Accident Analyses for Conversion of the University of Missouri Research Reactor (MURR) from Highly-Enriched to Low-Enriched Uranium

Nuclear Engineering Division

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Accident Analyses for Conversion of the University of Missouri Research Reactor (MURR) from Highly-Enriched to Low-Enriched Uranium

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December 2014

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EXECUTIVE SUMMARY

This report contains the results of reactor accident analyses for the University of Missouri Research Reactor (MURR). The calculations were performed as part of the conversion from the use of highly-enriched uranium (HEU) fuel to the use of low-enriched uranium (LEU) fuel. The analyses were performed by staff members of the Global Threat Reduction Initiative (GTRI) Reactor Conversion Program at the Argonne National Laboratory (ANL), the MURR Facility, and the Nuclear Engineering Program – College of Engineering, University of Missouri-Columbia. The core conversion to LEU is being performed with financial support from the U. S. government.

In the framework of non-proliferation policies, the international community presently aims to minimize the amount of nuclear material available that could be used for nuclear weapons. In this geopolitical context most research and test reactors, both domestic and international, have started a program of conversion to the use of LEU fuel. A new type of LEU fuel based on an alloy of uranium and molybdenum (U-Mo) is expected to allow the conversion of U.S. domestic high performance reactors like MURR. This report presents the results of a study of core behavior under a set of accident conditions for MURR cores fueled with HEU U-Al_x dispersion fuel or LEU monolithic U-Mo alloy fuel with 10 wt% Mo (U-10Mo).

For the proposed LEU-fueled core, previous studies [1] have shown that in order to maintain the neutron flux at various crucial experimental locations within the reactor at the same level after the fuel conversion from the HEU to LEU, the steady-state operating power level must be increased from 10 MW to 12 MW. Hence, the accidental positive reactivity insertions, and the loss-of-coolant and loss-of-flow accidents for the LEU core were initiated from an initial steady-state power level of 12 MW. It is noted that all references to the core power level for MURR in this report are the *thermal* power output of the reactor (i.e., MW_t).

A search of the published literature and available technical reports was conducted to obtain appropriate values of thermal conductivity and volumetric heat capacity for the materials of 1) the irradiated and unirradiated HEU and LEU fuel meat, 2) the zirconium layers in the LEU fuel plates, 3) the 6061-aluminum of the fuel plate cladding, the reactor vessel walls, and primary system pipes, and 4) the oxide layer on the cladding surfaces of the irradiated fuel plates. This set of material properties was used in the analyses of the reactivity insertion accidents (RIA), loss-of-coolant accidents (LOCA), and loss-of-flow accidents (LOFA).

The transient simulations of RIAs, LOCAs, and LOFAs are initiated from the conservative end of the normal operating band for reactor power, reactor primary coolant flow, and reactor core inlet temperature. The typical core state for the MURR was assumed in these simulations. This state has elements with a mixture of burnups, the flux trap is loaded with irradiation samples and experiments, and the control blades are banked in the same axial position. This state was also evaluated in the steady-state safety analyses for the core loaded with either HEU or LEU fuel.

Reactivity Insertion Accidents:

Accidents resulting from the uncontrolled or unanticipated insertion of positive reactivity in the MURR core have been evaluated to assess the impact of fuel conversion on reactor safety. The MURR Safety Analysis Report (SAR) [6] postulated three step reactivity insertion accidents and two continuous ramp reactivity insertion accidents. These were evaluated with the PARET/ANL code [19] and shown to result in no damage to the core fueled with HEU.

The PARET/ANL model parameters employed in the MURR SAR were examined in detail and updated with appropriate values for both HEU and LEU fueled cores in a previous analysis [4]. Specifically, the PARET/ANL model was revised to utilize reactivity feedback coefficients, detailed power distributions, and control bank worths for the reactor safety system that were calculated for HEU and LEU fueled cores with a detailed MCNP5 [14] core model. In this preliminary accident analysis, the PARET/ANL code was used to predict peak fuel temperatures in fresh HEU and LEU plates in the reference cores.

In the current work, the PARET/ANL models were enhanced to include depleted as well as fresh fuel plates. The depleted fuel plates will have a lower local heat flux in the reference cores compared with the fresh plates. However, the thermal conductivity is known to degrade with fuel irradiation. Additionally, the growth of an oxide layer on the fuel plates during residence in the core, plus the restriction of the coolant channel gap due to the oxide layer and irradiation-induced dimensional changes, may cause fuel temperatures to be higher in the depleted fuel plates relative to the fresh fuel plates previously evaluated. Furthermore, a series of sensitivity studies were executed to analyze the potential impact of variations in parameters such as thermo-physical properties or core operating conditions.

For the most limiting accident considered, which is a 0.6% $\Delta k/k$ step insertion of reactivity in a critical core at nominal full power, the peak HEU U-Al_x fuel meat temperature modeled during the reactivity transient was 194 °C (381 °F) in a fresh fuel plate in a reference core that is typical for MURR operations. In various cases with uncertainties the peak fuel temperature rose to 203 °C (397 °F). This fuel temperature is well below the measured fuel blister threshold temperatures at or above 480 °C (896 °F) for HEU U-Al_x dispersion fuel [20].

The same accident is also the most limiting for the LEU fuel, resulting in an estimated peak U-10Mo temperature of 264 °C (507 °F) in the limiting plate of a middle-of-life element and 262 °C (504 °F) in the limiting plate of an end-of-life element. The calculated peak U-10Mo temperature for the limiting plate in a fresh element was 240 °C (465 °F). This case included a reduction in thermal conductivity for the irradiated fuel, which at end-of-life was estimated to be 65% of the fresh value. The peak LEU fuel temperature modeled in various cases with uncertainties rose to 271 °C (520 °F). The additional cases were explored in order to estimate the sensitivity of the calculated fuel temperature to uncertainties in the measured thermal conductivity of irradiated fuel, the thermal resistance of the oxide layer that forms on the plate surface, initial steady-state operating conditions, and other modeling assumptions that could affect the analysis.

Loss-of-coolant and Loss-of-flow Accidents:

The MURR SAR demonstrates that for all credible loss-of-primary-coolant and loss-of-primary-flow accidents (LOCAs and LOFAs) unacceptable consequences to people and the environment would be avoided. For the conversion of the MURR core from HEU to LEU fuel, an analogous demonstration is made for the LEU core that replaces the existing HEU core. Both HEU and LEU cores are analyzed and compared with the aid of the RELAP5 code [17], and are shown to have adequate safety margins.

The LOCAs are distinguished from the LOFAs by having a loss of primary coolant. In the LOFAs there is no loss of primary coolant. The following four LOCAs were analyzed:

1. A double-ended break of the main 12-inch pipe at the reactor inlet
2. A double-ended break of the main 12-inch pipe at the reactor exit
3. An 8-inch hole in the reactor inlet pipe at a flow control valve
4. A 2-inch hole in the pipe that connects the pressurizer to the reactor inlet pipe.

The following three LOFAs were analyzed:

1. A loss-of-site power
2. Inadvertent closure of both main isolation valves
3. Locked rotor in one of two main coolant pumps

In addition, analysis to demonstrate the effectiveness of the anti-siphon system is provided with the aid of the RELAP5 code.

The very substantial RELAP5 simulation model of the MURR plant provided by the MURR SAR explicitly represented all of the core coolant channels and fuel plates of a single fuel element. The SAR model assumed that the core geometry was that for a core in which all elements were fresh. In the current analysis, this model is replaced with an updated set of models in which for HEU or LEU the single fuel element represented in the model is either a beginning-of-life (BOL) element, middle-of-life (MOL) element, or end-of-life (EOL) element. Improvements to the SAR RELAP5 model include:

1. Additional detail in an accurate representation of the plant protection system
2. Improved reactor core hydraulic parameters
3. The effects of fuel burnup are incorporated
4. An improved in-pool heat exchanger model
5. An improved anti-siphon system model
6. LEU and HEU fuel plate thermal properties were updated based on experimental measurements.

The initial steady-state reactor conditions assumed in the current RELAP5 analysis represent the most severe for normal operation. The current analysis shows that the highest peak fuel temperature for all of the LOCA/LOFA transients is 139 °C (282 °F) for the HEU fuel, which is only 31 °C (56 °F) above the initial steady state maximum value. Similarly, for the LEU fuel,

the highest transient peak fuel temperature is 157 °C (314 °F) which is only 13 °C (24 °F) above the initial steady state maximum value.

Maximum Hypothetical and Fuel Handling Accidents:

The MURR SAR evaluated the dose consequences to workers and the general public from two types of postulated accidents that result in the release of radioactive material. These accidents were also analyzed in this work to compute the doses for the reactor converted to LEU fuel, and to provide a comparison to the doses from the existing HEU fuel in the MURR core.

The first of these postulated accidents that results in a release of radioactive material is the Maximum Hypothetical Accident (MHA). In the MHA, an assumption is made that four fuel plates in the peak heat flux region of the core overheat and melt, releasing the inventory of noble gasses and radioiodines in those plates. The specific cause of this accident is immaterial to the analysis. The occupational dose to workers in the MURR containment was calculated in the SAR to be 132 mrem for the HEU fuel. Using a similar set of assumptions for the LEU-fueled core, the occupational dose is calculated to be 403 mrem. This increase is due to 1) the 20% core power uprate from 10 MW to 12 MW needed for the conversion to LEU fuel to maintain experimental performance at the MURR and 2) the assumption of a larger fraction of the total fission product inventory released in the LEU MHA based on the fuel element geometry and core power distribution. In both cases, the calculated occupational dose is well below the 5 rem regulatory limit for radiation workers.

The dose to the general public from the MHA was also calculated. The point of maximum dose to a member of the general public in the unrestricted area around the reactor containment was identified. The dose at the location was calculated to be 0.03 and 0.05 mrem, respectively, for the HEU and LEU cores, which is well below the regulatory annual limit of 100 mrem for members of the general public.

Fuel handling accidents (FHA) are categorized into the following events or scenarios: a fuel mishandling event that breaches the cladding on a fuel plate, simple failure of the fuel cladding due to a manufacturing defect or corrosion, and overheating of a fuel element with subsequent potential cladding failure as might occur in RIA, LOCA, or LOFA accidents. The fuel handling accident and fuel defect accidents are modeled with the same power history and plate location as the MHA, and are encompassed by the MHA.

Overall Accident Analysis Conclusions for Conversion:

The peak temperatures are much lower than the expected blister temperatures for all reactivity insertion, loss-of-coolant, and loss-of-flow accidents considered. The highest temperature occurs in the LEU core 0.6% $\Delta k/k$ reactivity accident, with analysis results showing peak fuel temperatures of 271 °C (520 °F) or less for all credible accident cases considered.

The calculated U-10Mo fuel temperature remained well below the lowest temperature at which blisters have occurred during post-irradiation blister testing conducted to date. For the LEU MURR cores, the EOL peak local fission density has been calculated to be between 1.1×10^{21} and 3.4×10^{21} fissions/cm³ where the highest burnup is found in the outer plates of the element [22]. For the monolithic UMo fuel, blisters have been observed in the range of 400 to 530 °C (752 to

986 °F) when irradiated fuel plates were placed in a furnace at fission densities relevant to MURR [21]. Monolithic UMo plates exceeding these burnup levels, up to 7×10^{21} fissions/cm³, have been blister tested and exhibit a blister temperature no lower than 365 °C (689 °F) [21]. This preliminary UMo monolithic data was measured across a range of U-10Mo plate geometries, fabrication variables and irradiation conditions. Fuel plates specifically representative of MURR geometry and irradiation conditions will be blister annealed in future tests.

Together with the preliminary blister measurements, results indicate that fuel conversion to a proposed U-10Mo monolithic fuel will result in no damage to fresh or depleted fuel plates for any credible accident postulated for MURR.

Finally, for any event where a release is considered in the current HEU SAR, the radiological consequences after conversion remain lower than the regulatory limits.

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1.0 INTRODUCTION

The University of Missouri Research Reactor (MURR) is a multi-disciplinary research and education facility providing a broad range of analytical and irradiation services to the research community and the commercial sector. The source of neutrons for the experimental facilities of the MURR is a compact fuel region which has a fixed geometry consisting of eight fuel elements, each having identical physical dimensions, as shown in Figure 1.1. The fuel elements are placed vertically around an annulus between two cylindrical aluminum pressure vessels. The MURR is currently fueled with highly-enriched uranium (HEU) in an aluminide dispersion (U-Al_x) fuel.

Figure 1.1. MURR Core Layout.

In the framework of non-proliferation policies, the international community presently aims to minimize the amount of nuclear material available that could potentially be diverted for nuclear weapons. In this geopolitical context, most research and test reactors, both domestic and international, have started a program of conversion to the use of low-enriched uranium (LEU) fuel. A new type of LEU fuel based on an alloy of uranium and molybdenum (U-Mo) is expected to allow the conversion of U.S. domestic high performance reactors like MURR.

Safety analyses of the MURR core fueled with a monolithic alloy of uranium and 10 wt% molybdenum (U-10Mo) were previously conducted to establish the steady-state safety basis of the reactor using the CD35 fuel element design [1, 2]. The thermal-hydraulic analyses of the reactor required detailed power distributions and geometrical considerations in order to identify regions of interest for safety analyses. In order to understand the impacts of accident and transient scenarios in a core fueled with an LEU U-10Mo element, a set of accident analyses have been performed. NUREG-1537 guidelines [3] postulate nine classes of accident-initiating scenarios to be considered.

Preliminary analyses of those accident scenarios that have been most challenging to fuel integrity were previously completed for cores loaded with LEU U-10Mo fuel elements [4]. These “Phase I” analyses for reactivity insertion (RIA), loss-of-coolant (LOCA), and loss-of-flow (LOFA) accidents predicted peak fuel centerline temperatures that were well below preliminary measurements of the blister temperature for the proposed monolithic LEU fuel type, but only fresh fuel plates were considered.

