Swelling of U-7Mo/Al-Si Dispersion Fuel Plates Under Irradiation – Non-Destructive Analysis of the AFIP-1 Fuel Plates

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Abstract

Extensive fuel-matrix interactions leading to plate pillowing have proven to be a significant impediment to the development of a suitable high density low-enriched uranium molybdenum alloy (U-Mo) based dispersion fuel for high power applications in research reactors. The addition of silicon to the aluminum matrix was previously demonstrated to reduce interaction layer growth in mini-plate experiments. The AFIP-1 project involved the irradiation, in-canal examination, and post-irradiation examination of two fuel plates. The irradiation of two distinct full size, flat fuel plates (one using an Al-2wt% Si matrix and the other an Al-4043 (~4.8wt% Si) matrix) was performed in the INL ATR reactor in 2008-2009. The irradiation conditions were: ~250W/cm² peak Beginning Of Life (BOL) power, with a ~3.5e21 f/cm³ peak burnup. The plates were successfully irradiated and did not show any pillowing at the end of the irradiation.

This paper reports the results and interpretation of the in-canal and post-irradiation non-destructive examinations that were performed on these fuel plates. It further compares additional PIE results obtained on fuel plates irradiated in contemporary campaigns in order to allow a complete comparison with all results obtained under similar conditions. Except for a brief indication of accelerated swelling early in the irradiation of the Al-2Si plate, the fuel swelling is shown to evolve linearly with the fission density through the maximum burnup.

1. Introduction

High uranium density fuels for research and test reactors are being developed by the Reduced Enrichment for Research and Test Reactor (RERTR) program under the Convert branch of the U.S. Department of Energy's (DOE) National Nuclear Security Administration (NNSA) Global Treat Reduction Initiative (GTRI) [1] (which is now executed under the Material Management and Minimization program (M3)). The goal of the RERTR program, since 1978, is to develop nuclear fuels that enable civilian reactors around the world to convert from high enriched uranium (HEU) fuels to new low enriched uranium (LEU)¹ fuels. These conversions should not result in a substantial loss in performance and should not impact their research missions.

Traditional material test reactors utilize dispersion type fuel design. This design is based on a particulate fuel phase dispersed in an aluminum matrix that forms the 'fuel meat'. The fuel meat is encapsulated in an aluminum alloy cladding that protects it from interaction with the reactor coolant. Only four fuel phase materials, (uranium aluminum alloy, uranium aluminide (UAl_x), uranium oxide (U_3O_8), and uranium silicide (U_3Si_2)) are currently considered 'qualified' for use by the US Nuclear Regulatory Commission (NRC). Development of a replacement fuel for the highest power research reactors requires qualification of a new fuel design that offers significantly higher uranium density than those currently available. Based on extensive irradiation testing conducted in the early 1990's, uranium-molybdenum alloy (U-Mo) was selected for this purpose [2].

Although the U-Mo fuel phase performed exceptionally well under irradiation, chemical interaction with aluminum resulted in formation of a U-Mo-Al fuel-matrix interaction (FMI) product that correlated with blistering of the fuel plates under high power and burnup irradiation conditions [3,4,5,6,7,8]. Irradiation studies using mini-plates [9,10] demonstrated that the addition of small amounts of silicon to the aluminum matrix drastically altered the composition of this FMI (now U-Mo-Al-Si) and irradiation induced growth was significantly reduced. This change dramatically stabilized the fuel meat during irradiation and suppressed blister formation [11,12,13,14]. In both cases the FMI layer was found to be an amorphous material [15,16,17,18,19] with unknown thermal and mechanical properties.

Following demonstration at the mini-plate scale, the next step in development of the U-Mo fuel design [20] required irradiation testing in both a prototypic configuration and under prototypic operating conditions. This test was necessary to demonstrate that the Si modified fuel meat design yields acceptable integrated fuel behavior under its intended service conditions. To this end, an irradiation device was developed for the Advanced Test Reactor (ATR) and 'full-size' fuel plates were produced for irradiation testing. This paper reports the results and interpretation of the non-destructive examinations that were performed on the fuel plates included in this experiment.

¹ LEU is defined as uranium content with less than 20 at% U-235.

2. Experiment Description

The fuel meat design was selected based on the successful irradiation of mini-plates in the RERTR-6 and RERTR-7 experiments. These tests showed that adding 2-5 wt% silicon to the Al matrix material resulted in suppression of U-Al-Mo interaction product that forms at the fuel-matrix interface (although a second U-Si-Mo interaction layer forms within the fuel particle instead). This feature appeared to stabilize the fuel meat under irradiation. These results also showed that higher silicon levels were required to achieve the same affect as the irradiation conditions (power density, burnup, and temperature) became more aggressive.

Two fuel plates [21] were required for this experiment, each with a different fuel meat design. The fuel meats consisted of a U-7Mo fuel phase dispersed in (1) aluminum-2 wt% silicon alloy (Al-2 Si) matrix and (2) Al4043 alloy matrix in fuel plates identified as 1T2 and 1B5, respectively. The fuel meat was encapsulated in Al-6061 alloy cladding using the historic manufacturing processes developed to fabricate fuel for the INL's Advanced Test Reactor (ATR) and ORNL's High Flux Isotope Reactor (HFIR) [22,23]. Eight fuel plates (four of each composition) were fabricated by Babcock and Wilcox Nuclear Operations Group (B&W NOG) in Lynchburg, VA using standard commercial processes [24]. The U-7Mo fuel powder was produced by the Korean Atomic Energy Research Institute (KAERI) using centrifugal atomization [25]. The aluminum alloy matrix particles were produced by HJE Company (Queensbury, NY) using gas atomization.

The key fuel meat design parameters of the two fuel plates selected for irradiation are summarized in Table 1. Although the plate length (~57 cm) is only half the active length of the ATR core (~120 cm), this plate length is typical of most MTR fuel designs and the plates are still considered 'full-size' for development testing purposes. The digitized x-ray images of the fuel plates used to establish homogeneity are shown in Figure 1. The average fuel volume fraction in the fuel meat is estimated by using the total fuel loading and the x-ray images to measure the fuel meat length and width. Based on the nominal fuel meat thickness (e.g. 0.508 mm), the average volume fraction of fuel in the fuel meat is calculated to be ~8.8 and 8.7 g U/cm³ for plate 1T2 and 1B5, respectively.

The local homogeneity of the fuel loading can be assessed quantitatively using the relative image density in x-ray radiographs taken on each plate prior to irradiation. The distribution of fuel density within the fuel zone is shown for both plates in Figure 2. The data shows that the spot size over which the analysis is performed can significantly alter the results. The spot size value selected for quality inspections is dictated by the thermal hydraulic safety analysis of each reactor facility (typically 3 mm). A very small spot size exaggerates the impact of 'hot spots' on the fuel plate associated with high local power density. Analysis of the data shows that the fuel volume fraction (and standard deviation) for plate 1T2 is 52.1±10.9% and

51.1±7.1% for a 50 μm and 3 mm spot size, respectively. The fuel volume fraction for plate 1B5 is 51.5±12.5% and 50.3±9.1% for a 50 μm and 3 mm spot size, respectively. Although analysis of more plates is required to demonstrate statistical significance, this data is consistent with anecdotal evidence collected during fabrication of mini-plates for earlier test campaigns. These tests indicated that increases in silicon content in the matrix correlated with higher inhomogeneity in fuel distribution and subsequent variability in the local fuel loading.

[Insert Table 1]

[Insert Figure 1]

[Insert Figure 2]

After the completion of all quality assurance inspections, the two plates were welded into the frame assembly as shown in Figure 3. Note that the plates are welded to the frame in only two corners at the mid-plane of the assembly and that the plates are nested into groves cut into the rails. This allows unconstrained axial and transverse expansion of the plates during irradiation. The two fuel plates were then pre-treated in an autoclave to produce a thin adherent layer of boehmite to the surface. This process has been historically demonstrated to control cladding corrosion in the Advanced Test Reactor (ATR) [26].

