongation is approximately 0.013 mm with respect to reference configuration. The major impact of the thermal cycling is difference on the magnitude of the calculated final stresses. The effects of thermal creep manifest itself as stress relaxation especially on the fuel foil.

Figure 42 shows the equivalent stress distribution for the fuel foil (Figure 42a) and the cladding material (Figure 42b). It seems thermal creep causes approximately 10% stress reduction. Maximum equivalent stress for the fuel foil is reduced to 285 MPa at the end of the thermal cycling process. As calculated, the magnitude of the equivalent stress was 318 MPa before the thermal annealing process. Similar reduction is valid for the low boundary. Lower bound for the equivalent stress is reduced to 155 MPa (from 163 MPa)
and it is located on the bonding faces on the thin sides. It was noticed that equivalent stresses are reduced on areas close to edges of the fuel foil.

Figure 42 Equivalent Stress [MPa] Contours for (a) Foil (b) Cladding

In Figure 42a, especially close to foil ends, a higher stress concentration was observed. The bonding faces along the thickness (thin sides), the equivalent stresses were computed.
to be much in magnitudes (approximately 155 MPa) compared to those on the bonding faces (top and bottom). The equivalent stress mapping of the cladding is shown in Figure 42b. The calculated maximum stress is approximately 55.15 MPa. Cladding material over the fuel region (bond region) exhibits yielding.

Figure 43 shows results for the calculated principal stresses for the fuel foil (Figure 43a) and for the cladding (Figure 43b).
Principal stress indicates that fuel foil returns to the compression state. Similarly, cladding returns to the tensile state. In figure 43a, highest stress is 300 MPa in compression and it is located close to the foil ends. Lowest stress in magnitude is 236 MPa in compression and it is located at the bonding faces on the thin sides of the foil. It seems cladding material (Figure 43b) is has 55.15 MPa tensile state over the fuel region.

Evaluation of the Interface

Figure 44 presents equivalent stress results and their changes with respect to time and temperature.

![Figure 44 Equivalent Stresses [MPa] at the Interface](image)

During the thermal annealing process, cladding does not exhibit a stress relaxation. On the other hand, stress relaxation on the fuel side is more evident. It seems stress at the fuel side is reduced to 280 MPa (as it was 320 MPa before the thermal annealing).
Figure 45 presents the equivalent stress for the end of heating period and Figure 46 shows the equivalent stress results for the end of cooling period for the interface.

In figure 45a, highest stress was computed to be approximately 40 MPa close to the corners of the foil and on regions slightly inside of the long transverse edge of the foil. From cladding side, highest stresses are close to the edges. These areas are more susceptible if blisters develop.
Chapter Summary

Thermo-mechanical response of the mini plates during thermal annealing was investigated. Residual stresses due to the fabrication process were considered carefully. A new thermal creep formulation was developed. It was noted that thermal creep causes reduction of the stresses on the fuel foil. It was computed that residual stress would be reduced approximately 10% at the end of thermal cycling. It was proven that at the end of the heating period, fuel would be under tension which makes the foil more susceptible for Blister development. During the hold period, these possible locations for Blisters were identified. Furthermore, it was shown that at the end of the thermal cycling process, the cladding material (AL6061-TO) goes back to a tensile field, while the fuel foil (U10Mo) would be back to the compressive state. Existence of a critical temperature was noted. Above this temperature, compressive stresses of the fuel change to tensile. This might act as a Furthermore, a slight stress relaxation caused by thermally induced creep was found.
Chapter 5: Mechanical Behavior of U10Mo Mini-Plates Subject to Irradiation

Overview

This chapter evaluates U10Mo alloy based monolithic nuclear plates during in-service (under irradiation) operating conditions via fluid–thermal–structural interaction. Two distinct steps were considered: (1) Fabrication induced residual stresses as an initial condition (2) transient mechanical behavior during the operation with the consideration of irradiation induced phenomena. Once residuals and plastic strains due to Hot Isostatic Pressing (discussed in Chapter 3) process were identified, solution was used as initial condition for in reactor simulation. For irradiation (in-service) simulation, coupled fluid-thermal-structural interaction had to be considered due to the thermal dependency of the mechanical, thermal and fluid properties. To estimate the heat removal from the plate surface, heat transfer film coefficient was computed first. For this Dittus-Boelter correlation, a commonly used relation was used for the estimation of the heat transfer film coefficient between the plate surface and the primary coolant. Volumetric heat generation was assigned to the fuel region. However this was updated with respect to irradiation time, due to the volumetric swelling. Once the thermal field was identified over the domain, this field was used to simulate the stress field. To precisely identify the stress field over the plates, multiple mechanics were considered. To capture the behavior in log term (i.e. days), a new thermal creep relation was formulated. Furthermore, irradiation induced creep relation (i.e. as a function of neutron flux) was implemented in
the form of power law. Finally, relation for volumetric swelling of the fuel foil was converted to strain rates. Abaqus user defined subroutine CREEP was developed and implemented accordingly. The analysis showed that even though, irradiation induced creep causes stress relaxation over the foil, swelling acts as a counter effect. It has been shown that stress gradients at the interface are still a concern.

Introduction

Fabrication of the plates was discussed in Chapter 3. Once mini plates are fabricated, total 32 mini plates are assembled into 4 capsules made of Al6061-T6. The capsules are positioned vertically in a basket and are cooled by direct contact with primary coolant.

Flow velocities over the plates are 11 m/sec and 14 m/sec for outer and inner channels respectively. Approximate coolant pressure is 2.56 MPa. Representative schematic of plate assembly with partial cuts is shown in Figure 47.
Fuel plates are exposed to multi-physics in the reactor: cross interaction of fluid, thermal and structural. Fluid velocity is 14 [m/sec] and due to this high velocity turbulent flow occurs.

Coolant enters the capsules with 52 °C and temperature of the coolant increases while it travels along the channel. Furthermore, because of the existence of a power profile on the foil (one side of the plate closer to the ATR has higher neutron flux), coolant over this region is exposed to higher temperatures. This power profile creates significant thermal gradient on the plates. Furthermore, heat generation rate of the foil varies during the operation. All this nonlinearity should be considered to identify the thermal field of the plates precisely. Once thermal field is computed, it should be used as thermal forces to calculate the strains.

In addition, different components of the plates have various mechanical behaviors. For instance, fuel foil has volumetric swelling. By definition, volumetric swelling is a function of the neutron flux and neutron flux differs with respect to plate coordinates. Furthermore, flux varies during time. Another important point that should be considered is irradiation induced creep. Irradiation induced creep is function neutron flux and equivalent stress. As mentioned, there is stress gradient on the plate and one side of the plate closer to ATR core has higher irradiation exposure and this implies that creep of the foil is non-linear with respect to coordinates. Similar issue is valid for thermal creep of the cladding. Cladding has a temperature gradient with respect to coordinates and
temperature of the plates also varies with respect to time. In this chapter, methodology that was used to incorporate all these phenomena in the finite element will be explained.