In the current “Phase II” accident analyses the modeling has been expanded to include analysis of depleted fuel plates representing cores with the mixture of burnup states used during MURR fuel management. The depleted fuel plates will have a lower heat generation rate relative to the fresh fuel, but the thermal conductivities of the irradiated U-Al_x (HEU) and U-10Mo (LEU) fuel are degraded relative to the fresh fuel, which could result in higher fuel temperatures. Additionally, the formation of an oxide layer on the plate surface during operation will elevate the plate clad and fuel temperatures. Lastly, the reduction of the coolant channel thickness between fuel plates due to oxide growth and irradiation-induced dimensional changes will also be modeled. The Phase II accident analyses also include development and application of models that eliminate unnecessary conservatism from the preliminary accident analyses, where such enhancements are reasonable and clearly defensible.

Chapter 2 provides a summary of the thermo-physical properties of the fuel and structural materials that are required in the modeling of the accidents. Chapter 3 describes the initial core operating conditions assumed for the accident analyses. The reactivity insertion accident modeling and analysis are described in Chapter 4. Plant transients involving loss-of-coolant and loss-of-flow scenarios are described in detail in Chapters 5 and 6, respectively. The maximum hypothetical accident (MHA) and several fuel handling accidents (FHA) have been also analyzed to predict dose consequences as part of the Phase II accident analysis. Separate reports issued by MURR provide the details of the modeling and analysis, but the predictions for the MHA and FHA scenarios are summarized in Chapter 7. Lastly, the conclusions of the full set of accidents analyzed in this work are provided in Chapter 8.

2.0 THERMO-PHYSICAL PROPERTIES

The thermo-physical properties of the materials of construction for the MURR fuel plates and reactor structural components are needed for proper modeling of heat conduction and calculation of temperatures. A search of the published literature and available technical reports was conducted to obtain appropriate data. The data used in the modeling of the RIA, LOCA, and LOFA cases are reported in this chapter.

2.1 HEU Fuel Meat

2.1.1 U-Al_x Thermal Conductivity

The value of the thermal conductivity for the fresh HEU fuel meat can be obtained from Reference 5. In Table 4 of that reference, the volume-% of U-Al_x of 35.4% was added to the percent porosity of 6% to obtain a total value of 41.4%. Then in Figure 8 of the reference, at 41.4 volume-% (fuel + voids), the data indicate that the thermal conductivity for the fresh U-Al_x is in the range of 40 to 45 W/m-C. A lower bound value of 30 W/m-C is used for the thermal conductivity of high-burnup MURR HEU fuel, as discussed in Chapter 10 of Reference 2. The U-Al_x thermal conductivity will be assumed to reduce linearly from 40 W/m-C for fresh HEU fuel, to 30 W/m-C for the maximum burnup HEU fuel (average element burnup of 150 MWd) in the current accident analysis. This is the same approach used in the steady-state safety analysis [2].

2.1.2 U-Al_x Heat Capacity

Reference 6 indicates that the fuel used in the MURR HEU core has the same composition as the uranium aluminide (U-Al_x) fuel used in the Advanced Test Reactor. The density and heat capacity of this fuel are density = 6.4 g/cm³ and heat capacity = 0.387 + 0.00022 T in J/g-C, where T is in K and the applicable range is 20 to 600 °C [5]. These data will be used in the current accident analyses. Table 2.1 compares the current values for heat capacity with those that were used in both the MURR Safety Analysis Report (SAR) [6] RELAP5 [7] model and the Phase I model. It should be noted that RELAP/SCDAPSIM/MOD3.4 was used in the SAR analysis, as it was the production version at the time. Based on the Phase I analysis, the expected peak fuel temperature for the RIA in the HEU core is less than 230 °C, and for the HEU cold-leg LOCA the peak fuel temperature is less than 200 °C. As Table 2.1 shows, between 20 and 260 °C the current data values are 29% to 35% greater than those used in the RELAP5 models that were used in the MURR HEU SAR and in the Phase I analysis. Increasing the heat capacity of the fuel meat will reduce the peak transient temperature rise.

2.2 LEU Fuel Meat

2.2.1 U-10Mo Thermal Conductivity

Reference 8 provides correlations for the thermal conductivity and specific heat capacity for unirradiated U-10Mo. Burkes et al. developed these correlations by averaging Burkes' measured data for the thermal conductivity of U-10Mo with data from other references. Only the data

Table 2.1. Heat Capacity for HEU Fuel.

Temperature		Current Data [5]		SAR [6] & Phase I [4] Models		Ratio of Current Data to SAR and Phase I Models
°F	°C	Btu/ft ³ -F	kJ/m ³ -C	Btu/ft ³ -F	kJ/m ³ -C	
68	20.0	43.09	2890	33.50	2247	1.29
500	260.0	48.12	3227	35.60	2388	1.35
1000	537.8	53.96	3619	38.17	2560	1.41
1112	600.0	55.26	3706			
3000	1648.9	77.28	5183	48.46	3250	1.59

measurements in Reference 8 conducted by Burkes for the unirradiated U-10Mo are used for the current accident analysis. A comparison of the data and linear least-squares fits with temperature are presented in Figure 2.1. It can be seen from the figure that the thermal conductivity of the unirradiated U-10Mo is larger when only Burkes' data are taken into account. However, it was noted in Reference 8 that a laser flash diffusivity technique was employed by Burkes for measuring the thermal conductivity, which is considered more accurate than the electrical conductivity measurement techniques utilized by the other researchers. Therefore, utilizing the other measurement data for the thermal conductivity of fresh U-10Mo would result in overly conservative estimates of the fuel temperature. The resulting linear fit of the Burkes data from Reference 8 for the thermal conductivity of unirradiated U-10Mo is:

$$\kappa = 14.821 + 0.0309 \cdot T \text{ W/m-C [T in } ^\circ\text{C]} \quad (2.1)$$

Figure 2.1. Thermal Conductivity Data for Unirradiated U-10Mo from Reference 8.

Reference 9 reported the results of measurements of thermal conductivity and specific heat capacity for samples of unirradiated U-10Mo and irradiated U-10Mo with fission densities ranging from 2.9 to 4.1×10^{21} fissions/cm³. The thermal conductivity for the irradiated samples at 50, 150, and 300 °C were reported graphically. In the present work, these data were fit with a correlation that was linear with fission density and 2nd order with temperature, as follows:

$$\kappa = 12.57 + T \cdot 0.040 - N_F \cdot (1.322 + 0.00278 \cdot T) - T^2 \cdot (2.351 \times 10^{-5} + 4.996 \times 10^{-6} \cdot N_F) \quad (2.2)$$

where T is the temperature in °C, N_F is the fission density in 10^{21} fissions/cm³, and κ is the thermal conductivity in W/m-C. The RMS error of this correlation is 0.5%. Figure 2.2 summarizes the thermal conductivity evaluated from this correlation as a function of temperature for several fission density values of interest for the Phase II analyses. The fit of the unirradiated thermal conductivity derived from the data in Reference 8 (Equation 2.1) is also included in Figure 2.2.

Figure 2.2. Thermal Conductivity for Irradiated U-10Mo Derived From Reference 9.

It is noted, first, that the thermal conductivity for unirradiated samples from Reference 9 is about 14% less than the Burkes 2010 data from Reference 8. This may be partly due to the large experimental uncertainty for the unirradiated U-10Mo thermal-conductivity measurements in Reference 9 (the experimental error bars are roughly ± 15 -20%). The experimental uncertainty

for the irradiated sample measurements is much smaller (~5-10%). It is further observed that the thermal conductivity of the U-10Mo is about 35% lower for U-10Mo with a fission density of 2.6×10^{21} fissions/cm³ relative to unirradiated U-10Mo (Fit of Burkes 2010 Data). This is an important phenomenon to model in the accident analyses, and so has been incorporated to accurately evaluate the fuel temperatures of beginning-of-life (BOL), middle-of-life (MOL), and end-of-life (EOL) fuel plates.

The fission density in the LEU fuel plates varies radially (plate number), axially, and azimuthally. In order to be conservative in the accident analyses, the thermal conductivity corresponding to the peak expected fission density in a given plate will be used in the RIA, LOCA, and LOFA analyses. Table 2.2 summarizes the fuel element burnup, peak fission density in plate 23 (the location of peak heat flux in all LEU elements), and associated thermal conductivity for fuel elements at various burnups that will be modeled in the accident analyses. Also included is the minimum ratio of the irradiated to unirradiated thermal conductivity that occurs over the temperature range of 50 to 300 °C. In the PARET/ANL and RELAP5 models that include irradiated fuel plate channels, the correlation for the unirradiated U-10Mo thermal conductivity derived from the Burkes 2010 data (Equation 2.1 above) will be multiplied by the minimum ratio factor in Table 2.2 to account for degradation of the thermal conductivity due to the fuel burnup.

2.2.2 U-10Mo Heat Capacity

In the Phase I analysis, a correlation provided in Reference 8 of specific heat capacity as a 2nd-order function of temperature was used. An alternate correlation as a linear function of temperature was provided in Reference 10. The correlation in Reference 10 evaluates to slightly lower values of the specific heat capacity compared with the correlation from Reference 8, which will provide more conservative results for the accident analyses. Thus, the volumetric heat capacity derived from Reference 10 and a U-10Mo density of 17.02 g/cm³ for unirradiated U-10Mo (99% of the theoretical density of 17.2 g/cm³ reported in Reference 8) will be used in the current accident analysis, yielding the following correlation:

$$\rho \cdot c_p = 1.923 \times 10^6 + 1199.9 \cdot T \text{ J/m}^3\text{-}^\circ\text{C} \text{ [T in K]} \quad (2.3)$$

Reference 9 states that “the specific heat capacity of the U-10Mo fuel is not significantly dependent on burnup.” Consequently, the correlation for the volumetric heat capacity of U-10Mo in Equation 2.3 will be used for all unirradiated and irradiated fuel plates in the accident analyses.

2.3 6061-Al

The MURR SAR states: “The reactor core assembly support structure consists of the reactor pressure vessel, the island tube, and the reflector tank. The entire structure is approximately 12 feet (3.7 m) in overall length and fabricated of Aluminum-Alloy 6061-T6 with stainless steel hardware.” Therefore, 6061-Al thermal properties will be used for the reactor and tank structures. Likewise, the cladding material for both the HEU and the LEU fuel is 6061-Al.

Table 2.2. Thermal Conductivity of U-10Mo in Unirradiated and Irradiated LEU Fuel Elements.

Element	Element Burnup (MWd)	Peak Fission Density in Plate 23 (fissions/cm ³) ^a	Thermal Conductivity, κ (W/m-C), in range from 50 to 300 °C	Minimum ratio of $\kappa/\kappa_{\text{unirradiated}}$ ^b
Fresh (Unirradiated U-10Mo)	0	0.0	16.37 (50 C) to 24.09 (300C) ^c	1.00
Average Element in Reference Mixed-Burnup Core ^d	85.9	1.25x10 ²¹	12.67 (50 C) to 19.20 (300C) ^e	0.77
Middle of Life (MOL)	77	1.15x10 ²¹	12.82 (50 C) to 19.46 (300C) ^e	0.78
	96	1.40x10 ²¹	12.45 (50 C) to 18.81 (300C) ^e	0.75
End of Life (EOL)	180	2.60x10 ²¹	10.68 (50 C) to 15.68 (300C) ^e	0.65

^a Assume local fission density in LEU elements is linearly proportional to the element burnup. The peak local fission density in plate 23 was calculated to be 2.6x10²¹ fissions/cm³ for a discharged LEU element at 180 MWd in a MURR fuel cycle simulation performed in the steady-state technical basis evaluation [1].

^b Minimum ratio over temperature range 50-300 C.

^c Thermal conductivity for unirradiated U-10Mo evaluated from fit of Burkes 2010 data [8] (Equation 2.1).

^d Average burnup of all elements in reference mixed-burnup core derived in Reference 1.

^e Thermal conductivity for irradiated U-10Mo evaluated from fit of data in Reference 9 (Equation 2.2).

The 6061-Al properties used in the Phase I analysis were taken from the HEU RELAP5 model for the MURR SAR. However, other data for 6061-Al are provided in Reference 11. In the anticipated range for the steady-state and peak accident temperatures of the 6061-Al for the RIA, LOCA, and LOFA, the thermal conductivity and heat capacity reported in Reference 11 are less than the data used in the Phase I analysis. Modest differences in thermal conductivity and heat capacity for the in-pool heat exchanger and the inlet piping should have an insignificant effect on the predicted LOCA and LOFA peak fuel temperatures, although smaller values of these thermal properties for the clad would tend to result in a higher (more conservative) prediction of the peak fuel temperatures. Therefore, in the Phase II analysis, the 6061-Al thermal properties of Reference 11 will be used for the aluminum clad, the aluminum pressure vessel, the island tube, and the piping. The data from Reference 11 for thermal conductivity and volumetric heat capacity were fit with second order correlations in temperature, and are shown in Equations 2.4 and 2.5, respectively.

$$\kappa_{6061-Al} = 147.8 + 0.1792 \cdot T - 2.616E-04 \cdot T^2 \text{ W/m-C [T in } ^\circ\text{C}] \quad (2.4)$$

$$\rho \cdot c_{P,6061-Al} = 4.252E+06 - 2,685 \cdot T + 2.214 \cdot T^2 - 3.774E+08/T \text{ J/m}^3\text{-C [T in K]} \quad (2.5)$$

2.4 Zirconium

A thin layer of zirconium (nominally 1 mil thick) will be used in the LEU fuel plates as an interlayer between the U-10Mo fuel foil and the 6061-Al clad. Correlations of the thermal conductivity and specific heat capacity of zirconium are provided in Reference 12.

$$\kappa_{Zr} = 8.8527 + 0.007082 \cdot T + 2.5329E-6 \cdot T^2 + 2,992/T \text{ W/m-C [T in K]} \quad (2.6)$$

$$c_{P,Zr} = 24.1618 + 8.7558E-3 \cdot T - 6.9942E+4/T^2 \text{ J/mol-C [T in K]} \quad (2.7)$$

The correlation for the specific heat capacity of zirconium in Equation 2.7 is in a form that cannot be entered into the PARET/ANL model (the term that is inversely proportional to the temperature squared is not allowed). Furthermore, PARET/ANL uses the volumetric heat capacity ($\rho \cdot c_{P,Zr}$), which can be calculated from Equation 2.7 using a Zr density of 6.57 g/cm³, and a molar weight of 91.224 g/mol. Consequently, the correlation from Reference 12 was evaluated over the temperature range of 0 to 500 °C and then the volumetric heat capacity was refit with a linear least-squares correlation.

$$\rho \cdot c_{P,Zr} = 1.859E+6 + 739.2 \cdot T \text{ J/m}^3\text{-C [T in } ^\circ\text{C}] \quad (2.8)$$

2.5 Oxide Layer

For the oxide layer, Reference 13 provided the thermal conductivity of the material as $\kappa_{oxide} = 2.25 \text{ W/m-C}$. No data were available for the volumetric heat capacity of the oxide. For the reactivity insertion accidents, most of the energy resulting from the reactivity insertion will stay within the fuel meat in the time-scale of the accident before reactor scram. Therefore, the precise value of the volumetric heat capacity of the oxide will have little effect on the accident results. A very low (conservative) value of 1.0 J/m³-C was selected for the analyses. These values of oxide thermal conductivity and volumetric heat capacity were used in both PARET/ANL RIA, and the RELAP5 LOCA and LOFA analyses.