[Insert Figure 3]

2.1 Irradiation History

The test plate assembly was inserted into the ATR Full-size-plate Irradiation in a flux-trap Position (AFIP) test rig [27] (Figure 4) that was specially designed to fit into the Center Flux Trap (CFT) position of the ATR. The device is capable of holding two fuel plate assemblies and can be disassembled and reconfigured in the ATR fuel handling canal between ATR irradiation cycles. As shown in Table 2, the AFIP-1 experiment was irradiated for three full ATR cycles in position 1 of the irradiation vehicle. Position 2 of the irradiation vehicle was occupied by the AFIP-2 fueled test plate assembly, an aluminum dummy plate assembly, and the AFIP-3 fueled test plate assembly, respectively, during each successive irradiation cycle.

[Insert Figure 4]

[Insert Table 2]

The as-run calculations were performed based on the irradiation history (shown in Table 2 and Figure 5) and using the Monte Carlo N-Particle (MCNP) code, ORIGEN2.2 and MCWO [28]. The fuel zone of each plate was divided into 10 axial nodes to calculate the power density and depletion as function of the axial flux shape in the

ATR core. Calculations were performed at four discrete time points during each irradiation cycle (including beginning-of-cycle, two mid-cycle points, and end of cycle). The local power history, expressed as fuel zone surface heat flux and calculated fission density distribution at the end of each irradiation cycle are shown in Figures 6 and 7, respectively.

[Insert Figure 5]

[Insert Figure 6]

[Insert Figure 7]

The fuel plate surface temperature is determined based on the hydraulic conditions within the coolant channels and the local convective heat transfer coefficients. Estimation of the hydraulic conditions was based on out-of-pile flow tests. The fuel plate surface temperatures as a function of time were then calculated using the local power values provided by the neutronic calculations and using the Colburn model for convective heat transfer. The approximate fuel plate surface temperatures at the end of each irradiation cycle are shown in Figure 8. Mismatch in temperature predicted on each side of the plate during the first and third cycles is caused by the additional power being generated in the neighboring fuel plate assembly during those cycles.

[Insert Figure 8]

It is important to note that the centerline temperature of the fuel plate cannot be reasonably estimated without knowledge of the thickness and spallation behavior of aluminum-hydroxide that forms on the plate surface and the internal microstructure of the fuel meat. The presence of significant amounts of fuel/matrix interaction product, and accounting for the large uncertainty in its thermal preproperties, can also lead to large variations in temperature [29]. These conditions will be evaluated in a future paper that will report on the destructive examination and analysis phase of this experiment.

2.2 Post-irradiation Examination Methods

The condition of the fuel plates was evaluated both in the ATR fuel handling canal between irradiation cycles and in the INL Hot Fuel Examination Facility (HFEF). The in-canal examinations were performed using both visual and ultrasonic (UT) examination. The UT examination is performed with a novel device based on the inspection methods routinely used on fuel plates prior to irradiation. After a period of cooling in the ATR canal, the fuel plates were shipped to the INL Hot Fuel Examination Facility (HFEF) for post irradiation examination. Non-destructive examinations performed in the HFEF hot cell facility included visual inspection, gamma scanning, oxide thickness measurement, and plate metrology. Destructive examination results will be addressed in a future article.

The in-canal UT inspection device was developed at the INL specifically for inspection of research reactor fuel plates. The system allows for the evaluation of acoustic transmission through the fuel plate and time-of-flight (TOF) acoustic reflection measurement of the fuel plate thickness between each irradiation cycle. This inspection allows for the geometric evolution of the plate to be tracked throughout the life of the experiment. For the AFIP-1 experiment, measurements were performed four times (prior to irradiation and after the 1st, 2nd and 3rd irradiation cycles). The device is based on the use of two detectors³⁰ located on opposite sides of the plate that generate and monitor the response of ultrasonic signals sent toward the plates. The detectors are mounted on a rail system that moves in the x-y plane and allows them to be positioned over any location of the plates. This system is used to examine the fuel plates on an extremely fine grid where measurements are taken at 80 μm intervals in the x and y directions so that ~5 million data points are collected on each plate.

The two signal types can be used to identify 1) the presence of large voids in the fuel plates that are typically a pre-cursor to blister formation and subsequent loss of fission product encapsulation and 2) the distance from the detector to the surface of the plate. The second set of data is used to evaluate the general shape and local thickness of the plate.

After the completion of irradiation, the experiment was transported to the INL HFEF hot cell facility using the GE-2000 cask. Upon arrival, the fuel plate assembly was examined visually and photographed to identify anomalies. The assembly was then examined using the HFEF Remote Fuel Metrology System (shown in Figure 9) to measure the local aluminum oxide and plate thickness distributions. The system allows probes to be maneuvered to various measurement locations over the plates, which are held in an aluminum fixture, using an X Y positioning system. The X Y positions are measured with Sony Magna Scales. The oxide measurement is performed using a Fischer Dual Scope FMP 220 model ETA3.3h eddy current probe that makes contact with the plate and measures the aluminum oxide film thickness on the plate. Twenty-one measurements were taken on one face of each plate following a 3x7 grid. The same measurement stage is then used to determine the local plate thickness using opposing Sony Digital Scale probes with a round contact point 4.4 mm in diameter. The plate thickness was measured in sixteen strips down the plate and at 175 axial locations of each plate (e.g. on a 3 mm x 3mm grid over the entire plate for a total of ~3000 data points). Gamma scanning was then performed (after ~100 days of cooling) using the Precision Gamma Scanner (PGS) (which fundamentally consists of a collimator, a germanium detector system, and a positioning stage) along an axial strip down the center of the fuel plate assembly. The slit size was 0.254 mm x 1.27 mm and energy resolved measurements were taken every 1.27 mm along the axial length of the plates.

[Insert Figure 9]

Upon completion of the non-destructive examinations, two small samples were extracted from the center of each fuel plate for chemical burnup analysis. The two samples were located 17.5 and 49.5 cm from the end of the fuel plate marked with the plate identifier. Samples were transported from the HFEF to the Analytical Laboratory hot cells using a pneumatic transfer system where they were dissolved and analyzed for chemical composition using an inductively coupled plasma – mass spectrometer (ICP-MS).

3. Results

3.1 In-Canal Ultrasonic Inspection

A series of ‘debond scans’ based on through-transmission of the ultrasonic signal were taken before irradiation and after each irradiation cycle to track the evolution of any macroscopic porosity during irradiation. These images are shown in Figure 10 and 11 for plates 1B5 and 1T2, respectively. The images show an increase in transmission (indicated by image lightening) after the first irradiation cycle. This suggests improved contact between the fuel particles and matrix due to pressure from fuel swelling and bonding due to chemical interaction. Darkening of the image in later cycles indicates that transmission is being reduced as the burnup increases. This is likely due to the growth of oxide on the plate surface, formation of increasing amounts of fuel/matrix interaction products, and distributed porosity in the fuel meat. However, no large voids indicative of blister formation are observed.

[Insert Figure 10]

[Insert Figure 11]

Thickness scans were also performed on each plate to evaluate the general geometry of the fuel plates and fuel plate swelling. The plate thickness maps are shown in Figure 12 and 13. These maps confirm the anticipated general relationship between increasing burnup at the upper edge and increased fuel plate thickness growth. The presence of greater growth around the edges of the fuel plates is also significant and shows the effect of fuel meat relocation in this region. These features are more clearly shown by plotting the plate thickness along transverse (see Figure 14) and axial (see Figures 15 and 16) locations on the plate. The transverse values shown in Figure 14 are the average values for the region between the edge peaks shown in Figure 13. Because the transverse variation in burnup is minimal in these plates, the shown thickness value is effectively for a single, unique burnup condition.

It is also important to note that, although the device is calibrated using a known standard before each scan, the thickness measurements must be re-zero-ed to support quantitative analysis due to small drifts in measurement values that occur due to local water temperature variation caused by decay heat from the plate. These small changes alter the speed of sound in the water and therefore the TOF used for calibration purposes must be corrected. The effect can clearly be identified

and correcte by comparing stable areas of the fuel plate (like the non-fueled rail region atboth ends of the each plate). Adjustments to the data are made using these reference areas to allow direct comparison of plate thickness between cycles.