Mathematical Preliminaries

The total-strain components $\varepsilon_{ij}^{total}$ are represented as the sum of the corresponding components of the elastic, plastic, thermal, creep, swelling and initial ones. Such as,

$$\varepsilon_{ij}^{total} = \frac{1}{2}(u_{i,j} + u_{j,i}) = \varepsilon_{ij}^{el} + \varepsilon_{ij}^{pl} + \varepsilon_{ij}^{th} + \varepsilon_{ij}^{cr} + \varepsilon_{ij}^{sw} + \varepsilon_{ij}^{in}$$  \hspace{1cm} (5.1)

Thermal strain tensor $\varepsilon_{kl}^{th}$ was expressed by,

$$\varepsilon_{kl}^{th} = \alpha_{kl}(T) \times \Delta T$$  \hspace{1cm} (5.2)

$\alpha_{kl}(T)$ is the tensor governing coefficient of thermal expansion, and $\Delta T$ is the temperature change from the reference point. The mechanical constitution of the plate is;

$$\sigma_{ij} = D_{ijkl} \times \varepsilon_{ij}^{el} + \sigma_{ij}^{in}$$  \hspace{1cm} (5.3)

where, $D_{ijkl}$ is 6x6 temperature dependent elasticity matrix (i.e. $D_{ijkl}(T)$), $\varepsilon_{ij}^{el}$ elastic strain tensor, and $\sigma_{ij}^{in}$ is initial stress (i.e. residual stress). Substituting elastic strains into the mechanical constitution gives the stress distribution on the plate expressed as,

$$\sigma_{ij} = D_{ijkl} \times (\varepsilon_{kl}^{total} - \varepsilon_{kl}^{in} - \varepsilon_{kl}^{th} - \varepsilon_{ij}^{cr} - \varepsilon_{kl}^{sw}) + \sigma_{ij}^{in}$$  \hspace{1cm} (5.4)

In addition, flow rule was defined according to Von-mises rule as follows,

$$F_{d}(\sigma_{ij}, \varepsilon) = \frac{1}{2} \sigma_{ij} \varepsilon_{ij}' - \frac{1}{3} \sigma_{ij}^2 \varepsilon$$  \hspace{1cm} (5.5)
Incremental swelling strain is defined as

$$\Delta \varepsilon^{sw} = \frac{1}{3} \Delta \varepsilon^{sw} \mathbf{R}$$  \hspace{1cm} (5.6)

$$\dot{\varepsilon}_{sw}^A = \dot{\varepsilon}_{11}^{sw} + \dot{\varepsilon}_{22}^{sw} + \dot{\varepsilon}_{33}^{sw} = \frac{1}{3} (r_{11} + r_{22} + r_{33}) \dot{\varepsilon}_{total}^{sw}$$  \hspace{1cm} (5.7)

\( \mathbf{R} \) is a matrix with the anisotropic swelling ratios in the diagonal. \( \dot{\varepsilon}_{total}^{sw} \) is volumetric swelling strain rate and \( r_{11}, r_{22} \) and \( r_{33} \), the directions of the components of the swelling strain rate. For the mini-plates, swelling was assumed to be independent of the direction. Volumetric swelling strain was defined according to the following relations.

$$\begin{pmatrix} \Delta V \\ V_0 \end{pmatrix}_{total} = \begin{pmatrix} \Delta V \\ V_0 \end{pmatrix}_{sw} + \begin{pmatrix} \Delta V \\ V_0 \end{pmatrix}_{s}$$  \hspace{1cm} (5.8)

Solid fission swelling

$$\begin{pmatrix} \Delta V \\ V_0 \end{pmatrix}_{s} = 3.5 \times 10^{-21} f_d$$  \hspace{1cm} (5.9)

Gas bubble swelling

$$\begin{pmatrix} \Delta V \\ V_0 \end{pmatrix}_{s} = 0.5 \times 10^{-21} f_d \quad \text{if} \quad f_d < 3 \times 10^{21} f/cm^3$$  \hspace{1cm} (5.10)

$$\begin{pmatrix} \Delta V \\ V_0 \end{pmatrix}_{s} = 5.4 + 2.2 \times 10^{-21}(f_d - 3 \times 10^{21}) + 0.51 \times 10^{-42} \times (f_d - 3 \times 10^{21})^2 \quad \text{if} \quad f_d > 3 \times 10^{21} f/cm^3$$  \hspace{1cm} (5.11)

Where \( f_d \) is fission density, \( V_0 \) is the initial volume, \( \Delta V \) is the volume change.

Irradiation-induced creep of fuel material is based on an empirical model which relates the creep rate to the current fast neutron flux, \( \Phi \) [neutron/m².s] and amplitude of the
equivalent stress, \( \sigma \) [MPa]. This specific relation is in power form and is represented as follows,

\[
\left( \frac{d\varepsilon}{dt} \right)_{ir}^{cr} = (\dot{\varepsilon})_{ir}^{cr} = K \sigma^n \Phi^p \tag{5.12}
\]

The parameter \( K \) (is in \([m^2/neutron.Pa]\) and exponents \( n, p \) are unitless and determined from results of relaxation experiments. INL have chosen to represent irradiation induced creep in terms of fission rate. Proposed irradiation induced creep relation is,

\[
(\dot{\varepsilon})_{ir}^{cr} = A \sigma \dot{F} \tag{5.13}
\]

\( A \) is a material parameter (\( A=2.5 \times 10^{-25} \) [cm\(^3\)/fission.MPa]), \( \sigma \) is equivalent stress [MPa]; \( \dot{F} \) is the average fission density rate [fission/(cm\(^3\).sec)] in the plate. This behavior was represented as a power law and implemented as follows,

\[
\dot{\varepsilon}^{cr} = 5.02 \times 10^{-7} \sigma^n \cdot t^m \tag{5.14}
\]

As explained previously, thermal creep is defined by the power law as follows,

\[
\dot{\varepsilon}^{cr} = A \sigma^n t^m \tag{5.15}
\]

where \( \dot{\varepsilon}^{cr} \) is the uniaxial equivalent creep strain rate; \( \sigma^n \) is the equivalent deviatoric stress; \( t \) is the total time, and \( A, n, m \) are material parameters.

The fundamental law governing heat transfer is the first law of thermodynamics, or the principle of conservation of energy. The basic law is usually expressed in terms of temperature for convenience. Governing equation for pure conductive heat transfer in fuel foil and cladding is expressed as;
\[ \rho C_p \frac{\partial T}{\partial t} + \nabla \cdot (-k \nabla T) = Q \]  

(5.16)

Where \( \rho \) is the density, \( C_p \) is the specific heat capacity at constant pressure, \( T \) is absolute temperature, \( k \) is the thermal conductivity and \( Q \) is the heat sources other than viscous heating.

Heat transfer coefficient to identify thermal transport between the coolant and cladding surface was computed via Dittus–Boelter, a common and particularly simple correlation.

\[ h = \frac{k_w}{D_H} N_u \]  

(5.17)

\[ N_u = 0.023 \cdot Re^{0.8} \cdot Pr^n \]  

(5.18)

where \( k_w \) thermal conductivity of the coolant, \( D_H - D_i \) is hydraulic diameter and \( N_u \) is Nusselt number, \( Re \) - Reynolds number, \( Pr \) - Prandtl number and \( n = 0.4 \)

**Development of Thermal Creep Relation**

Considering that plates are exposed to thermal load for a long period of time, a new creep relation was formulated. This new relation considers the time scale in hours. The creep model assumes that some inelastic deformation occurs whenever the stress in the material is nonzero. Primary and tertiary creeps were neglected and secondary creep on the plate was formulated according to power law. Time hardening creep model (Equation 5.15) was formulated by using the property data presented in Table 9 of Chapter 4.
Figure 48 Thermal Creep - Time Hardening Power Law - Model for 177 °C
Power Law – Time Hardening Coefficients: $A = 1.66E-19$, $n=9$, $m=-0.9$, R-square: 0.976

Figure 49 Thermal Creep - Time Hardening Power Law - Model for 205 °C
Power Law – Time Hardening Coefficients: $A = 1.78E-16$, $n=8$, $m=-0.8$, R-square: 0.984
Figure 50 Thermal Creep - Time Hardening Power Law - Model for 260 °C
Power Law – Time Hardening Coefficients: A = 1.73E-13, n=7, m=-0.7, R-square: 0.987

Figure 51 Thermal Creep - Time Hardening Power Law - Model for 315 °C
Power Law – Time Hardening Coefficients: A = 8.59E-11, n=6, m=-0.6, R-square: 0.907
Figure 52 Thermal Creep - Time Hardening Power Law - Model for 370 °C

Power Law – Time Hardening Coefficients: $A = 1.475 \times 10^{-8}$, $n=5$, $m=-0.5$, R-square: 0.820

Table 11 presents the calculated coefficients of time hardening power law formulated in Equation 5.15 by using the thermal creep property presented in Table 9 of Chapter 4. Power law accepts stresses as [MPa], time as [hours] and outputs strain rate as [1/hour].