3.0 INITIAL OPERATING CONDITIONS FOR ACCIDENT ANALYSES

3.1 Background

Reactivity-insertions accidents (RIAs), loss-of-coolant accidents (LOCAs), and loss-of-flow accidents (LOFAs) are addressed in Chapters 4, 5, and 6, respectively. These upsets include the most extreme accidents conceived within the design basis of the reactor. The most severe of these extreme accidents, which have the potential to be most detrimental to the reactor plant, are not expected to occur during the life of the reactor and have not occurred during MURR's 48 years of operation. Postulated accidents include a variety of scenarios up to and including events as extreme as a double-ended guillotine break of the main 12-inch coolant pipe leading to the reactor core. In this postulated accident a short segment of this pipe that is outside of the pool and on the core side of the core inlet isolation valve is assumed to instantaneously disappear, causing a rapid depressurization of the primary system and allowing water/steam to rapidly flow from both sides of the break.

Two important inputs for the accident analyses that are presented in the next three chapters of this report are 1) the initial steady-state conditions of the reactor, i.e., reactor power, primary flow rate, core coolant inlet temperature, etc., and 2) the core state, i.e., burnup of the fuel elements in the core, control rod skew, flux trap loading, etc., assumed when the upset is initiated. For the initial steady state conditions the extreme conditions of the normal operating range that would yield conservative results for the accident consequence were assumed. These are discussed in Section 3.2 below. The choice of Limiting Safety System Settings (LSSS) conditions as the initiating point of a double-ended guillotine break of a main coolant pipe was rejected because it constitutes a double fault, which is beyond the design basis of the reactor. A severe pipe break coinciding with LSSS conditions is beyond the realm of credible accidents and is a condition reserved for the MHA. Similarly, core states that the reactor will rarely, if ever, achieve were also eliminated from consideration. Thus, for example, the empty-flux-trap state is not considered in the RIA, LOCA, or LOFA analyses since the reactor is never operated at full-power in this condition. The selection of the core state(s) assumed for the accident analyses are discussed in Section 3.3.

The selection of the extreme conditions of normal operation rather than more extreme LSSS conditions may seem contrary to the approach taken in the steady-state safety analysis [12]. However, the series of tables and curves provided in Chapter 14 of Reference 2 were designed to provide an envelope in which flow instability is precluded. If the envelope is exceeded, this provides part of the information to guide any investigation that may be needed to determine whether core damage occurred as a result of the accident. In the creation of the safety envelope very extreme assumptions were used so that it could be used with confidence in the assessment of a single-fault condition. There is no reason to assume that a RIA or a severe LOCA will occur while another upset is in progress or while an extremely off-normal reactor state exists.

3.2 Selection of Initial Steady-State Conditions for RIA, LOCA, and LOFA Analyses

Table 3.1 summarizes the initial steady-state conditions used in the analyses of the RIAs, LOCAs, and LOFAs. During operation, the steady-state reactor power is not allowed to exceed

10 MW for the HEU core and will not exceed 12 MW for the LEU core. The measurement uncertainty is included in the power readouts monitored by the reactor safety systems. Thus, the actual steady-state reactor power in MURR is always maintained at or below 10 MW or 12 MW, respectively, for the HEU and LEU cores.

Table 3.1. Initial Steady-State Conditions

Parameter	MURR Operating Band		For RIA Analysis	For LOCA/LOFA Analysis
	Low End	High End		
HEU Reactor Power, MW	-	-	10	10
LEU Reactor Power, MW	-	-	12	12
Primary Inlet Temperature, °F	118	125	125	125
Primary Coolant Flow, gpm	3700	3850	3700	3700
Primary System Pressure, psig	60 ¹	66 ¹	60 ²	63.7 ³
Pressurizer Level, inches	-7	+4	-7	0
Anti-Siphon Tank Pressure, psig	33	39	-	36
Anti-Siphon Tank Temperature, °F	-	-	-	105
Pool Temperature, °F	-	105	-	105

¹ The primary system pressure is measured at the PT-943 pressure gauge, which is near the junction of the two MURR flow loops.

² The pressure measured at the PT-943 gauge was attributed to the pressurizer in selecting the initial conditions for the reactivity insertion accident analyses.

³ Pressurizer pressure.

The “low end” and “high end” values provided in Table 3.1 are consistent with the normal operating range for five of the reactor parameters, as described below.

- The high end of the reactor inlet temperature range and the low end of the primary coolant flow rate are used in both the RIA and LOCA/LOFA analyses because these will produce the highest peak fuel temperatures.
- The lowest pressurizer pressure and level were assumed as the steady-state conditions in the RIAs. A parametric study of the effect of system pressure on the results of the RIA was conducted since a lower pressure can lead, depending on the conditions during an event, to earlier onset of nucleate boiling which lowers temperature through increased heat transfer, or potentially to earlier onset of critical heat flux resulting in higher plate temperatures.
- For the LOCA/LOFA events, a primary system pressure of 78 psia (63.7 psig) was adopted and used due to the insensitivity of the results to small pressure variations as discussed in Section 5.8.8 and Section 6.4. For example, when the most severe LOCA is initiated, the pressure in the reactor core drops below atmospheric pressure in less than a hundredth of a second. Thus, small differences in initial steady-state pressure will have essentially no effect on the peak fuel temperature that is reached during this LOCA.
- The pressurizer level has a very minor effect on the initial pressure since a foot of water adds only 0.43 psi to the pressure. Thus, a pressurizer level of 0 inches is used in the LOCA/LOFA analysis.

- It is not clear whether a low or a high anti-siphon tank pressure is more limiting in the LOCA. The mid-range value of 36 psig, at which the anti-siphon tank pressure is maintained, was selected for the LOCA analysis.
- The pool temperature has virtually no effect on peak fuel temperature. The high end of the operating range, 105 °F, was used in the LOCA/LOFA analysis. The same value was used for the anti-siphon tank temperature since the anti-siphon tank is immersed in the pool.

In the plant, gauge pressures, rather than absolute pressures, are measured. The core centerline is 606 feet and 2 inches above sea level. On the Columbia Campus at Sandborn Field, with an elevation around 710 feet, the average atmospheric pressure was 14.71 psia during the years 2004 through 2013. The lowest daily average atmospheric pressure recorded over this time period was 14.3 psia. Therefore, 14.3 psia is taken as atmospheric pressure in the modeling, rather than the standard atmospheric pressure of about 14.7 psia.

3.3 Core States for RIA, LOCA, and LOFA Analyses

The core state (fuel element burnup, flux trap loading, control blade prior history and positioning) affects the accident consequences. For example, the mixture of burnups for the eight fuel elements will affect the core power distribution. Additionally, the fuel burnup affects the thermo-physical properties of the fuel meat, and the coolant channel dimensions change over the life of the element due to several irradiation-induced phenomena, such as fuel swelling and oxide growth on the plate surface.

In the steady-state safety analyses performed for the HEU and LEU cores [1, 2], a set of 24 reference core states were identified that represent the range of possible conditions that could exist in MURR. These core states include variations in the burnup of the fuel elements (mixed-burnup cores or all fresh fuel loaded in the core), ^{135}Xe loading (no xenon at beginning-of-cycle and equilibrium-xenon conditions), flux trap contents (flux trap loaded with samples typical for MURR or an empty flux trap), and critical control blade configuration (blades that are banked or blades that are highly depleted and mismatched in axial position). However, many of these are core states that the reactor will rarely, if ever, achieve.

The typical core state in MURR has elements with a mixture of burnups, the flux trap is loaded with irradiation samples and experiments, and the control blades are banked in the same axial position. This state was represented in the steady-state safety analyses by the so-called Cores 3A and 7A for the core loaded with HEU and LEU fuel, respectively. These core states also have no ^{135}Xe (beginning-of-cycle conditions), so the control blades must be inserted more deeply than when the core is at equilibrium xenon, which results in a greater peak local heat flux. Consequently, these core states were used as the steady-state condition prior to the RIA, LOCA, and LOFA in the current analyses.

An important quantity in the accident analysis presented in the next three chapters is the peak fuel plate heat flux. Table 3.2 summarizes the peak heat flux in the steady-state HEU and LEU cores that are typical for MURR operations. The “MCNP calculated” values are taken from the results of an MCNP5 [14] model for the typical mixed-burnup core loaded with HEU or LEU

fuel at beginning-of-cycle (BOC) conditions. The peak heat flux by plate, axial level, and azimuthal position was computed from an f7 tally in the MCNP5 calculation. Based on calculations performed in Reference 15, it was assumed that 6.0% and 3.6% of the energy produced in the HEU and LEU cores, respectively, is deposited outside the primary coolant system, thus reducing the heat that must be conducted out of the fuel plate. The heat flux on the surface of the fuel plate was calculated by assuming that the remaining heat that is deposited in the fuel meat conducts out evenly between the two curved surfaces of the fuel plate.

Table 3.2. Peak Steady-State Heat Flux (W/cm²) in HEU and LEU Cores.

Plate with Peak Heat Flux	HEU Core (Core 3A)			LEU Core (Core 7A)		
	MCNP calculated ¹	Phase II RIA ²	Phase II LOCA/LOFA	MCNP calculated ³	Phase II RIA ⁴	Phase II LOCA/LOFA
BOL Fuel Plate	174.2	159.1	170.7	203.2	196.1	198.8
MOL Fuel Plate	144.1	132.8	141.3	184.2	177.9	181.1
EOL Fuel Plate	109.7	101.4	107.3	164.4	158.9	160.9

¹Calculated from MCNP5 f7 tally for typical mixed-burnup core at BOC conditions. Assumes 94.0% of total reactor power of 10 MW is generated in the core. Even power split between concave and convex surfaces of plate.

²Calculated heat flux multiplied by fraction of energy conducted towards the hottest surface of the fuel plate. For the HEU fuel, the hottest surface of fuel plate 1 is the convex side of the plate. The power split fractions to the convex surface are 45.68%, 46.07%, and 46.22% for the BOL, MOL, and EOL plates, respectively.

³Calculated from MCNP5 f7 tally for typical mixed-burnup core at BOC conditions. Assumes 96.4% of total reactor power of 12 MW is generated in the core. Even power split between concave and convex surfaces of plate.

⁴Calculated heat flux multiplied by fraction of energy conducted towards the hottest surface of the fuel plate. For the LEU fuel, the hottest surface of fuel plate 23 is the concave side of the plate. The power split fractions to the concave surface are 48.25%, 48.27%, and 48.33% for the BOL, MOL, and EOL plates, respectively.

The peak heat flux occurs in plate 1 in the HEU core, and in plate 23 in the LEU core. These plates are cooled by water flowing on both sides of the plate, but the coolant channel properties on each side of this plate are different. Specifically, the coolant channel on the concave side of HEU plate 1 is nominally 110 mil thick (the distance from the plate surface to the inner pressure vessel wall) and is heated on only one side (by plate 1), while the coolant channel on convex side of HEU plate 1 is nominally 80 mil thick and is heated on the opposing side by HEU fuel plate 2. Similarly, the coolant channel on the concave side of LEU plate 23 is nominally 93 mil thick and is heated on the opposing side by fuel plate 22, while the coolant channel on the convex side of plate 23 is nominally 95.5 mil thick (the distance from the outer surface of the plate to the inner surface of the outer pressure vessel) and is heated on one side only. Consequently, it is expected that the plate surface temperature will be higher on the convex side of HEU plate 1 (adjacent to the two-sided heated channel between plates 1 and 2), and that a larger fraction of the heat generated in HEU plate 1 will conduct out the concave side of the plate towards the one-sided heated channel. Likewise, the plate surface temperature will be higher on the concave side of LEU plate 23 (adjacent to the two-sided heated channel between plates 22 and 23), and a larger fraction of the heat generated in fuel plate 23 will conduct out the convex side of that plate.

PARET/ANL represents each coolant channel as a radially-symmetric half-channel, and so is not capable of directly representing different coolant channel thicknesses on the two sides of a fuel

plate. So, to avoid unnecessary conservatism in the RIA analysis, the calculated plate power density used in the PARET/ANL input was adjusted to account for the power split between the two channels on either side of the limiting fuel plate. In this way, a more accurate value of the heat flux from the plate into the hot channel is used in the PARET/ANL calculation. A PLTEMP/ANL [16] model for a full-core representation of the MURR at steady-state conditions was used to calculate the fraction of the heat conducted into the hot channel. A beta version of PLTEMP v 4.2 of October 30, 2013 (SVN revision 65) which includes speedup improvements, extended Groeneveld 2006 CHF table, channel-dependent hot channel factors, and an improved double search algorithm was used in the analysis. For the HEU elements, the power split is about 54%/46% for the concave/convex sides of plate 1. The power split for the concave/convex sides of LEU fuel plate 23 is 48%/52%. The disparity of the power split for the two radial surfaces of the plate is less for the LEU plate 23 because the difference in the coolant channel dimensions between the two sides of the limiting LEU plate are not as great as they are for the limiting HEU plate. These fractions were applied to the MCNP5 calculated heat fluxes in Table 3.2, and the result is listed in the columns labeled “Phase II RIA.” Without this correction, the heat flux used in the RIA analysis of the HEU core would be ~8.0% too high, and thus over-predict the fuel temperature in the accident. Correcting the heat flux input for the RIA analysis of the LEU core reduces the peak heat flux in the hot channel by ~3.5% compared with using the heat flux calculated by MCNP5 alone.