[Insert Figure 12]

[Insert Figure 13]

[Insert Figure 14]

[Insert Figure 15]

[Insert Figure 16]

3.2 Post-Irradiation Examination

3.2.1 Visual Examination

After arrival in the HFEF hot cells, the plates were photographed to document their general condition (Figure 17). The plates did not show any anomalous features. Variations in color are normally associated with aluminum hydroxide corrosion layer formation on the plate (e.g darker areas are typically associated with thicker corrosion layer). Layer thickness and composition normally correlates with the local cladding surface temperature (and thus power) and, as a result, the fuel zone is clearly outlined by the corrosion layer. It is worth noting the thin region around the edge of the fuel zone that does not exhibit visual evidence of a corrosion layer. This feature is due to the enhanced cooling provided by the two-dimensional thermal transport in the rail regions of the plate that lower the local fuel plate surface temperature and significantly slows local oxide growth.

[Insert Figure 17]

3.2.2 Gamma scan and chemical burnup analysis

Gamma scanning of the fuel plate assembly chemical analysis was performed to benchmark the depletion calculations. To support the chemical burnup analysis, samples were extracted from two locations in each fuel plate, dissolved in nitric acid, and analyzed by mass spectroscopy (ICPMS) to determine the isotopic composition. The analysis results for plutonium and uranium are shown in Table 3 (along with the composition of pre-irradiation samples). The average fission density achieved in each sample was estimated using the following formula.

$$fd_{fuel} = \left(\frac{n_{BOL}^{U235} - n_{EOL}^{U235} - n_{EOL}^{U236}}{n_{BOL}^U} \right) \left(\frac{N_{av}}{AW_{BOL}} \rho_{UMo} m f_U \right) \quad (1)$$

where n is the number of atoms of the specified isotope at either the beginning- or end-of-life in the chemical analysis sample, N_{av} is Avogadro's number, AW_{BOL} is the average atomic weight of the uranium at beginning of life (237.4 gm/mol), ρ_{UMo} is

the uranium-molybdenum alloy density (17.1 g/cm³), and mf_U is the mass fraction of uranium in the alloy (0.93 for U-7%wt Mo alloy). The beginning of life U atom value was determined using the following equation

$$n_{BOL}^U = \left(\frac{n_{EOL}^{U238} + n_{EOL}^{Pu}}{1-e} \right) \quad (2)$$

where e is the pre-irradiation enrichment (19.8%). The fission density in the fuel meat is calculated by multiplying the fission density in the fuel by the loading fraction.

$$fd_{fuelmeat} = fd_{fuel} V_f \quad (3)$$

This method accounts for conversion of U-238 into Pu isotopes, conversion of U-235 into U-236, and fissioning of U-235. The conversion of U-238 into Pu-239 and subsequent fissioning is neglected.

The uncertainty associated with the fission density calculations is a function of several measurement errors used to derive quantities included in Equation 1. These include the beginning and end of life atom density measurements, UMo alloy density, alloy compositions, and fuel loading volume fraction. The order of magnitude for each value and its error is summarized in Table 4. The relative contribution of each error can be evaluated by substituting the following relationship for each variable into Equation 1.

$$A_t = A_m + \partial A$$

This equation shows the relationship between the ‘true value’ (A_t) and a combination of the ‘measured value’ (A_m) and an error term (∂A). Equation 1 is then expanded and the lowest order terms are eliminated to develop Equation 4 (see Appendix for derivation).

$$\frac{\delta fd_{fuel}}{fd_{fuel}} \approx \frac{\delta n_{BOL}^{U235}}{(n_{BOL}^{U235} - n_{EOL}^{U235} - n_{EOL}^{U236})} + \frac{\delta \rho_{UMo}}{\rho_{UMo}} + \frac{\delta mf_U}{mf_U} \quad (4)$$

The percent uncertainty can therefore be estimated. Assuming uncertainties for a chemical analysis of 0.05%, alloy density of ~0.3 (compared to 17.1 g/cm³), and alloy composition 0.01% (compared to 0.93% nominal) the uncertainty in the chemical burnup analysis data should be ~±2%.

The gamma scan, chemical analysis and depletion calculation results are shown in Figure 18. Because the calculated results were used to determine U-235 depletion, conversion to U-236 was ignored, the calculated results were linearly scaled by a factor of 0.88 to agree with the measured results for fission density.

It should be noted that the gamma scan data shows significant peaking at the ends of the fuel plates. The observed edge peaks likely occur due to a combination of several factors, including a concentration of fuel at the end of the fuel plates. The fuel meat behavior that drives this phenomenon will be discussed in a later section.

[Insert Figure 18]

[Insert Table 3]

3.2.3 *Oxide measurement*

The thickness of the aluminum oxide corrosion layer on the fuel plate surface was measured to support analysis of the fuel plate thermal conditions. Because the thermal conductivity of the corrosion layer is so low, thin layers can have a meaningful impact on the fuel meat temperature during irradiation. The measured oxide thickness data is compared to the values predicted using the ATR modified Griess correlation (e.g. $0.7 \cdot \text{Griess}$ [31]) in Figure 19. The calculated values for oxide thickness are extremely sensitive to both the growth correlation selected and the convective heat transfer correlation selected. General assessment of the modified Griess correlation performance in other fuel tests shows good agreement with data collected from fuel plate regions that operated at high power ($> \sim 300\text{-}400 \text{ W/cm}^2$ surface heat flux). However, the oxide growth rate is substantially over-predicted in lower power applications like the AFIP-1 test and the Griess correlation must be more severely modified to a $0.2 \cdot \text{Griess}$ to better match the observed behavior [32].

[Insert Figure 19]

The oxide thickness observed in this experiment is remarkably thin compared to similar test conducted at other reactors worldwide [33,45]. This can be attributed to the use of boehmite pre-film on the Al-6061 alloy cladding which has been shown to limit oxide formation during irradiation in the ATR coolant chemistry conditions. As the formation of even very thin oxide layers can significantly increase the fuel centerline temperature by several 10's of °C, it is important to account for this effect on fuel performance when comparing to irradiation tests conducted under similar power and burnup conditions in other test reactors. Estimation of the oxide thickness may be required to correct fuel plate thickness data. In the case of E-FUTURE [33] and SELENIUM [45], for example, corrosion layer thickness was as high as 40 μm and represented a relevant fraction of the plate thickness. In the case of the AFIP-1 experiment, the very thin layer results in very little measurement error.

3.2.4 *Post-irradiation Thickness Measurements*

The fuel plate thickness was measured in the hot cell to confirm the fuel plate growth distribution maps collected using the in-canal UT scanner. A map for both plates is shown in Figures 12 and 13. Excellent agreement with the ultrasonic inspection data is observed. The plates show the anticipated general pattern of growth, with the maximum coinciding with the peak burnup areas. However, it is clear that some relevant features (e.g. the edge growth) are not identified in these maps.

4. Data Analysis

4.1 Plate growth at the fuel meat edges

One of the most prominent features identified in the plate thickness maps is the presence of a prominent ridge along the edges of the fuel meat zone. The height of the ridge appears to correlate with the fission density in that region. Initial review of the ridge would suggest that it is caused by either local power peaking or accelerated fuel swelling. However, detailed analysis of both transverse and axial stripes through the ridges show that the local growth of the ridge is driven by fuel meat creep away from the rail region.

As shown in Figure 20, the maximum increase in fuel plate volume in the ridge region is offset by a relatively thinner zone near the edge of the fuel. The thin region exhibits reduced plate swelling because the fuel meat growth is suppressed by mechanical constraint imposed by the rail of the cladding. This effect was clearly observed during the early stages of fuel development for the INL Material Test Reactor and during other U-Mo dispersion based fuel irradiation tests [34]. The mechanisms driving this phenomenon were recently explained analytically for the U-Mo dispersion [35] and monolithic [36] fuel systems. This behavior is strongly dependent on the material properties of the cladding, fuel meat swelling, and the irradiation enhanced creep rate of the fuel meat. Much like the approach used to study the monolithic fuel system, analytical study of this data could be used to determine the effective creep rate coefficient for a heterogeneous dispersion fuel meat.