Table 11 Coefficients for Thermal Creep Law

<table>
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<th>$A$</th>
<th>$n$</th>
<th>$m$</th>
<th>$T$ [°C]</th>
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<tr>
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<td>-0.5</td>
<td>370</td>
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Calculation of the Fitting for the Power Profile

Due to the vertical positioning in the reactor, fission densities on the plates are not uniform. One side of the plates (close to the reactor core) is exposed to higher fission densities. Table 12 tabulates this profile on the plate and Figure 53 presents the normalized data and the computed fitting.

<table>
<thead>
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<th>Distance [mm]</th>
<th>Fission Density [f/cm³]</th>
<th>Profile [-]</th>
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<td>7.13E+21</td>
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<td>4.00</td>
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<td>1.179</td>
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<td>5.33E+21</td>
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<td>4.43E+21</td>
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<td>4.12E+21</td>
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<tr>
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<td>3.93E+21</td>
<td>0.767</td>
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</table>
Actual volumetric heat release rate during the experiments (98 days of irradiation) is shown in figure 54. Because there is a power increase at day 51, simulations presented in this section consider the plates at both low power and high power. For this, plates were simulated at low power (16500 W/cm\(^3\)) for first 51 days. From day 51 to day 98, volumetric heat release rate was increased to 19300 W/cm\(^3\) to consider the plates at high power.

Figure 54 Volumetric Heat Release Rate for 98 Days
Preliminary Calculations for Fission Swelling

Volumetric swelling ratios due to the solid and gas products were presented in Equations 5.8 thru Equation 5.11. Table 13 present the swelling ratios with respect to coordinates.

Table 13 Fission Swelling for the Plates

<table>
<thead>
<tr>
<th>Distance [mm]</th>
<th>Fission Density [f/cm³]</th>
<th>Power Profile [-]</th>
<th>[%] Swelling (DV/V0)ₙ</th>
<th>(DV/V0)ₔ</th>
<th>Total (DV/V0)ₙ</th>
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<td>1.801</td>
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<td>38.90</td>
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<td>7.13E+21</td>
<td>1.391</td>
<td>24.96</td>
<td>23.19</td>
<td>48.14</td>
</tr>
<tr>
<td>4.00</td>
<td>6.04E+21</td>
<td>1.179</td>
<td>21.14</td>
<td>16.80</td>
<td>37.94</td>
</tr>
<tr>
<td>6.00</td>
<td>5.33E+21</td>
<td>1.040</td>
<td>18.66</td>
<td>13.29</td>
<td>31.95</td>
</tr>
<tr>
<td>8.00</td>
<td>4.81E+21</td>
<td>0.939</td>
<td>16.84</td>
<td>11.05</td>
<td>27.89</td>
</tr>
<tr>
<td>10.00</td>
<td>4.43E+21</td>
<td>0.864</td>
<td>15.51</td>
<td>9.59</td>
<td>25.09</td>
</tr>
<tr>
<td>12.00</td>
<td>4.12E+21</td>
<td>0.804</td>
<td>14.42</td>
<td>8.50</td>
<td>22.92</td>
</tr>
<tr>
<td>14.00</td>
<td>3.92E+21</td>
<td>0.765</td>
<td>13.72</td>
<td>7.86</td>
<td>21.58</td>
</tr>
<tr>
<td>15.00</td>
<td>3.87E+21</td>
<td>0.755</td>
<td>13.55</td>
<td>7.70</td>
<td>21.25</td>
</tr>
<tr>
<td>16.00</td>
<td>3.84E+21</td>
<td>0.749</td>
<td>13.44</td>
<td>7.61</td>
<td>21.05</td>
</tr>
<tr>
<td>17.00</td>
<td>3.84E+21</td>
<td>0.749</td>
<td>13.44</td>
<td>7.61</td>
<td>21.05</td>
</tr>
<tr>
<td>18.00</td>
<td>3.85E+21</td>
<td>0.751</td>
<td>13.48</td>
<td>7.64</td>
<td>21.11</td>
</tr>
<tr>
<td>19.00</td>
<td>3.93E+21</td>
<td>0.767</td>
<td>13.76</td>
<td>7.89</td>
<td>21.64</td>
</tr>
</tbody>
</table>
Additional update is needed to account the power generation increase occurs in day 51.

Table 14 below presents calculations for swelling this update.

Table 14 Updated Swelling for Fission Density Increase

<table>
<thead>
<tr>
<th>Irradiation</th>
<th>Total Fission</th>
<th>[%] Swelling</th>
</tr>
</thead>
<tbody>
<tr>
<td>Time</td>
<td>Density</td>
<td>Solid</td>
</tr>
<tr>
<td>[Days]</td>
<td>[fission/cm³]</td>
<td>(DV/V₀)s</td>
</tr>
<tr>
<td>0</td>
<td>0.00E+00</td>
<td>0</td>
</tr>
<tr>
<td>51.5</td>
<td>2.29E+21</td>
<td>8.03</td>
</tr>
<tr>
<td>51.6</td>
<td>2.69E+21</td>
<td>9.41</td>
</tr>
<tr>
<td>98</td>
<td>5.11E+21</td>
<td>17.88</td>
</tr>
</tbody>
</table>

Finally calculated volumetric swelling was converted to volumetric strain rates. This calculation is given in Table 15. Calculated results were then used to construct a fourth order polynomial. Finally, polynomial representing the swelling strain rate was implemented into the solver via user subroutine CREEP.

\[ (\dot{\varepsilon})^{sw} = \left( \frac{d\varepsilon}{dt} \right)^{sw} = 0.000032x^4 - 0.001473x^3 + 0.026182x^2 - 0.239764x + 1.801 \]  

(5.19)

Volumetric strains were assumed to be isotropic and swelling strain was distributed to length, width and thickness directions equally.
### Table 15 Calculations for Volumetric Swelling Strain Rate