Table 3.2 also provides the equivalent heat flux for the current LOCA/LOFA analyses. There was no need to adjust the power distribution data input to RELAP5 [17] to account for the uneven power split between the hot and cold faces of the fuel plate. This is because RELAP5 uses thermal-hydraulic methods that compute the power split during code execution. However, a four-node axial discretization (two 5-inch nodes in the middle of the fuel plate and two 7.75-inch nodes at the top and bottom of the plate) was selected for the LOCA/LOFA analyses to reduce the number of heat structure nodes in the full-core RELAP5 model. The MCNP5 model used a much finer axial discretization with twenty-four 1-inch nodes to provide a better estimate of the peak heat flux. Nonetheless, the peak heat flux in the LOCA/LOFA models is only about 2% lower than the MCNP5 calculated values.

In summary, the selection of initial steady-state core state conditions for RIA, LOCA, and LOFA accident analyses considers extensive and detailed 3-D power distributions, including modeling the most challenging plates and heat fluxes from a typical set of elements represented through the fuel management cycle used in MURR. Final safety analysis to be performed after fabrication and qualification of the fuel for MURR should be updated in the future to consider the range of changes in fuel management or other reactor operations. Appropriate for updated and final analyses would be a treatment of the assumptions and the impacts of control blades through their service lifetime. This was not included in the present analysis since the steady state analysis defined an extreme combination of conditions, including physical skews not expected in the MURR core, as well as effects of blade depletion based on current reactor operations. Potential changes in the MURR control blades prior to conversion, in addition to any fuel management changes, will require additional analysis and so could incorporate additional effects at that time.

3.4 Comparison of Selected Initial Conditions and Selected Core States with Those of the MURR Safety Analysis Report

Table 3.3 provides a comparison of the peak heat flux used in the accident analyses performed in the SAR [6] with what is used in the current Phase II accident analyses for the HEU core. The heat flux values for the SAR were derived from the PARET/ANL and RELAP5 models for the RIA and LOCA/LOFA models used in the SAR, respectively. In the SAR, only fresh fuel plates were modeled for the RIA and LOCA/LOFA analyses. The heat flux values for the Phase II analyses of the RIA and LOCA/LOFA are taken from Table 3.2, and were derived from an MCNP5 calculation for a typical HEU core (Core 3A) loaded with BOL, MOL, and EOL elements at BOC conditions. This represents a best estimate of the current operating conditions at MURR for the analysis of the accidents.

Table 3.3. Peak Steady-State Heat Flux (W/cm²) in HEU Core Accident Analyses.

Plate with Peak Heat Flux	RIA (PARET/ANL)		LOCA/LOFA (RELAP5)	
	SAR ¹	Phase II	SAR ²	Phase II
BOL Fuel Plate	179.8	159.1	137.2	170.7
MOL Fuel Plate	-	132.8	-	141.3
EOL Fuel Plate	-	101.4	-	107.3

¹Computed from PARET/ANL model used in HEU SAR [6].

²Computed from RELAP5 model used in HEU SAR [6].

For the Phase II RIA analysis, the peak heat flux in the hot channel for the fresh BOL fuel plate is 15% lower than that used in the accident modeling in the SAR. One of the goals of the Phase II analysis was to eliminate unnecessary conservatism in the models relative to that applied in the SAR analysis where such enhancements are reasonable and clearly defensible. Two of these enhancements that reduce the heat flux relative to the SAR model for the RIA have already been discussed above: 1) a non-negligible fraction of the fission energy is deposited outside the primary system and does not contribute to the fuel temperature, and 2) the uneven heat conduction out the two curved faces of the fuel plate reduces the heat flux into the hot channel. These two effects account for the lower peak heat flux in the hot channel for the Phase II analysis relative to the SAR. Additionally, while the SAR analysis modeled only the peak fresh fuel plate in the core, the Phase II accident analysis will also model depleted fuel plates to assess the impact of degraded thermo-physical properties with fuel burnup on the fuel meat temperature.

The steady-state coolant flow rate used for the RIA in the SAR was 3,600 gpm, and the coolant inlet temperature in the analysis was 130 °F. While initiating the RIAs from these steady-state conditions will provide conservative results relative to the nominal coolant flow rate of 3,800 gpm and coolant inlet temperature of 120 °F, they are outside the normal range for MURR operations provided in Table 3.1. In the Phase II analysis of the RIAs, the coolant flow rate at the lower end of the normal operating range and the coolant inlet temperature at the upper end of the operating range were selected so as to be reasonably conservative.

Table 3.3 shows that the peak heat flux in the current LOCA/LOFA model is significantly higher than that used in the accident analysis performed as part of the SAR. As indicated in Table 13-1 of the SAR, the initial steady-state conditions for the HEU core in the LOCA analysis were taken to be a reactor power of 11 MW, as well as a coolant inlet temperature of 155° F and core inlet flow rate of 3800 gpm. Thus, at first glance one might expect that the 11.0 MW reactor power used in the SAR would produce heat fluxes that are 10% *greater* than those used in the current HEU LOCA analysis (the reactor power level of 11 MW used in the SAR model is 10% greater than actual power of 10 MW). However, at the limiting location, the peak heat flux in the SAR analysis (137.2 W/cm^2) is 20% *lower* than the peak heat flux in the same location in the current analysis (170.7 W/cm^2).

Table C-2 of Reference 6 provides the initial steady-state power distribution used in the SAR LOCA analysis. The sum of the power fractions in Table C-2 is exactly 1. The analysis is similar to that provided in Chapter 5 of this report in that a single fresh fuel element is scaled to represent all eight fuel elements in the core. The 25.5-inch total length of each fuel plate is divided into 4 segments. The top and bottom segments of the fuel plate in the RELAP5 model are each 7.75-inches long, while the two middle segments of the plate are 5.00 inches long. All 24 fuel plates of a single HEU element are explicitly represented in the model. Thus, in the SAR and current RELAP5 models all of the power in the HEU core is distributed among the $24 \times 4 = 96$ model fuel segments.

In the RELAP5 LOCA analysis of the SAR, 93.0% of the steady-state reactor power of 11 MW, or 10.23 MW, was deposited in the core. The remaining 7.0% of the reactor power was assumed to be deposited outside of the core and did not contribute to the fuel temperature. A calculation of the heat fluxes produced by each of the 96 fueled segments in the analytical model found that the peak heat flux in the SAR RELAP5 model was 137.2 W/cm^2 and that it occurred in the third segment from the top of plate 1. This value was obtained by taking the total power generated by the fuel plate segment and dividing it by the total heat transfer area on both sides of the segment. It was noted that the curved fuel plates were approximated as flat plates in the SAR model. This calculation also assumed that the heat deposited in the segment conducted evenly out both curved surfaces of the segment, which is the same assumption as made in calculating the heat flux for the LOCA/LOFA Phase II heat flux in Table 3.2 and Table 3.3. The RELAP5 calculation performed in the SAR predicted that the initial steady-state heat flux is 146.6 W/cm^2 on the concave side and 127.8 W/cm^2 on the convex side of the segment with the peak heat flux. This asymmetric distribution of heat flux is largely due to plate 1 being an end plate for which the coolant of the concave side is much cooler than that on the convex side. It should be noted that the 7.0% fraction of the reactor power deposited outside the core, the fuel segment heat transfer area, and the heat fluxes from the concave and convex sides of the fuel segment were taken from an available SAR RELAP5 output file, "HD325newloca030p.o", rather than from the SAR itself. The 96 values of power distribution provided by Table C-2 of Reference 6 can be found in this RELAP5 output file.

Although not stated in the SAR, the source of the power distribution used in the LOCA/LOFA analysis was experimental. The first core containing the current HEU aluminide fuel elements was prematurely removed after reaching a core burnup of 640 MW days. One of the discharged elements was destructively analyzed through post-irradiation examination [18]. Longitudinal

gamma scans and burnup tests were performed and subsequently used to prepare the power profile data in Table C-2 of the SAR. It is expected that since the peak plate heat flux decreases by nearly 40% with fuel burnup (compare the EOL and BOL plate heat fluxes for the Phase II analysis in Table 3.3), the heat flux derived in this fashion underestimates the heat flux for the fresh fuel plate.

In the current HEU LOCA analysis, power fractions analogous to those in the SAR were determined for all eight fuel elements of Core 3A. These fractions do not account for variations in power along the arc length of each fuel plate. This effect is taken into account via azimuthal peaking factors derived from the MCNP5 analysis. In the neutronics calculations the fuel meat of each fuel plate is sectioned into nine vertical stripes. The average heat flux of each stripe is divided by the average heat flux taken over all nine stripes. The largest of these nine ratios is, by definition, the azimuthal peaking factor for the plate. For each fuel plate an additional plate-specific factor, which is greater than 1.0, is used to account for the azimuthal peaking.

In the current RELAP5 analysis of element 5 of HEU Core 3A, the reactor power used in the RELAP5 model was 12.464 MW, which is considerably more than 10.0 MW. Only 6% of the nominal 10.0 MW, or 0.60 MW, is deposited outside of the core. The reason for the considerable increase in core power in the model is two-fold. First, element 5 is one of two beginning-of-life elements in the core. Each of these generates more power than do each of the other six burned elements. Second, the azimuthal peaking factors are included. For plate 1 of element 5, for example, this factor is 1.062. Therefore, the power fractions for all four nodes of plate 1 were increased by a factor of 1.062 in the RELAP5 model. The goal here was to accurately represent the peak heat flux of each fuel plate segment of element 5 in the RELAP5 model even though this causes the reactor power in the RELAP5 model to be megawatts too large. This is to be compared to the SAR model, which in effect used an average element in the HEU core in a reactor that produced 11.0 MW and did not include azimuthal peaking factors.

The current heat transfer model uses curved fuel plates for which the heat transfer area of the concave side of each fuel plate is smaller than that of the convex side. The initial steady-state heat flux on the concave side of node 3 of plate 1 was 186.2 W/cm^2 and was 155.2 W/cm^2 on the convex side, as obtained for the RELAP5 output. Thus, at the limiting location, the peak heat flux in the current model is 27% greater than that obtained in the SAR analysis, i.e. 186.2 vs. 146.6 W/cm^2 .

The current HEU LOCA analysis uses a core initial steady-state flow rate of 3,700 gpm, which is 100 gpm less than the 3800 gpm of the SAR LOCA analysis. Both of these flow rates are considerably greater than the LSSS value for the HEU core of 3,200 gpm. The initial steady-state core inlet temperature of 155° F of the SAR LOCA analysis is the LSSS value and is 30° F above the 125° F value used in the current HEU LOCA analysis. As explained above, to use the LSSS core inlet temperature in the current analysis would be to assume conditions that are beyond the design basis.

4.0 REACTIVITY INSERTION ACCIDENTS

4.1 Introduction

Insertion of uncontrolled and/or unanticipated positive reactivity to either a critical reactor or during a reactor startup is one of the nine (9) postulated accident-initiating scenarios required to be analyzed for potential radiological consequence per the NUREG-1537 guidelines [3]. Following the guidance provided in NUREG-1537, two different accident scenarios for an insertion of positive reactivity were evaluated for the selected LEU core (CD35) and the results are compared against those for the current HEU core. First, several positive step reactivity insertions, based upon the various maximum reactivity limits given in the MURR Technical Specifications, were considered. Second, a continuous ramp insertion of positive reactivity based on the simultaneous continuous withdrawal of MURR's four shim control blades was considered. The exact mechanisms or events that could cause these reactivity insertions can vary, but could include an inadvertent rapid insertion or removal of an experiment from the center test hole, reactor or equipment malfunction, or operator error.

All the analyses in this chapter were performed using the PARET/ANL code [19]. PARET/ANL is a 1-dimensional reactor transient analysis code that models plate, pin, and nested tube fuel geometries. The collection of fuel meat, clad, and associated coolant is called a "channel" in PARET/ANL. An average channel is typically used to represent the core and bulk reactivity feedback effects from the transient, while one or more "peak" channels are used to represent those components (e.g., a fuel plate) which are expected to reach the highest temperature as a result of the transient. There is no heat transfer between the channels in PARET/ANL, but the channels are coupled through reactivity feedback effects; that is, the reactivity feedbacks from each channel in the model are volume-weighted and summed to compute the total core reactivity and power as a function of time in the transient.

The user provides a description of the fuel geometry, steady-state thermal-hydraulic conditions, thermo-physical properties, and the transient initiating condition (e.g., insertion of reactivity) as a time-dependent table. The thermal conductivity and volumetric heat capacity of the plate materials can be supplied as input to PARET/ANL as temperature dependent functions. The coolant properties are generated by the code using built-in functions. A point-kinetics solution is used to calculate the overall core reactivity, total core power, and local heat generation rates as a function of time. The thermal-hydraulics models in PARET/ANL are used to compute the heat conduction, material temperatures (e.g., fuel meat, clad, coolant), and inherent reactivity feedbacks. Lastly, termination of the reactor transient from engineered safety systems (i.e., control blade scram) that are tripped from core power, coolant flow, or reactor period settings can be modeled.