[Insert Figure 20]

This behavior is not expected to significantly impact overall fuel behavior. However, the fuel meat within the region located just inside the fuel meat thickness ridge is subjected to a tensile stress due to differential fuel meat growth [37]. The net stress on the fuel meat is dependent on the magnitude of external pressure applied by the coolant. If the combined compressive stress of the coolant and ridge formation is less than the internal gas pressure within pores of the fuel meat, accelerated fuel meat porosity formation and premature onset of blistering may occur. Incipient indications of this behavior have been qualitatively identified during metallography performed on U-Mo dispersion mini-plates irradiated in earlier studies.

4.2 Fuel Plate Swelling vs. Burnup

Because very little variation in the power or burnup occurs across the width of the plate, width-averaged thickness values were calculated and used for the analysis performed in this section. The ~ 10 mm zone that contains the creep induced ridge discussed previously is excluded from the calculated averages to simplify the analysis. The width averaged plate swelling is calculated by subtracting the pre-

irradiation plate thickness at the given axial location from the thickness measured after each irradiation cycle.

The local burnup distribution along the length of the fuel plate was calculated at several intervals through the irradiation as was previously described. While the gamma scan data collected using the HFEF PGS is not calibrated to provide absolute burnup determinations, the relative burnup distribution can be determined (as shown previously in Figure 18).

Using the measured plate swelling and the calculated local fission densities, the local swelling in each axial region can be linked to the local fission density. The fuel meat swelling as a function of local burnup for each irradiation cycle is shown for both plates in Figure 21 and 22. While the fuel meat swelling nominally appears to increase linearly with fission density, a non-linearity in the swelling rate appears evident in the high power region of the 1T2 fuel plate after the first irradiation cycle. Following this cycle the swelling rate appears to be reduced until after the final irradiation cycle where it has effectively returned to the rate common to the rest of the plate.

[Insert Figure 21]

[Insert Figure 22]

4.3 Fuel Swelling

In order to directly compare the performance of a specific fuel meat design with other similar experiments using a different fuel meat design (i.e. fuel loading), the fuel swelling is typically expressed in terms of 'fuel phase swelling.' In this representation, all of the fuel plate growth is attributed to the fuel phase only and is defined as the change in fuel plate volume divided by the original fuel phase volume. This assumption is reasonable to the first order but ignores density changes due to interaction production formation, irradiation-induced swelling of the interaction product, and fuel meat pore formation or closure due to irradiation effects. In some cases, specific factors that contribute to fuel plate swelling can be isolated and corrections to the calculation can be made. Specifically, the thickness of low density oxide on the plate surface and pre-irradiation porosity of the fuel meat can be measured and accounted for in determination of the fuel plate volume change. The plate thickness is converted into the local volume change using the following equation (developed in [33]),

$$S_f = \frac{(t_p - \frac{1}{1.975} t_{ox}) - t_{p0}}{(t_{p0} - 2t_c) V_f} \quad (4)$$

where S_f is the fuel phase swelling, t_p is the local plate thickness, t_{ox} is the local surface oxide thickness, t_{p0} is the initial local plate thickness, t_c is the cladding thickness, and V_f is the as-fabricated fuel volume fraction in the fuel meat. The fuel volume fraction is calculated from the fuel alloy density and fuel meat loading to be

55.8% and 55.3% for plate 1T2 and 1B5, respectively. Because the fuel meat has substantial porosity in the as-fabricated condition, fuel phase growth typically occurs without significant fuel plate swelling as the fuel meat porosity is partially closed. The measured fuel meat porosity is reported in Table 1. The fuel phase swelling required to close this porosity is (porosity/fuel volume fraction=) 5.5% and 4.9% for plate 1T2 and 1B5, respectively. The fuel phase swelling curve calculated using Equation 4 above should therefore be corrected (increased) by this amount. However, some fraction of this porosity is not closed during irradiation and more realistic correction of swelling results for this porosity requires detailed microstructural examination of the fuel meat. This results in a small overcorrection of the swelling data. For this data set, the correction for oxide thickness has been neglected. As is described in the next section, the oxide thickness for either plate in this experiment is less than 10 μm thick and makes a negligible contribution to the uncertainty in the data for most of the fuel plate. The fuel phase swelling as a function of fuel phase fission density is shown in Figure 23.

[Insert Figure 23]

4.4 Uncertainty in Fuel Swelling Calculations

The uncertainty associated with the swelling calculations is a function of several measurement errors used to derive quantities included in Equation 4. These include the plate thickness measurements, oxide thickness measurement, assumed cladding thickness, and fuel loading volume fraction. The order of magnitude for each value and its error is summarized in Table 4. The relative contribution of each error can be evaluated by substituting the following relationship for each variable into Equation 4.

$$A_t = A_m + \partial A$$

This equation shows the relationship between the ‘true value’ (A_t) and a combination of the ‘measured value’ (A_m) and an error term (∂A). Equation 1 is then expanded and the lowest order terms are eliminated to develop Equation 5 (see Appendix for derivation).

$$S_f + \partial S_f \approx \frac{(t_p - t_{p0})}{(t_{p0} + 2t_c)V_f} \left(1 + \frac{\partial V_f}{V_f} - \frac{\frac{1}{1.975}t_{ox}}{(t_p - t_{p0})} \right) \quad (5)$$

The percent uncertainty can therefore be estimated from the loading variation identified previously and the oxide thickness. For a nominal loading of 55% and a standard deviation of 10% expected for the local measurement used in Figure 1 (~50 μm spot size), the scatter in the local fuel swelling data should be $\sim \pm 10\%$. However, it is important to note the uncertainty is only moderately reduced if a larger spot size is considered (such that the uncertainty in loading is reduced). For example, if a spot size of 3 mm^2 is used the standard deviation decreases slightly to $\sim 6.5\%$. However, as the uncertainty in loading is decreased, the oxide thickness

correction becomes more significant. Assuming a high burnup region where the oxide growth (on the order of 10 μm) and plate thickness increase is greatest (on the order of 0.12 mm), the fuel swelling is over-predicted by $\sim 4\%$ if not corrected for oxide growth.

[Insert Table 4]

Note that in the AFIP-1 case the variability in fuel meat loading is so high and the oxide thickness is so low that the oxide correction can be neglected in the fuel swelling calculations (although it would still be the dominant source of uncertainty in the plate swelling calculations). This is small when compared to similar experiments discussed later (E-FUTURE and SELENIUM) where the peak oxide thickness is on the order of 40 μm , the error associated with oxide can be as high as 20% and must be accounted for in swelling calculations.

The uncertainty in fission density is a function of both the calculated value and the assigned location. The fission density as determined using MCWO described in section 2.1. However, inspection of the normalized gamma scan and calculated fission density data shown in Figure 18 shows significant scatter in the measured data and that the calculations under-predict the fission density on the low burnup 1/4 of the 1B5 fuel plate by a margin on the order of 10%. The scatter in the overall data can likely be attributed to local variability in the loading. However, the departure of the calculations from the measured data, along with the very small amount of fuel swelling observed at the low burnup end of plate 1T2 during cycle 2, suggests that control rod extraction may have distorted the typical ATR flux distribution by 'pushing' the neutron flux to the lower half of the reactor during some stages of the irradiation.

This plot can also be used to evaluate the impact of measurement location uncertainty. It is evident from the gamma scan data that the fission density variation over several mm on the fuel plates is quite small. Combined with the fact that the thickness measurements are collected on a very fine grid with location uncertainties in the range of μm , the uncertainty associated with measurement location are negligible.

For analysis of the AFIP-1 fuel plates it is clear that the uncertainty in the fuel swelling is completely dominated by the uncertainty in the local fuel loading and uncertainties related to thickness measurement and oxide thickness can be neglected without significant impact.