<table>
<thead>
<tr>
<th>Irradiation Time [hour]</th>
<th>Volume Swelling Increase ($\Delta V/V_0$) [%]</th>
<th>Normal Strain $\varepsilon_x=\varepsilon_y=\varepsilon_z$ [%]</th>
<th>Volume Strain $\varepsilon_x,\varepsilon_y,\varepsilon_z$ [%]</th>
<th>Strain Rate $(\varepsilon_x,\varepsilon_y,\varepsilon_z)/t$ [1/h]</th>
</tr>
</thead>
<tbody>
<tr>
<td>0</td>
<td>0</td>
<td>0.00</td>
<td>0.00</td>
<td>0.00E+00</td>
</tr>
<tr>
<td>1236</td>
<td>9.18</td>
<td>2.97</td>
<td>8.91</td>
<td>7.210E-05</td>
</tr>
<tr>
<td>1238.4</td>
<td>10.76</td>
<td>3.46</td>
<td>10.39</td>
<td>8.392E-05</td>
</tr>
<tr>
<td>1248</td>
<td>10.97</td>
<td>3.53</td>
<td>10.59</td>
<td>8.485E-05</td>
</tr>
<tr>
<td>1272</td>
<td>11.18</td>
<td>3.59</td>
<td>10.78</td>
<td>8.477E-05</td>
</tr>
<tr>
<td>1296</td>
<td>11.38</td>
<td>3.66</td>
<td>10.98</td>
<td>8.470E-05</td>
</tr>
<tr>
<td>1320</td>
<td>11.59</td>
<td>3.72</td>
<td>11.17</td>
<td>8.462E-05</td>
</tr>
<tr>
<td>1344</td>
<td>11.80</td>
<td>3.79</td>
<td>11.36</td>
<td>8.456E-05</td>
</tr>
<tr>
<td>1368</td>
<td>12.01</td>
<td>3.85</td>
<td>11.55</td>
<td>8.443E-05</td>
</tr>
<tr>
<td>1392</td>
<td>16.21</td>
<td>5.14</td>
<td>15.41</td>
<td>1.107E-04</td>
</tr>
<tr>
<td>2352</td>
<td>30.18</td>
<td>9.19</td>
<td>27.57</td>
<td>1.172E-04</td>
</tr>
</tbody>
</table>

### Calculations for Heat Generation Update

Volumetric increase causes variation in the heat generation. Once final swelling values were calculated, heat generation rate was re-calculated to account this increase and presented in Table 16.
Table 16 Calculation of Heat Release Rate to Account the Swelling

<table>
<thead>
<tr>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>0</td>
<td>0.000</td>
<td>0.399</td>
<td>0.399</td>
<td>16504.22</td>
<td>6592.36</td>
<td>16504.22</td>
</tr>
<tr>
<td>51.5</td>
<td>9.180</td>
<td>0.399</td>
<td>0.436</td>
<td>16504.22</td>
<td>6592.36</td>
<td>15116.57</td>
</tr>
<tr>
<td>51.6</td>
<td>10.758</td>
<td>0.399</td>
<td>0.442</td>
<td>19304.22</td>
<td>7710.78</td>
<td>17429.16</td>
</tr>
<tr>
<td>98</td>
<td>30.182</td>
<td>0.399</td>
<td>0.520</td>
<td>19304.78</td>
<td>7711.00</td>
<td>14829.04</td>
</tr>
</tbody>
</table>

Calculations for Irradiation Induced Creep

To utilize the irradiation creep relation presented in Equation 5.13, total fission density rate was computed. Table 17 below presents the calculation of averaged fission density.

Table 17 Calculation of Total Fission Density on the Plate

<table>
<thead>
<tr>
<th></th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>0</td>
<td>16504.22</td>
<td>5.16E+14</td>
<td>2.29E+21</td>
</tr>
<tr>
<td>51.5</td>
<td>16504.22</td>
<td>5.16E+14</td>
<td>2.29E+21</td>
</tr>
<tr>
<td>51.6</td>
<td>19304.78</td>
<td>6.03E+14</td>
<td>2.69E+21</td>
</tr>
<tr>
<td>98</td>
<td>19304.78</td>
<td>6.03E+14</td>
<td>5.11E+21</td>
</tr>
<tr>
<td>Average</td>
<td>17833.06</td>
<td>5.57E+14</td>
<td>3.63E+21</td>
</tr>
</tbody>
</table>
By using Equation 5.13, total strain rate and strains was calculated. Table 18 presents strains by irradiation induced creep for various stress level.

Table 18 Calculation of Strains Caused by Irradiation Induced Creep

<table>
<thead>
<tr>
<th>Stress [MPa]</th>
<th>Strain rate [1/sec]</th>
<th>Total Strain End of 98 days</th>
<th>Total strain End of 98 days</th>
</tr>
</thead>
<tbody>
<tr>
<td>50</td>
<td>6.97E-09</td>
<td>0.058982837</td>
<td>5.90</td>
</tr>
<tr>
<td>100</td>
<td>1.39E-08</td>
<td>0.117965673</td>
<td>11.80</td>
</tr>
<tr>
<td>200</td>
<td>2.79E-08</td>
<td>0.235931346</td>
<td>23.59</td>
</tr>
<tr>
<td>300</td>
<td>4.18E-08</td>
<td>0.353897019</td>
<td>35.39</td>
</tr>
<tr>
<td>400</td>
<td>5.57E-08</td>
<td>0.471862692</td>
<td>47.19</td>
</tr>
<tr>
<td>500</td>
<td>6.97E-08</td>
<td>0.589828365</td>
<td>58.98</td>
</tr>
<tr>
<td>600</td>
<td>8.36E-08</td>
<td>0.707794038</td>
<td>70.78</td>
</tr>
</tbody>
</table>

Finally this relation was expressed in terms of power law and corresponding coefficients were presented in Table 19.

Table 19 Coefficients for Irradiation Creep Strain

<table>
<thead>
<tr>
<th>A</th>
<th>n</th>
<th>m</th>
<th>Unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.39E-10</td>
<td>1</td>
<td>0</td>
<td>[sec]</td>
</tr>
<tr>
<td>5.02E-07</td>
<td>1</td>
<td>0</td>
<td>[hours]</td>
</tr>
</tbody>
</table>
Finite Element Model

A general purpose commercial finite element solver, ABAQUS, and computational clusters at the Ohio Super Computing Center (OSC) were utilized. Due to symmetry, only one-half of the plate (cut through the mid-plane) was modeled. The C3D8RT element in ABAQUS, an 8-node thermally coupled brick, tri-linear displacement and temperature with reduced integration, was used. The fuel was represented by using 13500 hexahedral and 18724 nodes in 3 layers, while the cladding contained 44100 hexahedral and 53826 nodes in 8 layers (totaling 57600 elements with 72550 nodes). Nodal points at the foil/cladding interface were connected with a tie constraint. On the mid-plane, a symmetry condition was assigned to corresponding nodes. Plate simulated for 98 days. Power profile with respect to time was introduced as parametric formulation and assigned to the fuel region. Irradiation induced creep (for U10Mo), swelling (for U10Mo), heat generation and its swelling update (for U10Mo), thermally induced creep (for Al6061-TO) formulations were implemented to the solver. Derivation of these formulations and preliminary calculations were explained previously. Results were stored for 10 days (240 hours) intervals. Volumetric swelling was assigned to fuel via user defined FORTRAN subroutine CREEP. One node (lower corner) of the model was constrained in three directions to prevent rigid body motion. On the mid boundaries, symmetry condition was assigned to corresponding nodes. As initial state, residual stress field (from HIP result, Chapter 3) was assigned to the corresponding nodes via predefined field. Yield was assumed according to Von-Mises flow criteria. Fuel was assigned non-linear volumetric heat generation via parametric formulation. Because heat generation rate varies in time,
this was supplied to the solver via parametric update. Due to the swelling, to keep the power per unit volume consistent, heat generation rate was updated accordingly. In addition, exponential profile of heat generation was implemented via parametric formulation. Top side of the plate (interface of the cladding and coolant) film condition was assigned. Film coefficient was computed via Dittus-Boelter correlation as explained previously. From sides, heat transfer assumed to be negligible. Inlet side of the plate assumed to be 52 °C during the irradiation.

Results and Discussions

Figure 55 shows calculated temperature field for the fuel foil. It seems maximum temperature is 128 C (401 K) for the fuel foil. Temperature profile caused by non-linear fission density can be seen. Similarly, figure 56 presents the temperature distribution at the cross section (section cut on YZ plane) of the foil. Two distinct results were presented: at the fuel core and at the fuel cladding interface.