4.2 Modeling of Reactivity Insertion Accidents

4.2.1 Previous Modeling of Reactivity Insertion Accidents

For the current HEU-fueled core, the accidental positive step reactivity addition accident scenarios were analyzed in the MURR SAR [6] from an initial steady-state power level of 10

MW, and with coolant flow, pressure, and core inlet temperature conditions as specified in Table 4.1. Primary coolant temperature and void feedback coefficients corresponding to the MURR Tech Spec limits of $-6.0 \times 10^{-5} \Delta k/k/^{\circ}\text{F}$ and $-2.0 \times 10^{-3} \Delta k/k/\%$ void, respectively, were employed. The continuous ramp insertion of positive reactivity accident scenario was also analyzed from low power (1 W) and from a nominal steady-state power of 10 MW. In the MURR SAR, calculations were performed with version 5.0 of the PARET/ANL code.

Table 4.1. Key Reactor Parameters for Reactivity Insertion Accidents in the MURR SAR.

Parameter	Nominal Value	Assumed Value in SAR Analysis
Reactor Power	10 MW	10 MW
Primary Coolant Flow Rate	3,800 gpm	3,600 gpm
Reactor Core Inlet Pressure	84 psia	75 psia ¹
Reactor Core Inlet Temperature	120 °F	130 °F

¹ The LSSS pressurizer pressure at MURR is 75 psia. For the Phase I analysis, a core inlet pressure of 63.7 psia was utilized, which was computed by MURR staff taking into account pressure losses from the pressurizer to the core inlet.

As part of the Phase I analysis, a review of the PARET/ANL models used in the MURR SAR analyses was conducted. The review found some modeling inconsistencies, which were corrected prior to performing the Phase I analysis [4]. Furthermore, instead of using the MURR Tech Spec values for the reactivity feedback coefficients, the reactivity feedback coefficients and reactor kinetics parameters (effective delayed neutron fraction [β_{eff}], and neutron lifetime [Λ]) were calculated for the MURR core loaded with HEU or LEU fuel using an MCNP5 core model. The coolant void, coolant temperature, and fuel temperature reactivity coefficients were calculated by making small perturbations to the appropriate reactor conditions from the nominal (hot operating) conditions in MCNP5. The kinetics parameters were calculated using the kinetics option in MCNP5. The validation of this methodology for calculating the feedback coefficients is described in more detail Section 4.5 of Reference 1.

The reactivity coefficients and kinetics parameters were calculated for three different core loadings of each fuel type (HEU or LEU): 1) all fresh fuel, 2) a typical mixed-burnup core loading at beginning-of-cycle (BOC) with no xenon, and 3) a typical mixed-burnup core loading at equilibrium-xenon conditions. For each fuel type, the smallest magnitude value calculated for each parameter was chosen for the current work, which is the conservative approach. Table 4.2 summarizes the calculated reactivity feedback coefficients that are used in the analysis.

In addition to inherent reactivity feedbacks from fuel and coolant temperature changes that occur during the transient, engineered safety systems are also available to terminate any transient initiated by an unanticipated positive insertion of reactivity. External reactivity control of the MURR core is accomplished with four Boral© shim blades that move in a water-filled gap between the reactor outer pressure vessel and the beryllium reflector. When any one of the various reactor trip settings is reached, a scram signal is sent and the blades are dropped to insert

**Table 4.2. Reactivity Feedback Coefficients and Kinetics Parameters for MURR
Calculated with MCNP5.**

Parameter	HEU	LEU
Coolant Density Coefficient, \$/%-void	-0.301 ± 0.011	-0.304 ± 0.011
Coolant Temperature Coefficient, ^a \$/°C	$-5.25 \times 10^{-3} \pm 9.4 \times 10^{-5}$	$-1.63 \times 10^{-3} \pm 9.5 \times 10^{-5}$
Fuel Temperature Coefficient \$/°C	N/A	$-0.911 \times 10^{-3} \pm 5.2 \times 10^{-5}$
β_{eff}	$0.783\% \pm 0.003\%$	$0.765\% \pm 0.002\%$
Λ (μs)	49.5 ± 0.10	38.7 ± 0.03

^a This is the reactivity feedback for temperature changes of the coolant only. The effect of coolant density changes on reactivity due to temperature increase or decrease is accounted for in the coolant density coefficient.

a sufficiently large amount of negative reactivity and terminate the transient. The blades can travel from fully-withdrawn to fully-inserted in 0.87 seconds. There is a short delay time of 150 milliseconds from the time the scram signal is sent to the start of blade motion.

The reactivity worth of the entire control bank was calculated with MCNP5 for HEU-fueled and LEU-fueled cores under different core burnup conditions. The calculated control bank worth curves for fresh control blades are provided in Figure 4.1. The control bank worth for the LEU-fueled cores is about \$4 less than for the HEU-fueled cores, but the total bank worth is still sufficiently large to compensate for the credible reactivity insertion accidents postulated for MURR. The reactivity worth from the critical blade position as a function of time from the start of the blade drop (assuming a blade drop rate of 0.76 m/s) was calculated and entered in the PARET/ANL models.

The control blade withdrawal rate in MURR is 1 inch/minute. Using the control bank worth curves in Figure 4.1 and the blade withdrawal rate, the calculated maximum reactivity insertion from control bank withdrawal is 0.00027 $\Delta k/k/s$ for HEU-fueled cores and 0.00023 $\Delta k/k/s$ for LEU-fueled cores. These values meet the MURR Tech Spec requirement that the total reactivity insertion rate from blade withdrawal is less than 0.00030 $\Delta k/k/s$.

Although the MURR core is typically loaded with a mixture of elements at different burnup stages, only plates at fresh conditions were evaluated in the Phase I analysis. The axial power density profile corresponding to the fuel plate that has the peak heat flux in a core loaded with all fresh elements was utilized to represent the conditions of the hot channel in the RIA analysis. The power distribution data were derived from MCNP5 results generated during the steady-state safety analysis [1] for the so-called cores 1B1 and 5B1, respectively, for HEU and LEU fuel.

The Phase I analysis was performed with PARET/ANL v. 7.5 [19]. In this version of the code, the fuel plate dimensions for all channels in the model must be the same. A comparison of the plate dimensions in the HEU and CD35 elements is provided in Table 4.3. While all the plates in

Figure 4.1. MURR Control Bank Worth Curves.

the HEU fuel element have the same meat and clad thickness (20 mil meat and 15 mil clad, for a total plate thickness of 50 mil), the LEU fuel plates are not all identical. The CD35 meat and clad thicknesses were optimized to flatten the steady-state heat flux profile [1]. For the LEU accident analysis, the plate dimensions for plate 23 in the CD35 fuel element were used to represent both the average plate in the core and the plate with the peak heat flux. Plate 23 was selected because it has the peak local heat flux in all cores considered in the steady-state safety analyses, regardless of fuel element burnup, blade position/skewing, and flux trap contents in the core. Since the same dimensions had to be used for both the peak and average plates in the PARET/ANL model, it was considered more important to accurately represent the dimensions of the peak plate because of the effect of the meat thickness on heat conduction and the peak fuel centerline temperature. Lastly, the modeling of the plate clad in Phase I was simplified to include only the 6061-Al clad. The 1 mil-thick zirconium interlayer that will be part of the LEU fuel plates was not modeled in Phase I. Rather, the total nominal design clad thickness of 6061-Al plus Zr was modeled as 6061-Al only.

Table 4.3 also shows that all internal coolant channels (the gap between fuel plates) in the HEU element are nominally 80 mil thick. There is a wider coolant channel that is nominally 110 mil thick on the concave side of HEU plate 1 (distance from the plate surface to the inner pressure vessel wall), and a channel that is nominally 90 mil thick on the convex side of HEU plate 24 (distance from the plate surface to the outer pressure vessel wall). The internal coolant channel thicknesses in the LEU element were varied as part of the fuel design optimization. These channels are nominally either 93 or 92 mil, with an average interior coolant channel thickness for the LEU element of 92.4 mil. The coolant channels on the concave side of LEU plate 1 and the convex side of LEU plate 23 are both nominally 95.5 mil.

Table 4.3. Nominal HEU and LEU Fuel Plate Dimensions.

Element Component	HEU	LEU (CD35)
Fuel plates per element	24	23
Fuel meat length (inches)	24.0	24.0
Fuel meat width, varies by plate (inches)	1.703 to 4.052	1.690 to 4.048
Unfueled width of plate in coolant channel (mil)	70 [#]	70 [#]
Plate 1 meat thickness (mil)	20	9
Plate 2 meat thickness (mil)	20	12
Plate 3 meat thickness (mil)	20	16
Plate 4-22 meat thickness (mil)	20	20
Plate 23 meat thickness (mil)	20	17
Plate 24 meat thickness (mil)	20	N/A
Plate 1 clad thickness (mil) – Al + Zr*	15	17.5
Plate 2 clad thickness (mil) – Al + Zr*	15	16
Plate 3 clad thickness (mil) – Al + Zr*	15	14
Plate 4-22 clad thickness (mil) – Al + Zr*	15	12
Plate 23 clad thickness (mil) – Al + Zr*	15	16
Plate 24 clad thickness (mil) – Al + Zr*	15	N/A
Average plate meat thickness (mil)	20	18.87
Average plate clad thickness (mil) - Al + Zr*	15	12.67
Coolant channel 1 thickness – plate 1 to inner pressure vessel wall (mil)	110	95.5
Interior coolant channel thickness (mil) – with exception of channels 6-19	80	93
Interior coolant channel 6-19 thickness (mil)	80	92
Average interior coolant channel thickness (mil)	80	92.4
Coolant channel 25 (HEU)/24 (LEU) thickness – plate 24 (HEU)/23(LEU) to outer pressure vessel wall (mil)	90	95.5

[#] Total unfueled width on each end of plate is 145 mil. 75 mil of plate extends into side plate.

*Zr interlayer exists only in LEU. The nominal Zr interlayer thickness is 1 mil.

The plate with the peak heat flux in the HEU cores is plate 1, while the plate with the peak heat flux in the LEU cores is plate 23. Both of these plates are cooled on one side by a channel that is wider than the other side, and the wider channel is heated on only one side, as it is bounded by either the inner pressure vessel (HEU) or the outer pressure vessel (LEU). It is expected that a majority of the heat deposited in the fuel plate will conduct out the cooler side of the plate.

However, PARET/ANL does not allow modeling of different coolant channel thicknesses on the two sides of a fuel plate. In the Phase I analysis it was assumed that the heat generated in the fuel plates conducts evenly out both curved surfaces of the plate. This is a conservative assumption, as a smaller fraction of the heat generated in the plate will conduct towards the surface of the plate adjacent to the hot channel. This was previously discussed in Section 3.3.

Various transients initiated by step and ramp insertions of positive reactivity were analyzed in Phase I. The step insertion of 0.6% $\Delta k/k$ (Tech Spec limit of all unsecured experiments) yielded

the highest prediction of the fuel temperature for all the accidents evaluated in the Phase I analyses.

For the step insertion of 0.6% $\Delta k/k$ in the all-fresh HEU Core 1B1 (\$0.77 inserted), the peak power reached during the accident was predicted to be 33.75 MW, and the highest fuel temperature reached in the accident was calculated to be 223.3 °C, at 0.067 seconds after the reactivity insertion. The accident analysis assumed the minimum reactor period trip setting of 8 s (which is reached almost simultaneously with the reactivity insertion) fails to initiate a reactor scram, but that the overpower trip setting of 12.5 MW does function (this is reached in less than a few milliseconds). In the beyond design-basis event that the control blades fail to scram at both the minimum reactor period trip and the overpower trip settings, the fuel temperature begins to slowly rise after the initial power burst and fuel temperature spike are turned around from inherent reactivity feedback effects. After 1.5 seconds in this unprotected mode, the peak HEU fuel temperature is 229.4 °C, 6 degrees above the peak temperature reached in the initial power burst.

The step insertion of 0.6% $\Delta k/k$ in the HEU reference Core 3A was also evaluated in the Phase I analysis. This reference core is loaded with fresh and depleted elements, which is typical of MURR operations, although only the temperature in the fresh fuel plates was calculated in the PARET/ANL model. The peak fuel temperature reached in the fresh fuel plates before reactor scram was predicted to be 216.9 °C. If the control blades fail to scram at both the minimum reactor period trip and the overpower trip settings, the fuel temperature begins to slowly rise after the initial power burst, and reaches a peak temperature of 224.3 °C at 1.5 seconds after the reactivity insertion.

For the step insertion of 0.6% $\Delta k/k$ in the all-fresh LEU Core 5B1 (\$0.78 inserted), the highest fuel temperature reached in the accident was calculated to be 259.5 °C, at 0.052 seconds after the reactivity insertion. The peak power reached in the accident was predicted to be 42.21 MW before the power burst was curtailed by the inherent reactivity feedback effects. As in the HEU accident analysis, failure of the minimum reactor period trip setting in the engineered safety system to scram the reactor is assumed, while the overpower trip setting of 15.0 MW (125% of the nominal reactor power of 12 MW) does initiate the reactor scram. If the accident remains unprotected for a full 1.5 seconds (i.e., in the beyond-design-basis event that the control blades fail scram at both the overpower trip setting of 15.0 MW and the reactor period trip setting of 8 s), the peak U-10Mo fuel temperature is calculated to reach 260.1 °C, just slightly above the peak fuel temperature reached in the initial power burst.

The step insertion of 0.6% $\Delta k/k$ in the LEU reference Core 7A was also evaluated in the Phase I analysis. Only the temperature in the fresh fuel plates was calculated for this reference core that is loaded with fresh and depleted elements. The peak fuel temperature reached in the fresh fuel plates before reactor scram was predicted to be 248.9 °C. If the control blades fail to scram at both the minimum reactor period trip and the overpower trip settings, the fuel temperature begins to slowly rise after the initial power burst, and reaches a peak temperature of 252.3 °C at 1.5 seconds after the reactivity insertion.

The previous analyses of the accident consequences from a positive insertion of reactivity predicted that the HEU and LEU fuel temperatures remained below the lowest measured blister temperatures of 480 C for U-Al_x fuel [20] and preliminary measurements with the proposed monolithic U-10Mo fuel type [21].