5. Discussion

The irradiation behavior of U-Mo alloy when configured in a standard dispersion fuel meat design using Al matrix for research reactor applications was first assessed

through irradiation of micro- and nano-plates² in the RERTR-1, -2, and -3 campaigns conducted using the ATR. Based on the apparent stable irradiation behavior observed in the alloy during microstructural examination, mini-plate samples with more prototypic dispersion fuel meat configurations were tested in the RERTR-4 and -5 campaigns. While bulk failure of the fuel meat was not observed in these tests, the presence of porosity formation at the interface between the Al matrix and U-Mo-Al interaction layer was identified. Concurrent with the RERTR-4/5 tests, full-size plate tests were being conducted in Europe at OSIRIS (France), IVV-2M (Russia) and BR2 (Belgium). The IRIS-1 test, conducted in OSIRIS, demonstrated stable behavior similar to that observed in the RERTR tests. However, the use of ground powder, as opposed to spherical atomized powder, may have attributed to the lack of pillow formation. However, the IRIS-2 [38] test revealed that when a critical operating power threshold was exceeded, the pores observed in the RERTR-4/5 test were prone to interlinkage, which ultimately induced plate pillowing. Comparable behavior was observed in the IVV-2M test [39], which was conducted at nominal peak power less than 100 W/cm². This behavior was further confirmed by the FUTURE test [40], conducted at BR2, which exhibited accelerated swelling in high power regions of the plates. It is worth noting that in all three high power tests, RERTR-4/5, IRIS-2, and FUTURE, the morphology of the fuel meat defects were similar but the failure mode (or lack thereof) was quite different. In the case of the RERTR-4/5 samples, defect interconnection was very limited and gross fuel meat swelling was not observed. In the FUTURE test, defect formation and coalescence was observed leading to accelerated swelling but bulk interconnection did not occur. In the IRIS-2 test, defect interconnection was very significant and led to the formation of a large pillow in the high burnup/power region.

Two thresholds for onset of accelerated swelling can be identified from this data. The IRIS-1 and IRIS-2 tests, conducted at different power levels, suggest that the critical fuel meat power density to induce accelerated swelling is below 240 W/cm². It is difficult to estimate the threshold value using the IRIS-1 and IVV-2M tests due to use of ground U-Mo powder and operational anomalies that occurred during irradiation, respectively. The IRIS-2 and FUTURE tests suggest a common fission density of $\sim 2 \times 10^{21}$ f/cm³ in the fuel phase for onset of accelerated swelling behavior. The variability in fuel meat behavior during the accelerated swelling phase might be explained by the relative coolant pressures in the test reactors used for these experiments. The coolant pressure in the ATR, BR2, and OSIRIS reactors are approximately 25, 12.5, and 3 atm, respectively. It is conceivable that as the external pressure is increased, the ability of pressurized pores in the fuel meat to grow and interconnect is suppressed. The onset of accelerated swelling and/or pillowing is thereby also suppressed. However, this does not explain the genesis of the fuel meat pores and the common fission density threshold for changes in bulk fuel meat behavior.

² Micro-, nano, and mini-plate descriptors represent plates with dimensions of 22x76, 10x41, and 25x100 (all in mm), respectively.

Analysis of these tests suggested that the formation of the U-Mo-Al interaction product was a primary driver for fuel meat failure. It was proposed and demonstrated through irradiation testing that the addition of small amounts of Si to the Al matrix would suppress thick U-Mo-Al based interaction layers in favor of thin U-Mo-Al-Si interaction layers. This phenomena was first demonstrated in the RERTR-6, -7, and -9 mini-plate irradiation campaigns where the tendency for pore formation was shown to decrease as the silicon content in the matrix was increased from 0 to 5 wt% in the Al. These tests also indicated that a change in fuel meat swelling rate gradually occurred at fission densities between 3 and 5×10^{21} f/cm³ in the fuel phase [41]. This change was attributed to the onset and progression of 'grain refinement' and changes in the bubble morphology within the fuel particles. The transition also coincided with the first observation of pore formation in the silicon-modified fuel meat. As in the previous tests, bulk fuel meat defects leading to pillowing were not observed. Full-size plate tests were subsequently conducted in both the US and Europe. The AFIP-1 test showed behavior consistent with the mini-plate test results. However, as is shown in this paper, indications of fuel meat instability early in irradiation of the Al-2Si plate was noted, even though it did not lead to pillowing. Three tests were conducted at OSIRIS using the same Al-2Si matrix design. The IRIS-3 test [38], which was conducted at low power density, showed stable fuel behavior while the IRIS-TUM [38] and IRIS-4 [38] tests, conducted at similar power to AFIP-1, resulted in plate pillowing. Two very high power tests were conducted at BR2 using high Si matrix designs. The E-FUTURE-1 test [33,43] used Al-4Si and Al-6Si matrix designs where plate pillowing was observed in the highest power regions of the plates. The E-FUTURE-2 test [44] used a very high silicon content matrix design (Al-8Si and Al-12Si) to evaluate the limits of the silicon effect. The plates in this test failed due to mechanical buckling at fission density less than 3×10^{21} f/cm³ in the fuel phase.

It can be concluded from the tests using Al-2%Si matrix that the formation of fuel meat pores can be retarded to much higher fission densities and the critical power threshold can be increased to ~ 250 W/cm² of fuel meat. It is possible this is enabled by protecting the integrity of the (nominally) Al matrix and improving fission gas management (due to interaction layers dominated by the dual layer U-Mo-Al-Si and U-Mo-Si compositions rather than U-Mo-Al compositions). In all three full-size plate cases, the onset of accelerated swelling and, in the IRIS-4 case, subsequent pillowing, occurs at approximately 3 - 4×10^{21} f/cm³. However, it again appears that external coolant pressure plays a role in the progression of pillowing. In the AFIP-1 plate containing 2% Si addition, accelerated swelling is observed at approximately 3×10^{21} f/cm³ in the fuel phase. This is then followed by an equilibration phase that eventually brings the swelling back to the linear rates observed for the rest of the plate. The IRIS-TUM case, like the IRIS-1, used ground powder rather than spherical atomized powder, which may have suppressed defect interlinkage thereby minimizing the rate of swelling acceleration and preventing pillowing.

Increasing the Si content of the matrix to 4-6% was shown to further stabilize the fuel meat in AFIP-1 plate 1B5 (Al-5%Si) and E-FUTURE-1. In these plates stable fuel

swelling was observed up to $\sim 6 \times 10^{21}$ f/cm³ in the fuel meat unless the local fuel meat power density exceeded ~ 450 W/cm², where pillowing was observed in the E-FUTURE-1 plates. While the fission density in the pillowed region was $\sim 5 \times 10^{21}$ f/cm³ at the end of irradiation, no quantitative data is available at this location earlier in the test. It is therefore not possible to definitively determine the critical fission density for onset of pillowing. Similar results were observed in the SELENIUM [45,46] test which included fuel plates where the U-Mo particles were coated with thin layers of Si or ZrN and irradiated under virtually identical conditions to the E-FUTURE tests.

Despite the consistencies in the onset thresholds, the phenomena occurring within the fuel meat that induce the transitions have not yet been well defined. In general, the fuel meat swelling can be described as the combined effect of several phenomena including fuel phase swelling, thermal-chemical interaction between the fuel and matrix, porosity closure and formation, thermal-mechanical response of the fuel meat to stress, and fission gas distribution within the fuel meat. The fuel swelling curves shown in Figure 23 can therefore be sub-divided into three distinct fission density dependent regimes.