![Figure 55 Temperature [K] Field for the Fuel Foil (at High Power)](image)
Figure 56 Temperature Distribution at the Mid-section (at High Power)

Figure 57 and Figure 58 present displacement fields calculated for the cladding material and the fuel foil at the end irradiation (98 days) period. From figure 57, it can be seen that cladding displaces approximately 0.22 mm.

Figure 57 Displacement [mm] Field on the Cladding Material

Figure 58 shows displacement maps for the fuel foil. Calculated displacements are 0.096 mm along the length, 0.07 mm along the thickness and 0.115 mm along the width. Figure 59 gives final displacement profile (98 days of irradiation) along the thickness direction.
Figure 58 Displacements [mm] of the Fuel Foil

(U₁₁ - Along the Length, U₂₂ - Along the Thickness, U₃₃ - Along the Width)

Figure 59 Foil Displacements along the Thickness Direction (Swelling on Day 98)
Figure 60 compares the swelling strain (Figure 60a) and corresponding displacement field for the fuel foil at the end of the irradiation period (for 98 days of irradiation). A non-linear fission profile (discussed previously and shown in Figure 53) causes an exponential swelling strain on the foils. In Figure 60a, it can be seen that far edge of the foil experiences approximately 20% swelling strain, while the opposite edge experiences much higher swelling strain (approximately 47%).

![Figure 60 Swellings - End of the Irradiation Process (98 Days)](image)

(a) Swelling Strain [%] (b) Displacements [mm]

Figure 61 shows the equivalent creep strain fields for the foil (figure 61a) and the cladding (figure 61a). Maximum irradiation induced creep strain was computed to be approximately 27% for the foil as shown in Figure 61a.

Similarly, figure 61b presents the calculated thermal creep strain of the cladding material. Maximum equivalent thermal creep strain was found to be at the bonding faces and it is approximately 0.75% as shown in figure 61b.
Figure 61 Calculated Creep Strains [%] for the Plate

(a) Irradiation Creep of Fuel (b) Thermal Creep of Cladding

Figure 62 gives equivalent stress history during the irradiation period for the foil. Figure 62 implies that residual stresses (computed 318 MPa in HIP simulation and presented in Chapter 3) caused by fabrication process evolves very rapidly and are reduced to approximately 240 MPa in several days of irradiation. While fission swelling should increase the stresses on the fuel foil, it seems irradiation induces creep becomes more dominant and causes further stress relaxation. This phenomenon can be seen in Figure 62.

Figure 62 Evolution of the Equivalent Stresses During the Irradiation
Evolution of the Stress Field During Irradiation Process

To understand the irradiation behavior of the plates, evolution of the equivalent stresses for the cladding and foil were examined and presented in Figure 63 through Figure 71.

Figure 63 presents the initial state for the plates before the irradiation process. Residual stress field on the plates (318 MPa for the fuel foil and 55.15 MPa for the cladding material) can be seen.

Figure 64 presents equivalent stress fields for the fuel and cladding materials when the reactor starts (1 hour of irradiation). Maximum stress of the foil is now reduced to 281 MPa due to increased temperatures. Furthermore, a slight stress relaxation (refer to calculated residual stresses presented in HIP simulation) on the cladding can be seen.

Figure 65 presents equivalent stress fields for the fuel (figure 65a) and cladding (figure 65b) at the end of 1 day of irradiation. Maximum equivalent stress for the fuel foil is reduced to 259 MPa. In figure 65b, it seems thermal creep causes approximately 10% stress relaxation on the cladding material.

Figure 66 shows equivalent stress fields at the end of 5 days of irradiation. On long transverse edge of the fuel, due to higher neutron flux and temperature, a stress concentration becomes more noticeable. Maximum equivalent stress for this region is 241 MPa. It seems that in day 5, volumetric swelling starts to manifest itself. This causes a
slight increase in stress magnitude for the cladding material. Similarly, equivalent stress fields for the 10 days of irradiation are shown in Figure 67. A slight stress relaxation (237 MPa maximum) on the foil can be seen.

Figure 68 gives equivalent stress fields on the plates for 30 days of irradiation. Maximum stress was computed to be 235 MPa for the fuel. It seems swelling of the fuel causes further yielding on the cladding. Figure 69 shows the contour mapping for 60 days of irradiation. Even though volumetric swelling strain reaches to significant values, irradiation creep continues to cause stress relaxation on the foil. For day 60, maximum equivalent stress was computed to be 230 MPa for the foil. Yielded regions are noticeable on the cladding as shown in figure 69b.

Equivalent stress fields for the plates under 90 days of irradiation are shown in Figure 70. A slight stress relaxation for the foil is noted. Maximum equivalent stress for the foil is reduced to 225 MPa.

Finally, figure 71 shows the equivalent stress fields for the fuel (figure 71a) and cladding (figure 71b) at end of the irradiation period (98 days). A complete evolution of the stress field on cladding is visible. It seems there is a stress increase over the bonding area caused by the volumetric increase of the fuel foil. A further relaxation of the foil was noticed. It seems maximum equivalent stress for the foil is reduced to 220 MPa (as opposed to 225 MPa in Day 90).
Figure 63 Equivalent Stresses [MPa], Before the Irradiation (a) Fuel (b) Cladding

Figure 64 Equivalent Stresses [MPa], 1 Hour (Heat-up) (a) Fuel (b) Cladding

Figure 65 Equivalent Stresses [MPa], 24 Hours (1 Day) (a) Fuel (b) Cladding
Figure 66 Equivalent Stresses [MPa], 120 Hours (5 Days) (a) Fuel (b) Cladding

Figure 67 Equivalent Stresses [MPa], 240 Hours (10 Days) (a) Fuel (b) Cladding

Figure 68 Equivalent Stresses [MPa], 720 hours (30 Days) (a) Fuel (b) Cladding
Figure 69 Equivalent Stresses [MPa], 1440 Hours (60 Days) (a) Fuel (b) Cladding

Figure 70 Equivalent Stresses [MPa], 2160 Hours (90 Days) (a) Fuel (b) Cladding

Figure 71 Equivalent Stresses [MPa], 2352 Hours (98 Days) (a) Fuel (b) Cladding
Chapter Summary

Thermo-mechanical response of the monolithic fuel mini-plates under in-reactor conditions was investigated. For this, a new thermal creep relation was developed to cover the long term behavior. Irradiation induced creep formulation was evaluated and implemented by using a time hardening power law. Non-linear swelling formulation was developed. Swelling was introduced to the fuel foil via user defined subroutine. Heat generation was re-formulated to account the volumetric swelling. Temperature field was identified via Thermal-Structural interaction. Heat transfer coefficient between the cladding and coolant was computed via Dittus-Boelter correlation. Once it is identified, temperature field was used as thermal load for the foil and the cladding. A non-linear transient solver was utilized to calculate the irradiation response of the plates. The analysis demonstrated that residual stresses still dominate the mechanical behavior of the monolithic plate. Furthermore, results have implied that relaxation due to irradiation induced creep is balanced by the stresses caused by the volumetric foil swelling.
Chapter 6: An Image Based Finite Element for Dispersion Plates

Overview
This chapter evaluates the dispersion fuel mini plates via fluid–thermal–structural interaction. Fabrication induced stresses were considered and simulated via image based modeling techniques. In order to identify the residuals over the U7Mo particles and the Aluminum matrix, a representative SEM image was employed to construct a microstructure based elasto-plastic Finite Element model. Once residuals were identified in micro-scale, solution was used as initial condition for subsequent multiphysics simulations. Furthermore, since solid, thermal and fluid properties are temperature dependent and temperature field is a function of the velocity field of the coolant, coupled multi-physics simulations were considered. First, velocity and pressure fields of the coolant were computed via fluid-structural interaction. Computed solution for velocity fields were used to identify the temperature distribution on the coolant and on the fuel plate via fluid-thermal interaction. Finally, temperature fields and residual stresses were used to obtain the stress field of the plates via fluid-thermal-structural interaction.