4.2.2 Phase II Modeling of Reactivity Insertion Accidents

In the Phase II analysis of the reactivity insertion accidents, the PARET/ANL models were enhanced to include modeling of depleted as well as fresh fuel plates. The effects of oxide layer formation on the fuel plates, and coolant channel restriction due to the oxide growth on the plate surface and irradiation-induced fuel swelling and creep were also included in the model. The thermo-physical properties provided in Chapter 2 were utilized. The inherent reactivity feedback coefficients and kinetics parameters previously calculated and summarized in Table 4.2 were used. The control bank worths shown in Figure 4.1 were also used for modeling the termination of transients from the engineered safety system when the trip settings were reached.

The Phase II analysis was completed with a modified version of the PARET/ANL v. 7.5 [19], using the controlled source code file `paret76lnx_beta_130417`. Unlike v. 7.5 which was used in the Phase I analysis, this newer version of the code does not require that all fuel plates in the channels be modeled with the same dimensions. Thus, each of the channels can be modeled with plate dimensions, oxide layer thickness, coolant channel thickness, and thermo-physical parameters that are appropriate for the geometry and burnup conditions of the plate. A drawing of a PARET/ANL channel showing the fuel plate and coolant is provided in Figure 4.2. The materials represented in the Phase II model are the fuel meat, clad (zirconium interlayer and 6061-Al), any oxide layer which forms during operation, and water coolant.

Figure 4.2. PARET/ANL Fuel Plate/Coolant Channel.

In the current analysis, up to four fuel plate/coolant channels will be modeled to represent the MURR core plus the fuel plates most likely to have peak fuel temperatures. The channels to be modeled will be 1) an average channel representing the core, 2) a beginning-of-life (BOL, unirradiated) fuel plate with the peak heat flux of all fresh fuel plates in the core, 3) an end-of-life (EOL) plate with the peak heat flux of all EOL plates in the core, 4) and a middle-of-life (MOL) plate with the peak heat flux of all MOL plates in the core. Appropriate degradation of the U-10Mo thermal conductivity with fuel burnup, the formation of an oxide layer on irradiated

fuel plates, and reduction of the coolant channel thickness due to oxide growth, fuel swelling, and fuel creep will be modeled in the MOL, EOL, and average fuel channels.

The fuel plate/channel dimensions for the average and peak plates in the HEU and LEU cores are summarized in Table 4.4. The dimensions for the average channel in the core were calculated using the average values for the nominal design data for all fuel plates in the element (e.g., fuel meat width, plate width, fuel meat thickness, clad thickness, and interior coolant channel thickness). The oxide layer thickness on the convex surface of HEU plate 1 or the concave surface of LEU plate 23 is estimated from a version of the Griess correlation [13] which was modified to take into account the specific operating conditions of the MURR [22]. These plate surfaces were chosen for predicting the oxide layer thicknesses because they will have the highest temperature, and so, a thicker oxidation layer than the surface on the other side of the plate.

Furthermore, during irradiation the coolant channel will become constricted relative to the fresh element coolant channel due to oxide growth, fuel swelling, and irradiation-induced fuel creep. For HEU fuel elements, the current acceptance criteria for the elements during irradiation limit this constriction to a maximum of 10 mil. For the LEU fuel, it was determined in Reference 22 that the total coolant channel restriction for channels bounded by two fuel plates will be no more than 8 mil due to these effects. The channel restriction is assumed to increase linearly, reaching 10 mil at the expected HEU element discharge burnup of 150 MWd and 8 mil at the expected LEU element discharge burnup of 180 MWd.

PARET/ANL is a 1-D code, and uses a user-supplied axial power profile for each channel to compute the thermal-hydraulic conditions and temperatures. It is assumed that while the reactivity insertion will affect the total core power, the power shape relative to the steady-state power distribution will not be affected by the transient. Detailed, 3-dimensional power distribution data in MURR cores loaded with HEU or LEU fuel were calculated using MCNP5 as part of the steady-state safety analyses for a number of reference cores [1]. As discussed in Section 3.3, reference cores that represent the typical operating conditions at MURR were selected for the accident analyses. These so-called Cores 3A (HEU) and 7A (LEU) are loaded with eight elements with a mixture of burnup, the flux trap is loaded with experiments typical for MURR operations, and the four control blades are banked at the same axial position. Tally results from the MCNP model for these reference cores were used to calculate the plate heat flux in radial (plate), axial, and azimuthal directions in all eight elements in the MURR core. For the peak BOL, MOL, and EOL channels in the PARET/ANL models, the 1-D axial heat source description was calculated for the plate and azimuthal strip in the core that generates the highest local heat flux. For the average channel in the PARET/ANL models, the axial heat source description can be calculated from averaging the power density of all plates in the core.

Table 3.2 above provides the peak heat fluxes into the hot BOL, MOL, and EOL channels for the RIA analysis for the reference HEU and LEU cores. Although the peak heat fluxes from the MOL and EOL LEU plates are roughly 9% and 19% less, respectively, than the peak heat flux from the BOL LEU plate in the reference core, the significant degradation of the U-10Mo thermal conductivity that occurs with irradiation (up to 35% reduction of the thermal

Table 4.4. Fuel Plate/Channel Dimensions.

Dimension	Average Plate/Channel		Peak Plate/Channel	
	HEU	LEU	HEU Plate 1	LEU Plate 23
Meat width (in)	2.877	2.868	1.703 ¹	4.048 ¹
Plate width (in)	3.017 ²	3.008 ²	1.843 ²	4.188 ²
Meat thickness (mil)	20	18.87	20	17
Zr interlayer thickness (mil)	N/A	1	N/A	1
6061-Al thickness (mil)	15	11.67	15	15
Plate thickness (mil)	50	44.22	50	49
Oxide layer thickness formed on surface of fuel plate (mil) ^{3,4}	0.0 (@ 0 MWd) 0.36 (@ 72.0 MWd ⁴)	0.0 (@ 0 MWd) 0.59 (@ 85.9 MWd ⁵)	<u>BOL</u> : 0.0 <u>MOL</u> : 0.34 (@ 65 MWd) <u>EOL</u> : 0.54 (@ 150 MWd)	<u>BOL</u> : 0.0 <u>MOL</u> : 0.63 (@ 96 MWd) <u>EOL</u> : 0.93 (@ 180 MWd)
Coolant channel thickness, C (mil) ⁶	C _o = 80.0 ⁷ C (72.0 MWd) = 75.2	C _o = 92.4 ⁷ C (85.9 MWd) = 88.5	<u>BOL</u> : C _o = 80.0 ⁸ <u>MOL</u> : C (65 MWd) = 75.7 <u>EOL</u> : C (150 MWd) = 70.0	<u>BOL</u> : C _o = 93.0 ⁹ <u>MOL</u> : C (96 MWd) = 88.7 <u>EOL</u> : C (180 MWd) = 85.0

¹ Peak heat flux data were calculated in a 5 mm stripe along the vertical edge of the fuel meat adjacent to the unfueled portion of the fuel plate (next to fuel element side plate).

² Includes 70 mil unfueled portion of fuel plate along both vertical edges of the plate that extends to the element side plate.

³ Oxide thickness estimated as a function of core residence time from Griess correlation modified based on measured data for HEU fuel plates in MURR [22].

⁴ Convex surface of HEU plate 1 or concave surface of LEU plate 23. These are the surfaces of the plates that will have the highest temperatures, and so, the thicker oxidation layer.

⁵ Average burnup of all elements in reference mixed-burnup core.

⁶ Coolant channel thickness assumed to reduce linearly with burnup due to combined effects of fuel swelling and oxide layer formation. Total combined effect is 10 mil at 150 MWd for HEU element, so that $C = C_o - 10 \cdot B / 150$, where C and C_o are the channel thickness in mil, C_o is the nominal channel thickness for an unirradiated element, and B is the element burnup in MWd. For the LEU element, the total combined effect is 8 mil at 180 MWd.

⁷ Average of all interior coolant channels in unirradiated element.

⁸ Nominal thickness of coolant channel on convex side of HEU plate 1 in unirradiated element.

⁹ Nominal thickness of coolant channel on concave side of LEU plate 23 in unirradiated element.

conductivity; see Section 2.2.1) and the growth of the oxide layer on the irradiated plate surface will possibly result in higher fuel temperatures for these irradiated plates. It is less likely that the irradiated HEU plates will have higher temperatures than the fresh HEU plates because the heat flux reduction in the EOL plate relative to the BOL plate is much greater (36%) than the degradation of the $U-Al_x$ thermal conductivity from BOL to EOL (25%).

The following reactivity insertion accidents will be evaluated in the LEU reference core:

- (1) *Step insertion of reactivity accident.*
 - 0.60% $\Delta k/k$ step insertion of reactivity in a core operating at full power. This is the Tech Spec limit for a) total reactivity worth of *all unsecured experiments* in the reactor, b) reactivity worth of *any secured removable experiment*, and c) total reactivity worth of *all experiments in the center test hole*.
 - 0.25% $\Delta k/k$ step insertion of reactivity in a core operating at full power. This is the Tech Spec limit for the reactivity worth of any *unsecured experiment*.
 - 0.10% $\Delta k/k$ step insertion of reactivity in a core operating at full power. This is the Tech Spec limit for the reactivity worth of any *moveable experiment*.
- (2) *Blade withdrawal accident.* 0.03% $\Delta k/k$ /second ramp insertion of reactivity from full power. This is the Tech Spec limit for the reactivity insertion rate from control blade withdrawal.
- (3) *Startup accident.* 0.03% $\Delta k/k$ /second ramp insertion of reactivity from low power (1.0 W). This will simulate a blade withdrawal accident at startup without operator intervention.

For the HEU core, only the 0.6% $\Delta k/k$ step reactivity insertion accident will be evaluated in the Phase II analyses. It has already been noted that this accident results in the most severe consequences (highest fuel temperature) of all the accidents to be analyzed. Furthermore, the full set of step and ramp insertion accidents identified above were already evaluated in the Phase I analysis, and it was observed that even with the modeling of depleted plates in the reference core that are included in the current analysis, the Phase II results are less severe than those already obtained.

Table 4.5 provides a summary of the steady-state conditions that will be assumed for a “base case” for each reactivity insertion accident. The base case assumes the initial steady-state conditions summarized in Table 4.6. These conditions are at the limits of the normal operating band for MURR that would provide conservative results for the accident. Table 4.5 also summarizes parameter variations that will be modeled in a series of branch cases to cover potential non-conservative variations of thermo-physical properties or core operating conditions that may affect the severity of the accidents. No hot channel factors will be applied in the reactivity insertion accident analyses.

Table 4.5. Conditions for Base and Branch Cases for Reactivity Insertion Accidents.

<p>Phase II Base Case</p>	<p>Phase II analyses of the step-insertion (0.6%, 0.25%, and 0.10% $\Delta k/k$) and blade withdrawal (0.03% $\Delta k/k/s$) accidents will be initiated from steady-state at the conditions given in Table 4.6.</p> <p>The startup accident will be initiated from these same conditions, except the initial core power will be 1.0 Watt with the blades fully inserted (-4.2% $\Delta k/k$, or -5.49).</p> <p><i>Rationale: The initial steady-state conditions were selected based on the normal operating range for MURR to obtain conservative results. A more complete discussion is provided in Section 3.2.</i></p>
<p>BRANCH CASES FOR REACTIVITY INSERTION ACCIDENTS</p>	
<p>Branch 01</p>	<p>Decrease the U-10 Mo thermal conductivity by 20% in all LEU fuel plates. For the LEU fuel, this will yield a thermal conductivity of the EOL plate 23 that is roughly half of that for the unirradiated U-10Mo.</p> <p><i>Rationale: Thermal conductivity of U-10 Mo degrades with burnup. The measurement data by Burkes et al. in Reference 9 indicate that at the peak burnup expected in the MURR fuel plates ($\sim 3 \times 10^{21}$ fissions/cm³), the lower-bound of the experimental data for the thermal conductivity is 50% of the fresh U-10Mo thermal conductivity.</i></p> <p><i>This branch case will not be run for the HEU fuel as the lower bound data for the U-Al_x thermal conductivity were selected from the available data (see Section 2.1.1).</i></p>
<p>Branch 02</p>	<p>Increase the oxide layer thickness on the MOL and EOL fuel plates.</p> <p><i>Rationale: The oxide layer thickness can be estimated using a correlation that depends on the hours the fuel plates are resident in the core and the fuel plate surface temperature. The correlation developed by Griess et al. in Reference 13 is for fuel plates in ATR. This was modified based on measurements at MURR for HEU fuel plates [22], which reflects the effect of the water chemistry (pH) of the MURR primary coolant and the MURR HEU fuel plate surface temperature. This “modified Griess correlation” was evaluated over the life of the fuel plate at the peak plate surface temperature for the core at equilibrium-xenon and nominal operating conditions. If the correlation is evaluated using the peak plate surface temperature at the steady-state conditions assumed for the RIA, the oxide thickness is estimated to be up to 18% thicker for HEU fuel plates, and 23% thicker for LEU fuel plates.</i></p>
<p>Branch 03</p>	<p>Coolant channel restriction with burnup will be limited to 5 mil in Branch Case 03, and no coolant channel restriction with burnup will be assumed in Branch Case 03b.</p> <p><i>Rationale: The formation of an oxide layer on the fuel plates, irradiation-induced fuel swelling, and fuel creep will cause the coolant channels between fuel plates to become constricted with fuel burnup. Acceptance criteria for the elements limit this constriction to 10 mil and 8 mil, respectively, for the HEU and LEU fuel elements. This branch case assesses the impact on the accident consequences if the coolant channel restriction is less than the assumed limit.</i></p>