Swelling observed in the first stage ($< \sim 2 \times 10^{21}$ ff/cm_{fuelmeat}³ depending on as-fabricated porosity) is dominated by solid fission product swelling of the fuel phase and closure of the as-fabricated porosity. During this stage the fuel is subject to stresses induced by differences in thermal expansion coefficients in Al and U-Mo as well as the local stress concentrations induced by non-homogeneous loading. This stress may create delamination between the materials if the 'bond strength' is exceeded. However, if the bond strength is not exceeded, this stress will decrease gradually due to creep in both the matrix and embedded particles. In the case of the U-Mo/Al system (tested in the IRIS-1, IRIS-2, IVV-2M, RERTR-4/5, and FUTURE experiment described previously), the bond quality between the Al and U-Mo-Al interaction product appears to be below the critical bond strength threshold, likely leading to formation of voids that eventually become fission gas sinks in the fuel meat. Once filled with gas being released from the U-Mo-Al interaction layer, this network of defects begins to interconnect and form a pillowed or blistered region in the fuel meat. Alternatively, if the matrix is robust enough to limit the creep induced strain rate in the fuel meat and thereby limit interfacial damage (as was apparently the case in RERTR-6/7/9, IRIS-3, IRIS-TUM, and E-FUTURE-1 experiments described above), the fuel meat stress can gradually equilibrate without excessive damage formation. It appears this stabilization is provided by the combined effect of Si addition which limits interaction layer formation, strengthens the Al to U-Mo-Al-Si layer interfacial bond, and hardens the matrix.

The threshold for transition to the second stage ($\sim 2 < f < 4.5$ ff/cm_{fuelmeat}³) is likely dependent on the specific fuel meat design (including extent of porosity, particle size distribution, homogeneity, etc.). A 'compliant' fuel meat configuration that allows for rapid plastic deformation once as-fabricated porosity is closed (i.e. with small fuel particles, low fuel loadings, and/or operating at high temperature) will

respond to this phase more favorably than a 'stiff' fuel meat. The second stage of fuel swelling is dominated by solid fission product swelling that result in measurable change in the fuel plate geometry. This change requires significant fuel meat creep (in the fuel phase, matrix, or both) which translates isotropic fuel meat growth into plate thickness increase. The silicon modified fuel meat designs perform well during this phase with one exception. The E-FUTURE-2 test which contained large amounts of silicon in the matrix (Al- 8 and -12 wt% Si) appeared to have a 'non-compliant' fuel meat that buckled under the stress of fuel particle swelling.

The third stage ($< \sim 4.5 \times 10^{21} \text{ f/cm}_{\text{fuel meat}}^3$) of the swelling is the most complex and variable between fuel tests. In this phase the onset of grain refinement within the fuel particle leads to bulk formation of micron scale fission gas bubbles and accelerated fuel phase swelling. In turn, this accelerated swelling can enhance mechanical stress exerted on the fuel meat and, if insufficient constraint is applied by the remaining matrix fibers, lead to formation of defects on the order of 10's of μm . The relatively large size of these meat defects can significantly increases the fuel meat growth rate if fission gas can readily be transported to them.

While the accelerated swelling in the fuel phase during the third stage that is associated with bubble formation in the fuel phase can generally be tolerated, the formation of larger fuel meat defects and their pressurization with fission gas can lead to breakaway fuel swelling and, in some cases, fuel plate failure. In the AFIP-1 experiment, the breakaway swelling observed during the third stage in the IRIS and E-FUTURE-1 tests is not evident. It is presumed that this behavior is somehow prevented by suppressing the large void formation in the fuel meat.

Since the experiments were very similar in composition and were conducted in three separate reactors with different irradiation test devices and irradiation environments, the operating conditions may be a key differentiating factor in their behavior. Most notably, differences in coolant pressure may have played a significant role in stage three large void evolution. It is evident from the three interim examinations of the 1T2 plate (Al-2%Si) that significant swelling occurred in the peak power region during the first cycle before reaching the end of stage one. In the IRIS-4 campaign [13], the first indications of pillowing were also identified in this fission density range. However, growth in this high swelling region of 1T2 appears to dramatically slow and return to 'normal' by the end of the irradiation while in IRIS-4 it accelerates and end in pillowing. This early growth in both tests may be caused by fuel meat defects formed during the mechanical deformation of stage one. In the case of IRIS-4, these defects remain open and later expand and interconnect due to the combined effects of fission gas pressurization, matrix consumption, and fuel particle growth. In the case of plate 1T2, the external coolant pressure may have offset the forces applied by fission gas pressurization of the defects and limited their growth and interconnection. This small stress relief may have been sufficient to keep the fuel meat intact long enough to achieve roughly twice the burnup IRIS-4.

In contrast, the higher silicon fuel plate (1B5) did not exhibit this growth during stage one. In this case the matrix-particle defects do not appear to form. This can be attributed to the increased fuel meat toughness achieved through higher silicon additions. Matrix alloys containing ~5% Si appear to be an optimal balance where the probability of plate pillowing due to early defect formation and buckling due to insufficient matrix creep is minimized. However, higher Si matrix designs are not immune to the stage one defect formation phenomena. As the beginning-of-life power is increased, the stresses exerted on the fuel meat also increase and a critical threshold is likely to exist for which defects will form. Based on the results from E-FUTURE-1, which had similar composition to plate 1B5 but exhibited pillowing, that threshold is likely between 250 and 450 W/cm² surface heat flux. However, the lack of interim examination results for E-FUTURE-1 prevent definitive confirmation of this hypothesis.

The relative behavior of these tests can be described using analogs to other heterogeneous materials like concrete. The first stage of swelling is similar to concrete under thermal load during a fire. Studies^{47, 48} have shown that significant stress is applied to the material interfaces as the temperatures increase (with higher interfacial stresses in spherical aggregate than in crushed aggregate). The second phase is mathematically similar to matrix shrinkage observed in curing concrete. It has been well established that localized stresses result in micro-cracks in the matrix during this process⁴⁹ (analogous to those observed in the ZrN-coated particle tests in SELENIUM⁵⁰). The density of micro-cracks increases with larger aggregate size, spherical aggregate (compared to angular aggregate) and when there is strong bonding between the particle and matrix (weak bonding allows crack blunting). The wide range of conditions observed during the third stage could also be interpreted within this context. Tests on concrete containing spherical and crushed aggregate demonstrated the superior fracture toughness of angular aggregates and when the matrix is weakly bonded to the particle (again due to crack blunting)⁵¹. This observation is consistent with the enhanced performance of ground particles observed in the IRIS-1 and IRIS-TUM experiments when compared to spherical powders. This also suggests that the mechanical properties of the U-Mo-Al(-Si) interaction layers and their bond quality likely play a role in fuel meat stability. Further study of these properties through destructive examination is required for further understanding. The lower 'toughness' configurations would also be expected to be more susceptible to catastrophic failures like those seen in FUTURE, IRIS-3, IRIS-4, and E-FUTURE-1.

6. Conclusions

The irradiation performance of U-Mo alloy dispersion fuel plates with an Al-Si matrix (Al-2%Si and Al-4043) were evaluated under prototypic configuration and irradiation conditions using the ATR AFIP test vehicle. Full size plates loaded to ~8.8 g U/cm³ were produced by B&W NOG using atomized U-7Mo powder supplied

by KAERI. Geometric evolution of the fuel plates was monitored between irradiation cycles through interim inspection in the ATR canal using a specially designed ultrasonic scanning bench. Post irradiation non-destructive examination including visual examination, gamma scanning, oxide thickness measurement, and profilometry was then performed in the HFEF.

The examinations showed that the rate of fuel swelling as a function of fission density was consistent with other similar experiments. However, breakaway swelling was not observed. Previous full-size plate tests conducted internationally using a pure aluminum matrix or Al-2%Si irradiated under slightly different environmental conditions did exhibit this behavior. Indications that the initial stages of breakaway were imminent in the AFIP-1T2 plate (Al-2%Si fuel meat design) were present but may have been suppressed by reactor coolant pressure long enough for the fuel meat to stabilize. No breakaway indications were seen in the AFIP-1B5 plate (Al-4043 (~4%Si) fuel meat design). These results support the stabilizing effect of silicon addition and suggest that the effect is enhanced by addition of larger amounts of silicon. It also reinforces that fuel performance is dependent on factors beyond selection of the fuel phase material. These also include the specific fuel meat design features and external reactor conditions.