Introduction
Dispersive fuel elements have been preferred in the research and test nuclear reactors due to their reliability, robust thermal and mechanical response [42]. For development of Molybdenum based dispersion fuels with higher densities, U-Mo particles are dispersed
in the Aluminum matrix. The process of producing of U-Mo based dispersion plates by Hot Rolling is as follows. Fabrication is according the well established picture-frame technique. This method has been used successfully for commercial fabrication of Aluminum-based dispersion fuel plates. The frame contains a compact that is assembled in a cavity machined into the frame plate. Fuel compact is prepared with atomization process as explained in elsewhere [43-47]. U-7Mo powder is mixed with 6061AL powder. The blended powder is then compacted under pressure and placed in a frame made of 6061 Al alloy. Cover plates are placed to the top and the bottom to form the cladding as illustrated schematically in Figure 72.

![Finished Fuel Plate via Hot Rolling](image)

Figure 72 Dispersion Plate via Picture Frame Technique

Assembly is then welded together and rolled under pressure to form a plate to reach the targeted thickness. The miniplates are produced by hot and cold rolling. The plates are hot rolled where the plates are in the furnace approximately 100 min at 500 °C and 15 min outside the furnace during the actual rolling. The plates are then cold rolled to
achieve the targeted thickness [48]. Dimensions for finished product are shown in Figure 73.

![Figure 73 Dimensions [mm] of a Dispersion Plate](image)

It is well accepted that Mechanical properties of a material is controlled by the microstructure. One way of simulating the macroscopic response of a given material is to construct a numerical model with the consideration of the microstructure. Once representative microstructure is constructed, microscopic behavior can be simulated. For this purpose, an effective numerical procedure, Image Based Finite Element has been developed by the scientists to calculate the mechanical and thermal properties. This technique is very efficient and accurate for the numerical simulation of microstructures. To achieve desired accuracy, a microstructural characterization should be carried out. Representative microstructure can be then used to produce realistic image-based finite element models. There are several tools are available for image processing such as OOF2 with Abaqus interaction, µFEM with Ansys interaction developed by ORNL, LaGriT
developed by LANL or iso2mesh with Matlab interaction. For this work, iso2mesh was used due to its Matlab interaction with COMSOL. [49-53]

Once residuals and plastic strains due to fabrication process are identified via microstructural based analysis, solution can be used as initial condition for subsequent simulations in continuum level. Because thermal response of the structure is mainly driven by amount of heat removed by primary coolant and heat removal rate is a function of the coolant velocity field and coolant temperature increases non-linearly while coolant travels along the path in the mini plate capsule and the heat generation rate has a non-uniform distribution along the plate width, fluid-structure interaction should be carefully coupled with fluid-thermal interaction before coupling them with structural mechanics. To achieve this, velocity and pressure fields of the coolant should be computed via fluid-structural interaction with thermal dependency. Computed velocity and pressure fields then should be used to identify temperature fields on both coolant and on the fuel plate via coupled fluid-thermal and thermal-structural interaction. Finally, computed temperature fields and residual stress distribution should be supplied to the solver to obtain the accurate mechanical response of a plate via coupled Fluid-Thermal-Structural interaction.

**Finite Element Model**

Since fuel particles are dispersed in an Aluminum matrix and location, size and orientation of the particles would alter the structural response of the domain; involvement
of microstructural characteristics should be considered and consequently, numerical model should include these geometric characteristics. For this purpose, a simulation in micro-scale should be employed first. Then, solution of the micro-model can be used as initial condition for subsequent simulations in the continuum level.

To calculate the fabrication induced residuals on the dispersion fuel compound (U7Mo+Al6061) after Hot Rolling process, general purpose commercial FE solver, COMSOL Multiphysics was employed. An image of representative microstructure (shown in Figure 74a) was used to construct the model.

Image processing toolbox of Matlab and iso2mesh were employed to convert the microstructure to a Finite Element mesh. An appropriate number of seeds are assigned to the particle boundaries. First, the fuel particles were meshed; then, matching mesh was assigned to Aluminum matrix. At the particle boundaries, a denser mesh was used to
increase the accuracy of the solution. Resulting finite element mesh is shown in Figure 74b.

One node (lower left corner) of the model was fixed and horizontal boundary at the bottom was constrained to restrict the motion on the vertical direction. Fuel was represented by using quadratic triangular elements totaling 7408 elements, while cladding contains 4135 quadratic triangular elements. Elasto-perfectly-plastic material models were assigned to both 6061 Aluminum matrix and U7Mo fuel particles. Elasto-plastic parametric solver with reduced integration was used. Since amount of time under temperature is small, thermal creep was assumed to have negligible effect. It was also assumed that complete mechanical bonding would be achieved at exact hot rolling temperature, and therefore 500°C was assumed to be reference point for the hot rolling simulation. From 500C, 20 sub-steps were used to reach the room temperature. Solution was stored for every 25°C temperature drop.

Residual Stresses

Equivalent stress distribution over the particles is shown in Figure 75. It was noted a complete plastic deformation of the matrix material (Al6061TO) with tensile residuals and compressive stresses on the particles (U7Mo). In figure 75, particles register stresses less than their yield level. However, existence of several hot spots was noted. It was understood that even though there are higher local residuals on the particles, matrix material holds approximately 55 MPa residuals at the end of hot rolling. Mechanical
response of the particles and the matrix with respect to temperature is shown in Figure 76. Maximum values were captured from the particles regardless of their location. It was noted that residual stress of the particles are higher where the mesh densities increase. It seems matrix material (Al6061-TO) exhibits plasticity.

Figure 75 Equivalent Stresses [MPa] on the Matrix

Figure 76 Stresses on the Particles and the Matrix
Fluid-Thermal-Structural Interaction

Once residuals and plastic strains due to fabrication process are identified via image-based FE analysis as discussed previously, computed residuals were used as initial condition. Velocity and pressure fields of the coolant were computed via fluid-structural interaction. Computed velocity and pressure fields were used to identify temperature fields on both coolant and on the fuel plate via coupled fluid-thermal and thermal-structural interaction. Finally, computed temperature fields and residual distribution were supplied to the solver to obtain the mechanical response of the plate via coupled Fluid-Thermal-Structural interaction.

Because of the symmetry, only one-half of the plate was modeled. Fuel was represented by using 3 layers totaling 7500 quadratic elements, while cladding contains 16656 quadratic elements distributed to 6 layers. Coolant was represented with 10 layers totaling 40260 quadratic elements. Resulted finite element model is shown in Figure 77.

For fluid flow, temperature dependent viscosity and density were assigned to corresponding elements. Inlet velocity of 14 [m/sec] and outlet boundary condition was assigned to corresponding nodes. Initial pressure of 2.56 MPa was assigned to coolant section. Coolant sides were treated as wall boundaries and slip conditions were assigned accordingly. Similar with symmetry condition used for solid portion, due to the symmetry of the coolant, only half of the coolant was modeled and consequently, symmetry boundary condition was assigned to the nodes on the top of the coolant.
For thermal transport simulation, temperature dependent thermal properties (conductivity, specific heat and density) were assigned to corresponding domains namely to fuel, cladding and coolant. Velocity fields from previous simulation were assigned to coolant. Inlet side was constrained with initial temperature (52 °C). Due to the small thickness of the plate, heat escape from the plate sides was assumed to be negligible and therefore, sides were treated as insulated boundaries. Symmetry/insulation boundary condition was assigned to the plate mid layer (i.e. fuel core) as well as to symmetry plane for the coolant. Convective flux was assigned to the nodes located at outlet side. Heat source/sink condition was assigned to the nodes shared by solid and the coolant to handle the heat transfer between two. Due to non-uniform fission density, fuel matrix closer to ATR core has higher heat generation rate. Heat generation rate was supplied to the solver via parametric formulation. Power profile discussed in Chapter 5 was used to compute a
polynomial. Polynomial was multiplied by the average heat generation rate of 10000 W/cm$^3$ to represent the heat generation rate.