<p>Branch 04</p>	<p>Initiate the reactivity insertion accident from different steady-state conditions for the coolant inlet temperature, coolant flow rate, and system pressure. These parameter variations will be done independently.</p> <ul style="list-style-type: none"> • Branch 04a (HEU): $T_{\text{inlet}} = 155^{\circ}\text{F}$ and core coolant flow rate = 3,700 gpm • Branch 04b (HEU): $T_{\text{inlet}} = 130^{\circ}\text{F}$ and core coolant flow rate = 3,200 gpm • Branch 04a (LEU): $T_{\text{inlet}} = 145^{\circ}\text{F}$ and core coolant flow rate = 3,700 gpm • Branch 04b (LEU): $T_{\text{inlet}} = 130^{\circ}\text{F}$ and core coolant flow rate = 3,300 gpm • Branch 04f (HEU and LEU): Pressurizer pressure at 90 psia and pressurizer level of +4 inches. <p><i>Rationale: In the “request for additional information” questions (RAI) in the review of the MURR SAR [23], the reviewer asked for an analysis of the accident consequences at “TS or license limits.” The response from MURR was to evaluate the accident at the LSSS conditions for coolant inlet temperature and flow rate.</i></p> <p><i>The system pressure assumed in the base case was for a pressurizer pressure of 74.3 psia (60 psig) and a pressurizer level of -7 inches. This is a pressure below the LSSS pressure of 75 psia. The Branch Case 04f assesses the impact of using a pressure that is above the normal operating band for MURR.</i></p>
<p>Branch 05</p>	<p>Perform the analyses of the step insertion of reactivity and blade withdrawal accidents for Mode II operating conditions.</p> <p>HEU: initiated from reactor power = 5 MW (primary system power = 4.70 MW) and at LSSS flow (1600 gpm), temperature (155°F), and pressure (75 psia pressurizer pressure, -16 inch water level).</p> <p>LEU: initiated from reactor power = 6 MW (primary system power = 5.78 MW) and at LSSS flow (1650 gpm), temperature (145°F), and pressure (75 psia pressurizer pressure, -16 inch water level).</p> <p>For the startup accident, the initial conditions will be a steady-state core power of 1.0 Watt and LSSS conditions for the coolant flow rate, inlet temperature, and pressure.</p> <p><i>Rationale: This repeats a case performed by MURR in response to the RAI’s.</i></p>
<p>Branch 06</p>	<p>The step reactivity insertion accident was modeled in the MURR SAR and the Phase I analysis with an insertion time of 10^{-5} seconds. Branch cases will be run with the reactivity insertion occurring in 10^{-3} (Branch 06a) and 10^{-1} (Branch 06b) seconds.</p> <p><i>Rationale: The step insertion of 0.6% $\Delta k/k$ represents the ejection of the Tech Spec limit total worth of all unsecured experiments. For this ejection to occur in 10 microseconds (or even 1 millisecond) is extremely rapid, and beyond any realistic scenario for the ejection of unsecured experiments in MURR. An ejection time of > 0.1 second is more realistic.</i></p>

Branch 07	<p>The blade withdrawal and startup accidents will be modeled with the stainless steel regulating blade withdrawn concurrently with the shim blades. The Technical Specification limit for the reactivity insertion from regulating blade withdrawal is 0.025% $\Delta k/k/s$, giving a combined reactivity insertion rate of 0.055% $\Delta k/k/s$. However, the Tech Spec limit for the total worth of the regulating blade is 0.6% $\Delta k/k$, so that the reactivity addition from the regulating blade withdrawal will last for only the first 24 seconds (0.6/0.025) of the accident. After this, the Tech Spec limit for the shim blade withdrawal of 0.03% $\Delta k/k/s$ will apply.</p> <p><i>Rationale: The RAI's for the MURR SAR review requested justification for not including the regulating blade withdrawal in the startup accident. While administrative procedures prohibit the simultaneous withdrawal of both shim blades and the regulating blade, the scenario was analyzed in MURR's response to the RAI.</i></p>
Branch 08	<p>The startup accident will be evaluated in Mode III/natural circulation conditions initiated from a steady-state power of 50 kW, a coolant inlet temperature of 100 °F (same as pool temperature), and atmospheric pressure (14.3 psia at the MURR site; 21.02 psia at the bottom of the fuel element).</p> <p><i>Rationale: Low power operations will be used for startup physics testing of the LEU core. Note that this accident will be analyzed only for the reference core loaded with all-fresh LEU fuel (reference core 5B1).</i></p>

Table 4.6. MURR Steady-State Conditions Prior to Reactivity Insertion Accidents.

Parameter	Mode I Operations		Phase II Reactivity Insertion Accident Analyses
	Nominal	LSSS	
Reactor Power (MW)	10 (HEU) 12 (LEU)	12.5 ¹ (HEU) 15.0 ¹ (LEU)	10 (HEU) 12 (LEU)
Core (Primary System) Power (MW)	9.40 ² (HEU) 11.57 ² (LEU)	11.75 ² (HEU) 14.46 ² (LEU)	9.40 ² (HEU) 11.57 ² (LEU)
Primary Coolant Flow Rate (gpm)	3,800	3,200 (HEU) 3,300 ³ (LEU)	3,700
Pressurizer Pressure (psia)	84	75	74.3
Pressurizer Level (inches)	0	-16	-7
Core Inlet Pressure (psia)	68.4 ⁴ (HEU) 69.1 ⁴ (LEU)	63.2 ⁴ (HEU) 63.0 ⁴ (LEU)	59.3 ⁴ (HEU) 59.9 ⁴ (LEU)
Coolant Inlet Temperature (°F)	120	155 (HEU) 145 ³ (LEU)	125

¹ 125% of full core power.² HEU core power is 94.0% of reactor power (6.0% of power deposited outside primary system); LEU core power is 96.4% of reactor power. [15]³ LSSS conditions for primary coolant flow rate and core inlet temperature for LEU core determined in steady-state safety basis [2].⁴ Pressure calculated just inside the entrance to the coolant channels between the MURR fuel plates by spreadsheet model.

4.3 Results for Reactivity Insertion Accidents

4.3.1 HEU Core Reactivity Insertion Accident Results

Results for the 0.6% $\Delta k/k$ step RIA in cores loaded with HEU fuel are presented in Table 4.7. The all-fresh Core 1B1 is not a core state that is expected to be achieved in MURR operations, but was evaluated in the Phase II analysis for consistency with the Phase I analysis. The reference mixed-burnup Core 3A is typical for MURR operations and is the primary focus for the Phase II analysis.

Table 4.7. Peak Reactor Power and Peak Fuel Temperature for 0.6% $\Delta k/k$ Step Insertion of Reactivity in HEU Reference Cores.

		Reactor Power, MW		Peak Fuel Temperature, °C (Plate)		<i>Effect of branch case on peak fuel temperature relative to base case for Core 3A, °C</i>
		All-Fresh Core (1B1)	Mixed-Burnup Core (3A)	All-Fresh Core (1B1)	Mixed-Burnup Core (3A)	
Base Case ($T_{inlet} = 125$ °F, Flow = 3700 gpm, Pressurizer pressure = 74.3 psia, Pressurizer level = -7 inches)		34.25	34.27	212.5 (BOL)	194.0 (BOL)	
Branch02	Increase oxide layer thickness by 18%		34.34		194.5 (BOL)	+0.4 <i>Minimal</i>
Branch03b	No coolant channel restriction with burnup		34.53		203.0 (BOL)	+9.0 <i>More severe</i>
Branch04a	$T_{inlet} = 155$ °F (LSSS temp.)		33.61		199.7 (BOL)	+5.6 <i>More severe</i>
Branch04b	Flow = 3200 gpm (LSSS flow)		34.35		203.2 (BOL)	+9.2 <i>More severe</i>
Branch04f	Pressurizer pressure = 90 psia Pressurizer level = +4 inches		34.28		194.0 (BOL)	0.0 <i>Minimal</i>
Branch05	Mode II LSSS Operating Conditions		18.23		188.7 (BOL)	-5.3 <i>Less severe</i>
Branch06b	$t_{insertion} = 0.1$ s		34.27		194.0 (BOL)	0.0 <i>Minimal</i>

The step insertion of 0.6% $\Delta k/k$ reactivity in the critical, steady-state core results in a sharp reactor power rise from 10 MW to nearly 34.3 MW in both the all-fresh and mixed-burnup reference cores before the transient is curtailed by negative reactivity feedback effects from changes in the coolant conditions (density and temperature). The transient is terminated by reactor scram at 0.152 seconds after the reactivity insertion. The peak fuel meat temperature reached during the accident in the all-fresh core is 212.5 °C. However, this is for a core state that

is not used for reactor operations (all fresh fuel, empty flux trap, skewed and highly depleted control blades). For the reference mixed-burnup core that is typical for MURR operations, the peak HEU fuel temperature reached in the accident is predicted to be 194.0 °C in a fresh plate 1 in element position 5. This is well below the measured U-Al_x blister threshold temperatures which have ranged upwards from 480 °C [20]. The peak fuel temperatures in the MOL and EOL plates in this core are predicted to be 188.8 °C and 168.6 °C, respectively.

It is noted that the Phase II model of the all-fresh Core 1B1 estimates a peak power which is 0.5 MW higher than the Phase I model result (34.25 vs. 33.75 MW). The heat capacity of the HEU fuel is about 30% higher in Phase II than in the Phase I model (see Table 2.1). Consequently, for a given amount of energy generated during the transient, there will be a smaller temperature rise of the fuel meat, and in turn for the clad and coolant. As a result, the negative reactivity feedback effects associated with coolant temperature and density changes are smaller so that a higher power is reached in the Phase II analysis before the power spike is curtailed. However, the Phase II predicted peak fuel temperature is about 11 °C lower than the Phase I result. There are a number of reasons for this. For example, the Phase II analysis removed unnecessary conservatisms from the accident modeling by 1) accounting for the fraction of fission energy deposited outside the primary system, and 2) accounting for the reduced heat flux into the hot channel due to the uneven power split between the hot and cold surfaces of the fuel plate. Furthermore, the differing initial steady-state conditions assumed for the Phase II accident analysis compared to those used in the Phase I analysis would contribute to a lower fuel temperature (lower coolant inlet temperature and higher coolant flow rate). Lastly, the version of PARET/ANL used in the Phase II analysis has improvements to the steady-state thermal-hydraulics solution, which reduces the calculated peak fuel temperature at steady-state by 0.7 °C.

Several branch cases were run to estimate the effect of potential non-conservative variations of the core operating conditions on the severity of the accident in the HEU core. In the Branch02 case, the oxide layer thickness on the MOL and EOL plates was increased by 18% relative to the base case. An oxide layer thickness of 0.54 mil on an EOL plate in the plate 1 position had previously been estimated from a modified Griess correlation [22] based on fuel elements in a core operating at nominal conditions (see Table 4.4). However, if the MURR consistently operated at the limits of the operating band assumed for the RIAs, the oxide layer thickness on an EOL plate in the plate 1 position is estimated to be 0.63 mil (18% thicker). The peak reactor power reached during the accident increases only 0.07 MW in this branch case relative to the base case, while the increased oxide thickness results in a 2.6 °C increase in the peak fuel temperature in the EOL plate (up to 171.2 °C from 168.6 °C in the base case). However, there is only a marginal effect on the peak fuel temperature in the limiting BOL plate in the core.

In the base case, the coolant channel restriction at EOL due to irradiation effects was set at the MURR acceptance criteria of ≤ 10 mil. In the Branch03b case it was assumed that coolant channels remain at their nominal gap (i.e., there is no channel restriction with burnup). Under these conditions the peak power is 0.3 MW higher relative to the base case and the peak fuel temperature reached in the BOL plate during the accident is 9.0 °C higher. Without the reduction of the coolant channel gap from burnup-related effects, the EOL channel receives a greater fraction of the coolant flow rate than in the base case, reducing the coolant flow rate in the BOL

channel. This effect, along with the increased reactor power level reached in the transient, leads to the higher fuel temperature.

Three branch cases based on the LSSS conditions were evaluated. In the Branch04a and Branch04b cases, the coolant inlet temperature and coolant flow rate are set at the LSSS values, respectively. If the coolant inlet temperature is increased from 125 °F (51.7 °C) to the LSSS value of 155 °F (68.3 °C) (Branch Case 04a), there is a decrease in the peak transient power by about 0.7 MW. There is more negative reactivity feedback from coolant density effects with the higher inlet coolant temperature, resulting in a lower peak power before the power burst is curtailed. However, the peak fuel temperature is higher in this case because the higher coolant inlet temperature raises the fuel temperature by nearly 6 °C. If the coolant flow rate is reduced from 3,700 gpm to the LSSS flow rate of 3,200 gpm (Branch Case 04b), the peak transient power is slightly higher than in the base case for the accident analysis. The peak fuel temperature reached during the transient is higher by more than 9 °C, primarily because the lower coolant flow rate causes an increase in the fuel temperature.

In Branch Case 05, the steady-state conditions in the analysis were set to the LSSS coolant inlet temperature and coolant flow rate for Mode II operations of MURR. Under these conditions, the coolant inlet temperature is 155 °F (68.3 °C), and the core coolant flow rate (single coolant loop) is 1,600 gpm. The total steady-state reactor power is 5 MW and the overpower trip setting is 6.25 MW. In this case, the peak transient power is only 18.23 MW and the peak fuel temperature reached during the transient is 188.7 °C. The predicted peak fuel temperature from the 0.6% $\Delta k/k$ RIA is 5.3 °C lower in this case compared with the base case for Mode I (full-power) operations.

Branch Case 04f was evaluated to assess the impact of the coolant pressure on the accident consequences. In the base case, the pressurizer pressure was assumed to be 74.3 psia (60 psig), and the pressurizer water level was assumed to be -7 inches. This pressure corresponds to low end of 60 psig for the system pressure in the MURR operating band, which is measured at the PT-943 pressure gauge. This pressure gauge is located near the junction of the two coolant flow loops. The actual pressure at the pressurizer cannot be measured during operations because there is no direct readout of the pressurizer pressure in the control room. The pressure measured at the PT-943 pressure gauge is calculated to be about 6.1 psi lower than the pressure in the pressurizer due to friction and head losses. The upper end of the pressure measurement at the PT-943 gauge is 80.3 psia (66 psig). Adding 6.1 psi to account for friction and head losses, the pressurizer pressure corresponding to the upper end of the operating pressure would be 86.4 psia. This was rounded up to 90 psia for this branch case. The pressurizer water level was also set at the upper end of +4 inches. It can be seen from the results in Table 4.7 that the peak power from the accident is only 0.01 MW higher than in the base case, and there is no change in the peak fuel temperature relative to the base case. Therefore, it is concluded that the coolant pressure has no impact on the accident consequences.