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8. Appendix

8.1 Uncertainty in the chemical analysis

The uncertainty in the fission density as calculated from the chemical analysis data can be estimated using the following derivation. Starting with equation 1 that was used to calculate the fission density,

$$fd_{fuel} \approx \left(\frac{n_{BOL}^{U235} - n_{EOL}^{U235} - n_{EOL}^{U236}}{n_{BOL}^U} \right) \left(\frac{N_{av}}{AW_{BOL}} \rho_{UMo} m f_U \right)$$

Represent each ‘measured value’ as a combination of the ‘true value’ and the ‘measurement error’.

$$\begin{aligned} & fd_{fuel} + \delta fd_{fuel} \\ & \approx \left(\frac{n_{BOL}^{U235} + \delta n_{BOL}^{U235} - n_{EOL}^{U235} + \delta n_{EOL}^{U235} - n_{EOL}^{U236} + \delta n_{EOL}^{U236}}{n_{BOL}^U + \delta n_{BOL}^U} \right) \left(\frac{N_{av} + \delta N_{av}}{AW_{BOL} + \delta AW_{BOL}} \right) (\rho_{UMo} \\ & + \delta \rho_{UMo})(m f_U + \delta m f_U) \end{aligned}$$

The first term can be reduced by eliminating lower order terms;

$$\begin{aligned} & \frac{n_{BOL}^{U235} + \delta n_{BOL}^{U235} - n_{EOL}^{U235} + \delta n_{EOL}^{U235} - n_{EOL}^{U236} + \delta n_{EOL}^{U236}}{n_{BOL}^U + \delta n_{BOL}^U} \\ & \approx \frac{O(3 \cdot 10^{21}) + O(10^{19}) - O(5 \cdot 10^{20}) + O(10^{18}) - O(6 \cdot 10^{19}) - O(10^{17})}{O(3 \cdot 10^{21}) + O(10^{19})} \\ & \approx \frac{n_{BOL}^{U235} - n_{EOL}^{U235} - n_{EOL}^{U236}}{n_{BOL}^U} + \frac{\delta n_{BOL}^{U235}}{n_{BOL}^U} \end{aligned}$$

followed by reduction of the second term;

$$\begin{aligned}
& \left(\frac{N_{av} + \delta N_{av}}{AW_{BOL} + \delta AW_{BOL}} \right) (\rho_{UMo} + \delta \rho_{UMo})(mf_U + \delta mf_U) \\
& \approx \frac{(N_{av}\rho_{UMo} + \delta N_{av}\rho_{UMo} + N_{av}\delta \rho_{UMo} + \delta N_{av}\delta \rho_{UMo})(mf_U + \delta mf_U)}{AW_{BOL} + \delta AW_{BOL}} \\
& \approx \frac{O(6 \cdot 10^{23})O(2 \cdot 10^1) + O(10^{20})O(2 \cdot 10^1) + O(6 \cdot 10^{23})O(10^{-1}) + O(10^{20})O(10^{-1})}{O(2 \cdot 10^2) + O(10^{-1})} (O(10^0) \\
& + O(10^{-2})) = \frac{(N_{av}\rho_{UMo} + N_{av}\delta \rho_{UMo})(mf_U + \delta mf_U)}{AW_{BOL}} \\
& \approx \frac{(N_{av}\rho_{UMo}mf_U + N_{av}\delta \rho_{UMo}mf_U + N_{av}\rho_{UMo}\delta mf_U + N_{av}\delta \rho_{UMo}\delta mf_U)}{AW_{BOL}} \\
& \approx \frac{O(6 \cdot 10^{23})O(2 \cdot 10^1)O(10^0) + O(6 \cdot 10^{23})O(10^{-1})O(10^0) + O(6 \cdot 10^{23})O(2 \cdot 10^1)O(10^{-2}) + O(6 \cdot 10^{23})O(2 \cdot 10^1)O(10^{-2})}{O(2 \cdot 10^2)} \\
& \approx \frac{O(6 \cdot 10^{23})O(2 \cdot 10^1)O(10^0) + O(6 \cdot 10^{23})O(10^{-1})O(10^0) + O(6 \cdot 10^{23})O(2 \cdot 10^1)O(10^{-2})}{O(2 \cdot 10^2)} \\
& \approx \frac{N_{av}\rho_{UMo}mf_U}{AW_{BOL}} + \frac{(N_{av}\delta \rho_{UMo}mf_U + N_{av}\rho_{UMo}\delta mf_U)}{AW_{BOL}}
\end{aligned}$$

The equation simplifies after recombining the two terms

$$\begin{aligned}
& fd_{fuel} + \delta fd_{fuel} \\
& \approx \left(\frac{n_{BOL}^{U235} - n_{EOL}^{U235} - n_{EOL}^{U236}}{n_{BOL}^U} + \frac{\delta n_{BOL}^{U235}}{n_{BOL}^U} \right) \left(\frac{N_{av}\rho_{UMo}mf_U}{AW_{BOL}} \right. \\
& \quad \left. + \frac{(N_{av}\delta \rho_{UMo}mf_U + N_{av}\rho_{UMo}\delta mf_U)}{AW_{BOL}} \right)
\end{aligned}$$

The uncertainty term is therefore as follows;

$$\begin{aligned}
\delta fd_{fuel} & \approx \left(\frac{\delta n_{BOL}^{U235}}{n_{BOL}^U} \right) \left(\frac{N_{av}\rho_{UMo}mf_U}{AW_{BOL}} \right) \\
& + \left(\frac{n_{BOL}^{U235} - n_{EOL}^{U235} - n_{EOL}^{U236}}{n_{BOL}^U} \right) \left(\frac{(N_{av}\delta \rho_{UMo}mf_U + N_{av}\rho_{UMo}\delta mf_U)}{AW_{BOL}} \right) \\
& \approx \frac{(n_{BOL}^{U235} - n_{EOL}^{U235} - n_{EOL}^{U236})N_{av}\rho_{UMo}mf_U}{n_{BOL}^U AW_{BOL}} \left(\frac{\delta n_{BOL}^{U235}}{(n_{BOL}^{U235} - n_{EOL}^{U235} - n_{EOL}^{U236})} \right. \\
& \quad \left. + \left(\frac{\delta \rho_{UMo}}{\rho_{UMo}} + \frac{\delta mf_U}{mf_U} \right) \right) \\
\frac{\delta fd_{fuel}}{fd_{fuel}} & \approx \frac{\delta n_{BOL}^{U235}}{(n_{BOL}^{U235} - n_{EOL}^{U235} - n_{EOL}^{U236})} + \frac{\delta \rho_{UMo}}{\rho_{UMo}} + \frac{\delta mf_U}{mf_U}
\end{aligned}$$

8.2 Uncertainty in the fuel swelling

The uncertainty in the calculated swelling values can be estimated using the following derivation. Starting with equation 1 that was used to calculate the fuel swelling,

$$S_f = \frac{(t_p - \frac{1}{1.975}t_{ox}) - t_{p0}}{(t_{p0} - 2t_c)V_f}$$

Represent each 'measured value' as a combination of the 'true value' and the 'measurement error'.

$$S_f + \partial S_f = \frac{(t_p + \partial t_p) - (t_{p0} + \partial t_{p0}) - \frac{1}{1.975}(t_{ox} + \partial t_{ox})}{[(t_{p0} + \partial t_{p0}) - 2(t_c + \partial t_c)](V_f + \partial V_f)}$$

Expand the relationship,

$$\begin{aligned} S_f + \partial S_f &= \frac{(t_p + \partial t_p) - (t_{p0} + \partial t_{p0}) - \frac{1}{1.975}(t_{ox} + \partial t_{ox})}{[(t_{p0}V_f + \partial t_{p0}V_f + t_{p0}\partial V_f + \partial t_{p0}\partial V_f) - 2(t_cV_f + \partial t_cV_f + t_c\partial V_f + \partial t_c\partial V_f)]} \end{aligned}$$

and eliminate the lowest order terms,

$$\frac{(0(1) + \cancel{O(0.001)}) - (0(1) + \cancel{O(0.001)}) - (0(0.01) + \cancel{O(0.001)})}{[(0(1) + \cancel{O(0.001)}) + O(0.1) + \cancel{O(0.0001)}) - (O(1) + O(0.01) + O(0.1) + \cancel{O(0.001)})]}$$

$$S_f + \partial S_f \approx \frac{(t_p - t_{p0} - \frac{1}{1.975}t_{ox})}{[(t_{p0}V_f + t_{p0}\partial V_f) - 2(t_cV_f + \partial t_cV_f + t_c\partial V_f)]}$$