For structural mechanics simulations, elasto-perfectly-plastic material models were assigned to both cladding and fuel materials. One node (lower left corner) of the model was constrained in three directions to prevent rigid body motion. Because fuel plate is placed into a guided channel, side nodes were constrained accordingly. On the mid boundary, symmetry condition was assigned. Residual stresses were introduced to the solver as initial condition. Temperature results of Fluid-Thermal interaction were applied as thermal load. Elasto-plastic parametric solver with reduced integration was employed. Fuel compound is mixture of 40% U7Mo and 60% Al6061 and material properties of the compound were assigned accordingly. The plate was assumed to be free of any defects at the room temperature.

Thermal Results

Figure 78 shows computed temperature field of the coolant film layer. As expected, coolant has 52 °C on the inlet side. Temperature gradients due to non-linear heat generation rate were noted. In addition, coolant experiences temperature increase while it travels through the channel. These thermal gradients closer to the outlet side are more noticeable. Highest temperature for film layer of the coolant was computed as 93 °C as shown in the Figure.
While cladding surface is exposed to approximately 93°C, maximum temperature between the cladding material and the fuel compound is approximately 102 °C as seen in Figure 79. Highest temperature was found to be close to the reactor core on the outlet side.
The temperature field for the fuel core (i.e. plate mid plane) is presented Figure 80. Highest temperature for the fuel compound is approximately 108 °C and it is on the outlet side of the coolant. Thermal gradients are more noticeable.

![Figure 80 Temperature Field at the Fuel Core (plate mid-plane)](image)

**Structural Results**

The final step of the multiphysics simulations is to couple previously computed results. Residual stresses which were computed via structural-thermal interaction, and temperature fields which were computed via coupled fluid-structural and thermal-fluid interaction were used to simulate the structural response of the plates. Computed temperature values were assigned to corresponding nodes as thermal loads and residuals due to fabrication process were assigned as initial condition.
Figure 81 shows the displacement field of the dispersion plate. Maximum total displacement was computed as 0.109 mm, while directional displacements were 0.108 mm, 0.037 mm and 0.0012 mm along the length, width and thickness directions. Corresponding strains to the directional displacements were computed as 0.0015, 0.0021 and 0.0020 on x, y, and z directions respectively.

![Figure 81 Total Displacements on the Plate](image)

Figure 82 shows equivalent stress distribution on the fuel matrix (40%U7Mo + 60%AL6061). While fuel compound holds approximately 50 MPa, equivalent stresses increase to approximately 145 MPa close to the interface. Same figure presents equivalent stress field on the cladding (on the right) material. It seems cladding over the bond region, has yielded.
Figure 82 Equivalent Stresses [MPa] on the (a) Matrix (b) Cladding

Figure 83 gives equivalent stresses distribution on the fuel cladding interface. Fuel compound-cladding interface is exposed to approximately 65 MPa.

Figure 83 Equivalent Stresses at the Interface
Chapter Summary

Dispersion fuel mini-plates were evaluated. A microstructural finite element was constructed for the calculation of the residual stresses. Residual stress was used as initial condition. Thermal field was computed via Fluid-Thermal interaction. Once temperature was field characterized, results were used to calculate thermal stresses via Thermal-Structural interaction. Compared to proposed monolithic fuel, dispersion plates showed significant compliance.
Chapter 7: Conclusion

In Chapter 2, an energy based fatigue life prediction framework was discussed. Not only a new testing procedure was proposed for optimizing hysteresis plastic energy identification at operating frequencies lower than 0.05Hz, it was also validated with good accuracy that hysteresis plastic energy per cycle increases exponentially as cycles close to the projected fatigue life. SEM investigations were carried out to evaluate the transition of the microstructure during the fatigue process. It was found that, during the fatigue process, small but gradual changes alter the microstructure of the specimen. It was observed that material evolves from ductile to brittle state. An improved version of energy-based fatigue criterion with the capability of estimating fatigue life of in-service parts was postulated. The comparison between the life prediction and the experimental results indicates that the criterion provides promising life estimations. Further investigations have to be carried out for assessing the fatigue life of specimens and/or components under multiaxial stress strain states.

In chapter 3, thermo-mechanical response of the monolithic fuel plates during the fabrication was investigated. A new finite element model was developed and fuel-cladding stress-strain characteristics were defined. The analysis demonstrated that the residual stresses are significant, and therefore, cannot be neglected. Differences in coefficient of thermal expansion between the cladding and the foil and high stiffness of
the U10Mo foil are the primary causes of these residual stresses. It was shown that the cladding material is subjected to tensile stresses that exceed its proportional limits. The fuel foil however, is subject to compressive stresses and remains below yield. In particular, the simulations indicate the presence of high stress gradients at the fuel/cladding interface, thus emphasizing the need for a high quality bond.

In Chapter 4, thermo-mechanical response of the mini plates during thermal annealing was investigated. Residual stresses due to the fabrication process from Chapter 3 were considered carefully. A new thermal creep formulation considering short term was developed. It was computed that residual stress would be reduced approximately 10% at the end of thermal cycling. It was proven that at the end of the heating period, fuel would be under tension which makes the foil more susceptible for Blister development. During the hold period, these possible locations for Blisters were identified. Furthermore, it was shown that at the end of the thermal cycling process, the cladding material (AL6061-TO) goes back to a tensile field, while the fuel foil (U10Mo) would be back to the compressive state. An existence of a critical temperature was noted.

In chapter 5, thermo-mechanical response of the monolithic fuel mini-plates under in-reactor conditions was investigated. For this, a new thermal creep relation was developed to cover the long term behavior. Irradiation induced creep formulation was evaluated and implemented by using a time hardening power law. Non-linear swelling formulation was developed. Swelling was introduced to the fuel foil via user defined subroutine. Heat
generation was re-formulated to account the volumetric swelling. Temperature field was identified via Thermal-Structural interaction. Heat transfer coefficient between the cladding and coolant was computed via Dittus-Boelter correlation. Once it is identified, temperature field was used as thermal load for the foil and the cladding. A non-linear transient solver was utilized to calculate the irradiation response of the plates. The analysis demonstrated that residual stresses still dominate the mechanical behavior of the monolithic plate. Furthermore, results have implied that relaxation due to irradiation induced creep is balanced by the stresses caused by the volumetric foil swelling.