Expand again,

$$S_f + \partial S_f \approx \frac{(t_p - t_{p0} - \frac{1}{1.975}t_{ox})}{[(t_{p0} + 2t_c - 2\partial t_c)V_f - (t_{p0} + 2t_c)\partial V_f]}$$

$$\begin{aligned} S_f + \partial S_f &\approx \frac{(t_p - t_{p0} - \frac{1}{1.975}t_{ox})}{[(t_{p0} + 2t_c - 2\partial t_c)V_f - (t_{p0} + 2t_c)\partial V_f]} \frac{[(t_{p0} + 2t_c - 2\partial t_c)V_f + (t_{p0} + 2t_c)\partial V_f]}{[(t_{p0} + 2t_c - 2\partial t_c)V_f + (t_{p0} + 2t_c)\partial V_f]} \end{aligned}$$

$$S_f + \partial S_f \approx \frac{(t_p - t_{p0} - \frac{1}{1.975}t_{ox})[(t_{p0} + 2t_c - 2\partial t_c)V_f + (t_{p0} + 2t_c)\partial V_f]}{[(t_{p0} + 2t_c - 2\partial t_c)^2V_f^2 - (t_{p0} + 2t_c)^2\partial V_f^2]}$$

and eliminate the lowest order terms

$$\frac{(O(1) - O(1) - O(0.01))[O(1) + O(1) - \cancel{O(0.01)} + O(0.1) + O(0.1)]}{[(O(1) + O(1) - \cancel{O(0.01)})O(1) - (O(1) + O(1))\cancel{O(0.01)}]}$$

Reduce the relationship, expand, rearrange, and reduce,

$$S_f + \partial S_f \approx \frac{\left(t_p - t_{p0} - \frac{1}{1.975} t_{ox}\right) [(t_{p0} + 2t_c)V_f + (t_{p0} + 2t_c)\partial V_f]}{[(t_{p0} + 2t_c)^2 V_f^2]}$$

$$S_f + \partial S_f \approx \frac{(t_p - t_{p0})}{(t_{p0} + 2t_c)V_f} + \frac{(t_p - t_{p0})}{(t_{p0} + 2t_c)} \frac{\partial V_f}{V_f^2} - \frac{\frac{1}{1.975} t_{ox} [V_f + \partial V_f]}{[(t_{p0} + 2t_c)V_f^2]}$$

$$O(1) + O(.1) - \frac{O(0.01)[O(1) + \cancel{O(0.1)}]}{O(1)}$$

The uncertainty can then be expressed as two terms; the first representing uncertainty associated with variations in fuel loading and the second associated with oxide growth.

$$\partial S_f \approx \frac{(t_p - t_{p0})}{(t_{p0} + t_c)V_f} \frac{\partial V_f}{V_f} - \frac{\frac{1}{1.975} t_{ox}}{(t_{p0} + t_c)V_f}$$

$$\partial S_f \approx \frac{(t_p - t_{p0})}{(t_{p0} + 2t_c)V_f} \left(\frac{\partial V_f}{V_f} - \frac{\frac{1}{1.975} t_{ox}}{(t_p - t_{p0})} \right)$$

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Table 1. Key design and fabrication parameters of the irradiated fuel plates.

Table 2. AFIP-1 experiment irradiation history.

Table 3. AFIP-1 experiment chemical burnup analysis results. (fission yields from <http://www.nndc.bnl.gov/exfor/endl02.jsp>)

Table 4. Order of magnitude of values and uncertainty factors in AFIP-1 swelling analysis inputs

	Plate ID	
	1B5	1T2
Fuel Alloy	U-7.0 wt% Mo	U-7.0 wt% Mo
Matrix Alloy	Al-4043 (~Al-4.5 wt % Si)	Al-1.9 wt% Si
Cladding Alloy	Al-6061	Al-6061
U-Mo Particle Size Dist.	(wt%)	(wt%)
<45µm	27.0%	27.0%
>45 µm, <125 µm	72.7%	72.7%
>125 µm	0.3%	0.3%
Matrix Particle Size Dist.		
>44 µm	6%	6%
<44 µm, >18 µm	53%	54%
<18 µm, <8 µm	29%	28%
<8 µm	12%	12%
Fuel Alloy Density	16.9±0.1 g/cm ³	16.9±0.1 g/cm ³
Fuel Meat Loading Fraction	8.7 gU/cm ³	8.8 gU/cm ³
As-fabricated Fuel Meat Porosity	2.7%	3.1%
Nominal Plate Dimensions		
Fuel Plate Length	57.2cm	
Fuel Plate Width	5.7 cm	
Fuel Plate Thickness	0.127 cm	
Fuel Meat Length	53.6 cm	
Fuel Meat Width	4.3 cm	
Fuel Meat Thickness	0.0508 cm	

Table 1.

ATR Cycle	AFIP Test ID	AFIP Frames Irradiated	Dates Irradiated	Cycle EFPDs	Mid-Cycle Scram Decay Days	Post-Cycle Decay Days	Center Flux Trap Power (MW)	Total Core Power (MW)	Est. Peak Power at BOC (W/cm ²)	Est. Peak Fission Density at EOC (f/cm ³ meat)
142B	AFIP-1/2	A,B	07/04/2008 – 08/30/2008	52.0	5	25	25.4	116.8	250	1.6 x10 ²¹
143A	AFIP-1	B	09/24/2008 – 12/06/2008	48.9	25	17	24.3	112.2	170	2.7 x10 ²¹
143B	AFIP-1/3	A,B	12/23/2008 – 02/20/2009	57.3	2	21	24.9	113.0	100	3.4x10 ²¹

Table 2.

Sample ID	pre-irradiated	1T2-A	1T2-B	1B5-A	1B5-B
	mg/g sample				
139La	--	2.660	3.390	3.040	3.550
141Pr	--	2.540	3.230	2.870	3.390
234U	0.1253	0.107	0.094	0.1	0.09
235U	19.7807	7.194	3.5	5.405	3.037
236U	0.1575	2.605	3.243	2.93	3.321
238U	79.9365	90.09	93.16	91.56	93.55
Total U	100	903.6	904.6	898.7	946.1
238Pu	--	1.564	3.115	2.253	3.509
239Pu	--	67.35	57.69	62.55	56.14
240Pu	--	18.25	20.09	19.22	20.09
241Pu	--	10.11	12.5	11.69	12.77
242Pu	--	2.724	6.605	4.286	7.489
Total Pu	--	8.488	10.55	9.49	11.283
Fissions/cm ³ meat	--	2.5E+21	3.1E+21	2.8E+21	3.3E+21

Table 3.

Input	Nominal Value	Value	Uncertainty
Plate Thickness (via UT)	1.27± 0.001 mm	O(1)	O(0.001)
Oxide Thickness	0.01 ± 0.001 mm	O(0.01)	O(0.001)
Cladding Thickness	0.381 ± 0.020 mm	O(1)	O(0.01)
Fuel Volume Fraction	0.55 ± 0.1 %	O(1)	O(0.1)

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Figure 4. AFIP test vehicle.

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Figure 2. Loading distribution based on image analysis of x-ray radiographs.

Figure 3. AFIP fuel plate frame assembly containing 2 fuel plates (top and bottom), rail (left and right), and handling fixtures (top and bottom). The two plates are welded to the frame in the center and are free to expand axially and transversely into grooves cut into the rail. The handling fixtures provide structural rigidity during handling.

Figure 4. AFIP test vehicle components and configuration showing a) the 5 components including the ramrod, ram, 2 fuel plate assemblies, and the flux trap insert and b) top view in the assembled configuration.

(a)

(b)

(c)

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(a)

(b)

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