Chapter 6 evaluated the dispersion fuel mini-plates under in-reactor conditions. A microstructural finite element was constructed for the calculation of the residual stresses. Compared to proposed monolithic fuel, dispersion plates showed significant compliance. Unless there is a micro failure (i.e. particle-Al matrix separation), in overall, plate demonstrated no structural weakness.
Appendix A: Fortran Subroutines for Swelling

Swelling with profile

SUBROUTINE CREEP(DECRA, DESWA, STATEV, SERD, EC, ESW, P, QTILD, 1 TEMP, DTEMP, PREDEF, DPRED, TIME, DTIME, CMNAME, LEXIMP, LEND, 2 COORDS, NSTATV, NOEL, NPT, LAYER, KSPT, KSTEP, KINC)

C
INCLUDE 'ABA_PARAM.INC'

C
CHARACTER*80 CMNAME

C
DIMENSION DECRA(5), DESWA(5), STATEV(*), PREDEF(*), DPRED(*), 1 TIME(2), COORDS(*), EC(2), ESW(2)

C
DESWA(1) = 0.0001172*DTIME* 1 (0.000032*((COORDS(3)+9.525)**4) - 2 0.001473*((COORDS(3)+9.525)**3) + 3 0.026182*((COORDS(3)+9.525)**2) - 4 0.239764*(COORDS(3)+9.525) + 1.801)

C
RETURN
END

Swelling without profile

SUBROUTINE CREEP(DECRA, DESWA, STATEV, SERD, EC, ESW, P, QTILD, 1 TEMP, DTEMP, PREDEF, DPRED, TIME, DTIME, CMNAME, LEXIMP, LEND, 2 COORDS, NSTATV, NOEL, NPT, LAYER, KSPT, KSTEP, KINC)

C
INCLUDE 'ABA_PARAM.INC'

C
CHARACTER*80 CMNAME

C
DIMENSION DECRA(5), DESWA(5), STATEV(*), PREDEF(*), DPRED(*), 1 TIME(2), COORDS(*), EC(2), ESW(2)

C
DESWA(1) = 0.000117221*DTIME

RETURN
END
Appendix B: Matlab Script for Heat Transfer Coefficient

clc; clear all; close all;

T= [274:553];

for i=1:length(T)

% density
rho(i) = 8.38466135E+02+1.40050603E+00*T(i)-3.01123760E-
03*T(i)^2+3.71822313E-07*T(i)^3;

% Specific heat
Cp(i) = 1.20101471E+04-8.04072879E+01*T(i)+3.09866854E-
01*T(i)^2-5.38186884E-04*T(i)^3+3.62536437E-07*T(i)^4;

% Thermal conductivity
k(i)=-8.69083936E-01+8.94880345E-03*T(i)-1.58366345E-
05*T(i)^2 +7.97543259E-09*T(i)^3;

% Kinematic viscosity
if(T(i)>=273.15 && T(i)<413.15)
    nu(i)= 1.3903230155E-03-2.1392545737E-05*T(i)+
1.3717187260E-07*T(i)^2-4.6851166702E-10*T(i)^3+8.982339887E-
13*T(i)^4-9.1602964729E-16*T(i)^5+3.8807189536E-
19*T(i)^6;
end
if (T(i)>=413.15 && T(i)<553.75)
    nu(i)= 1.36222834E-06-4.45388688E-09*T(i)+4.04364450E-
12*T(i)^2;
end

% Dynamic Viscosity
if(T(i)>=273.15 && T(i)<413.15)
    eta(i) = 1.3799566804E+00-2.1224019151E-
02*T(i)+1.3604562827E-04*T(i)^2 -4.6454090319E-07*T(i)^3
+8.9042735735E-10*T(i)^4 -9.0790692686E-13*T(i)^5 +3.8457331488E-
16*T(i)^6;
end
if (T(i)>=413.15 && T(i)<553.75)
    eta(i) = 4.01235783E-03-2.10746715E-05*T(i)+3.85772275E-
08*T(i)^2-2.39730284E-11*T(i)^3;
end
end
% Dittus Boelter Correlation - Heat Transfer Coefficient
v = 14; % [m/s]
l = 2.256e-2; % [m]
w = 0.1105e-2; % [m]
A = 0.249288e-2; % [m^2]
P = 4.733e-2; % [m]
D = 0.21068075e-2; % [m]

for i=1:length(T)
    Pr(i) = Cp(i)* eta(i)/k(i);
    Re(i) = v*rho(i)*D/eta(i);
    Nu(i) = 0.023*(Re(i)^0.8)*(Pr(i)^0.4);
    h_coeff(i) = k(i)*Nu(i)/D;
end

% TABULATE DATA TO A FILE
fid = fopen('water_hcoeff.txt','w');
fprintf(fid,'-----------------------------------------
');
fprintf(fid,'T   | h_coeff |
[\ K] | [W/(m^2.K)] |
-----------------------------------------
');
for i=1:length(T)
    fprintf(fid,'%d | %.1f \r
', T(i), h_coeff(i));
end
fclose(fid);

% PLOT ALL
figure();
plot(T,h_coeff,'M','LineWidth',2),grid on;
xlabel('Temperature [K]');
ylabel('Heat Transfer Coefficient [W/(m^2.K)]');
saveas(gcf,'plots/heat_transfer_coefficient.jpeg');
delete *.asv;
close all;
Appendix C: Matlab Scripts for Thermal Creep Fitting

%% strain rate, stress and time are required
%% either supply manually or place them in a text file

% for 177 C
data = load('data/temp_177C.txt');
strainrate = data(:,1);
stress = data(:,2);
time = data(:,3);

% Curve Fit
ft = fittype( 'A * (x^9) * (y^-0.9)', 'indep', {'x', 'y'}, 'depend', 'z' );
opts = fitoptions( ft );
opts.Display = 'Off';
opts.Lower = -Inf;
opts.StartPoint = 0.198493798811171;
opts.Upper = Inf;
opts.Weights = zeros(1,0);
[fitresult, gof] = fit( [stress, time], strainrate, ft, opts );

% for 205 C
data = load('data/temp_205C.txt');
strainrate = data(:,1);
stress = data(:,2);
time = data(:,3);

% Curve Fit
ft = fittype( 'A * (x^8) * (y^-0.8)', 'indep', {'x', 'y'}, 'depend', 'z' );
opts = fitoptions( ft );
opts.Display = 'Off';
opts.Lower = -Inf;
opts.StartPoint = 0.504335533650718;
opts.Upper = Inf;
opts.Weights = zeros(1,0);
[fitresult, gof] = fit( [stress, time], strainrate, ft, opts );
% for 260 C
  data = load('data/temp_260C.txt');
  strainrate  = data(:,1);
  stress      = data(:,2);
  time        = data(:,3);
% Curve Fit
  ft = fittype( 'A * (x^7) * (y^-0.7)', 'indep', {'x', 'y'},
  'depend', 'z' );
  opts = fitoptions( ft );
  opts.Display = 'Off';
  opts.Lower = -Inf;
  opts.StartPoint = 0.929491354398484;
  opts.Upper = Inf;
  opts.Weights = zeros(1,0);
  [fitresult, gof] = fit( [stress, time], strainrate, ft, opts );

% for 315 C
  data = load('data/temp_315C.txt');
  strainrate  = data(:,1);
  stress      = data(:,2);
  time        = data(:,3);
% Curve Fit
  ft = fittype( 'A * (x^6) * (y^-0.6)', 'indep', {'x', 'y'},
  'depend', 'z' );
  opts = fitoptions( ft );
  opts.Display = 'Off';
  opts.Lower = -Inf;
  opts.StartPoint = 0.328036441146183;
  opts.Upper = Inf;
  opts.Weights = zeros(1,0);
  [fitresult, gof] = fit( [stress, time], strainrate, ft, opts );

% for 370 C
  data = load('data/temp_370C.txt');
  strainrate  = data(:,1);
  stress      = data(:,2);
  time        = data(:,3);
% Curve Fit
  ft = fittype( 'A * (x^5) * (y^-0.5)', 'indep', {'x', 'y'},
  'depend', 'z' );
  opts = fitoptions( ft );
  opts.Display = 'Off';
  opts.Lower = -Inf;
  opts.StartPoint = 0.274279186919626;
  opts.Upper = Inf;
  opts.Weights = zeros(1,0);
  [fitresult, gof] = fit( [stress, time], strainrate, ft, opts );
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