The last branch case evaluated the impact of the insertion time for the 0.6% $\Delta k/k$ reactivity in the accident. The time needed to eject the unsecured experiments from the core was conservatively set to 10 μ s in the base case. In the Branch06b case the ejection time was increased to

0.1 seconds, which is a more realistic time. This adjustment had a negligible effect on the accident consequence.

The modeling variations in the Branch03b, Branch04a, and Branch04b cases are within the allowed operations of the MURR. Likewise, each of these cases resulted in an increase in the peak fuel temperature predicted for the 0.6% $\Delta k/k$ step RIA. Among all of the cases considered, the peak U-Al_x fuel temperature was 203.2 °C for the 0.6% $\Delta k/k$ step RIA.

The Phase I analysis [4] showed that in the HEU core the 0.6% $\Delta k/k$ step reactivity insertion accident is the most limiting in terms of peak fresh fuel temperature reached during the accident. In the Phase II analysis, modeling enhancements were made to improve the accuracy of the PARET/ANL predictions relative to the Phase I analysis, as described in Section 4.2.2. For example, depleted MOL and EOL plates were included in the PARET/ANL model, as well as improvements to the heat source description that removes unnecessary conservatisms and more accurately reflects the heat conduction from the fuel plates into the hottest coolant channels in the core. For an accidental step insertion of 0.6% $\Delta k/k$ in the HEU core (0.77 inserted), the peak U-Al_x temperature is well below the lowest measured blister threshold temperature of 480 °C [20]. Even when depleted MOL and EOL plates are included in the PARET/ANL analysis for mixed-burnup reference core, the peak HEU fuel temperature still occurs in the fresh, BOL plate.

The peak fuel temperature calculated by the Phase II analysis for this accident is less than that calculated during the Phase I analysis, even when accounting for potential non-conservative variations in reactor operating conditions that could increase the peak fuel temperature resulting from the accidental reactivity insertion. For example, the peak HEU fuel temperature for all cases considered for the reference Core 3A is predicted to be 203.2 °C, compared with a peak temperature of 216.9 °C predicted during the Phase I analysis. Consequently, the other accidents considered in the Phase I analysis (i.e., the blade withdrawal and startup accidents) will not be repeated in the Phase II analysis. It is not expected that higher peak fuel temperatures will be predicted for these other types of reactivity insertion accidents, even if MOL and EOL plates are included in the analysis.

4.3.2 LEU Core Reactivity Insertion Accident Results

4.3.2.1 0.6% $\Delta k/k$ Step Reactivity Insertion

The results for the 0.6% $\Delta k/k$ step reactivity insertion accident in the LEU reference cores are summarized in Table 4.8. The base case assumed the same operating conditions as for the HEU accident analyses. The accident was modeled in two reference core configurations. Core 5B1 is an all-fresh core with an empty flux trap and highly-depleted control blades that are mismatched positions. This is a core state that will not be achieved in MURR operations. However, since this core state was considered in the Phase I analysis, it was evaluated here for the sake of providing continuity with the current work. The mixed-burnup Core 7A was selected as the reference core because it is a core condition that will be achieved in normal operations after the reactor is converted to LEU fuel.

Table 4.8. Peak Reactor Power and Peak Fuel Temperature for 0.6% $\Delta k/k$ Step Insertion of Reactivity in LEU Reference Cores.

		Reactor Power, MW		Peak Fuel Temperature, °C (Plate)		<i>Effect of branch case on peak fuel temperature relative to base case for Core 7A, °C</i>
		All-Fresh Core (5B1)	Mixed-Burnup Core (7A)	All-Fresh Core (5B1)	Mixed-Burnup Core (7A)	
Base Case ($T_{in} = 125$ °F, Flow = 3700 gpm, Pressurizer pressure = 74.3 psia, Pressurizer level = -7 inches)		41.82	42.00	251.0 (BOL)	264.0 (MOL)	
Branch01	Decrease κ_{U10-Mo} by 20%		42.00		270.9 (MOL)	+6.9 <i>More severe</i>
Branch02	Increase oxide layer thickness by 23%		42.07		270.8 (MOL)	+6.8 <i>More severe</i>
Branch03b	No coolant channel restriction with burnup		42.12		266.5 (MOL)	+2.5 <i>More severe</i>
Branch04a	$T_{inlet} = 145$ °F (LSSS temp.)		41.61		263.7 (MOL)	-0.3 <i>Minimal</i>
Branch04g	$T_{inlet} = 118$ °F (Lower end of operating band)		42.15		264.6 (MOL)	+0.6 <i>More severe</i>
Branch04b	Flow = 3300 gpm (LSSS flow)		42.05		267.9 (MOL)	+3.9 <i>More severe</i>
Branch04f	Pressurizer pressure = 90 psia Pressurizer level = +4 inches		42.01		264.0 (MOL)	0.0 <i>Minimal</i>
Branch05	Mode II LSSS Operating Conditions		22.64		227.6 (MOL)	-36.4 <i>Less severe</i>
Branch06b	$t_{insertion} = 0.1$ s		38.48		263.1 (MOL)	-0.9 <i>Less severe</i>

The step insertion of 0.6% $\Delta k/k$ reactivity in the critical, steady-state core results in a sharp reactor power rise from 12 MW to about 42 MW in both the all-fresh and mixed-burnup reference cores before the transient is curtailed by negative reactivity feedback effects from changes in the fuel temperature and coolant conditions (density and temperature). The transient is terminated by reactor scram at 0.152 seconds after the reactivity insertion. The peak fuel meat temperature reached during the accident in the all-fresh Core 5B1 is 251.0 °C. This is a core state that is not expected to be used for reactor operations. For the reference mixed-burnup core that is typical for MURR operations (Core 7A), the predicted maximum LEU fuel temperature is about 142 °C in an MOL plate at steady-state, and reaches a peak of 264.0 °C in the accident. The peak fuel temperatures in the EOL and BOL fuel plates are 262.1 and 240.3 °C, respectively.

The PARET/ANL model of the all-fresh Core 5B1 for the Phase II analysis predicts a peak power which is about 0.4 MW lower than the Phase I model result (41.82 vs. 42.21 MW). The Phase I analysis was performed with an earlier version of PARET/ANL which required that the fuel plate dimensions for the average- and peak-plate channels in the model be the same. The average meat thickness in the variable-thickness plates of the CD35 element is 18.87 mil. However, the meat thickness for the average-plate channel in the PARET/ANL model used in the Phase I analysis was only 17 mil, which is the meat thickness of plate 23. It was considered more important to accurately represent the dimensions of the peak plate because of the effect of the meat thickness on heat conduction and the peak fuel centerline temperature. Using an 11% thinner fuel meat dimension (17 mil vs. 18.87 mil) in the Phase I analysis reduced the surface heat flux for the average plate computed by PARET/ANL by 11% (the heat source profile in PARET/ANL is based on the power density, and no correction was made to this input quantity in Phase I to conserve the heat flux for the average plate). Consequently, the coolant temperature rise in the average channel was underpredicted in the Phase I analysis. As a result, there was less reactivity feedback due to coolant temperature effects in the Phase I analysis, resulting in a higher prediction of the peak power before the transient was curtailed by reactivity feedback effects.

The predicted peak fuel temperature in the peak BOL plate in the all-fresh Core 5B1 is 8.5 °C lower in the Phase II analysis compared to Phase I. This is partly due to the 0.4 MW lower total reactor power predicted during the transient, as discussed above. Likewise, the heat source description for the peak plate is 3.6% lower in the Phase II analysis. It was calculated that only 96.4% of the fission power is deposited in the core primary system, while the remaining 3.6% is directly deposited in the ex-core structures (e.g., reactor reflectors and pool) [15]. The Phase II analysis also accounts for the reduced heat flux into the hot channel due to the uneven power split between the hot and cold surfaces of the fuel plate. Lastly, the differing initial steady-state conditions assumed for the Phase II accident analysis compared to those used in the Phase I analysis, and improvements to the steady-state thermal-hydraulics solution in the version of PARET/ANL used for the Phase II analysis, reduces the calculated peak fuel temperature.

Figure 4.3 plots the time-dependent results for the 0.6% $\Delta k/k$ step reactivity insertion accident in Core 7A. The overpower trip setting of 15 MW is reached at 1.8 milliseconds after the start of the transient. This is followed by a 150 millisecond delay before the reactor scram occurs. Prior to the reactor scram which will shut down the reactor and terminate the accident, the reactor reaches a peak power of 42.0 MW at 0.043 seconds after the reactivity insertion. This power burst is then curtailed due to inherent reactivity feedback effects. The maximum fuel meat temperature in the fuel plates increases due to the energy generated by the transient, reaching a peak temperature in the BOL plate at 65 milliseconds after the start of the accident, and at 152 milliseconds in the MOL and EOL plates, before heat conduction from the fuel meat to the surrounding clad and coolant decreases the fuel temperature. The time to reach the peak temperature is longer for the burned plates because the lower thermal conductivity and oxide layer for these plates relative to the unirradiated fuel plate inhibits heat conduction.

The delay time of 150 milliseconds from the time that the scram signal is sent to the start of the blade drop is the same as used in References 4 and 6 for the previous RIA analyses. A shorter time of 75 milliseconds is used for the LOCA and LOFA analyses in Chapters 5 and 6,

Figure 4.3. Reactor Power and Maximum Fuel Temperatures for 0.6% $\Delta k/k$ Step Reactivity Insertion in LEU Core 7A.

respectively. The shorter delay time of 75 milliseconds is based on improved measurements of the safety system trip functions timing performed at MURR in May and June of 2014. The longer delay time of 150 milliseconds was retained for the modeling of the RIAs in the current work and will yield conservative results for the peak fuel temperatures resulting from the accident. If the 75 millisecond delay time is used, the predicted peak MOL plate fuel temperature is about 5 °C lower, and about 10 °C lower for the predicted EOL plate fuel temperature.

Several branch cases were run to estimate the effect of potential non-conservative variations of the thermo-physical properties or core operating conditions on the severity of the accident. In the Branch01 case, the thermal conductivity of the U-10Mo was reduced by 20% from that used in the base case. This takes into account the experimental uncertainty in the U-10Mo thermal conductivity measurements conducted in Reference 9. The predicted peak reactor power from the reactivity insertion is not affected by this modeling change. However, the predicted peak fuel temperatures are higher because of the lower thermal conductivity. The peak fuel temperature in the limiting MOL fuel plate in Core 7A is 6.9 °C higher relative to the base case.

In the Branch02 case, the oxide layer thickness on the MOL and EOL plates was increased by 23% relative to the base case to account for the impact of core operating conditions on the oxide thickness. An oxide layer thickness of 0.93 mil on the concave surface of an EOL plate in the plate 23 position had previously been estimated from a modified Griess correlation [22] based on fuel elements in a core operating at nominal conditions. As was done for the HEU accident analyses, if the MURR consistently operated at the limits of the operating band assumed for the reactivity insertion accidents, the oxide layer thickness on the fuel plates was computed to be

23% thicker. While there is only a 0.07 MW increase in the peak reactor power relative to the base case, the increased oxide thickness results in a 6.8 °C increase in the peak fuel temperature in the limiting MOL plate.

In the base case, the coolant channel restriction at EOL due to irradiation effects was set at the anticipated MURR acceptance criteria for LEU elements of ≤ 8 mil. In the Branch03b case it was assumed that coolant channels remain at their nominal gap (i.e., there is no channel restriction with burnup). Under these conditions the peak power is 0.1 MW higher relative to the base case and the peak fuel temperature reached in the limiting MOL plate during the accident is 2.5 °C higher. Without the reduction of the coolant channel gap from burnup-related effects, the EOL channel receives a greater fraction of the coolant flow rate than in the base case, reducing the coolant flow rate in the BOL and MOL channels. This effect, along with the increased reactor power level reached in the transient, leads to the higher fuel temperature.

Three branch cases based on the LSSS conditions were evaluated. In the Branch04a and Branch04b cases, the coolant inlet temperature and coolant flow rate were set at the LSSS values of 145 °F (62.8 °C) and 3,300 gpm, respectively. Using the LSSS inlet temperature (Branch Case 04a) gives a 0.3 °C decrease in the fuel temperature. Although the steady-state fuel temperature is higher because the coolant inlet temperature is increased by 11.1 °C, the peak power reached during the transient is 0.4 MW lower so that there is less energy deposition in the core due to the transient. Initiating the transient from the LSSS flow rate (Branch Case 04b) has little effect on the peak power reached during the accident, but increases the peak fuel temperature 3.9 °C because of the lower coolant flow rate.

In Branch Case 05, the steady-state conditions in the analysis were set to the LSSS coolant inlet temperature and coolant flow rate for Mode II operations of MURR. Under these conditions, the coolant inlet temperature is 145 °F (62.8 °C), and the core coolant flow rate (single coolant loop) is 1,650 gpm. The total steady-state reactor power is 6 MW and the overpower trip setting is 7.5 MW. In this case, the peak transient power is only 22.6 MW and the peak fuel temperature reached during the transient is 227.6 °C. The predicted peak fuel temperature from the 0.6% $\Delta k/k$ RIA is nearly 40 °C lower in this case compared with the base case for Mode I (full-power) operations.

A steady-state coolant inlet temperature at the upper end of the normal operating band was selected for the base case with the expectation that it would yield conservative results. However, when the coolant inlet temperature was raised from 125 °F (51.7 °C) to the LSSS temperature of 145 °F (62.8 °C) in Branch Case 04a, the predicted peak fuel temperature decreased slightly relative to the base case. When the coolant inlet temperature is increased in the branch case, there are competing effects that affect the fuel temperature. The increased coolant temperature causes an increase in the fuel temperature, while the lower peak power reached during the accident (the reactivity feedback from coolant voiding is greater when the coolant inlet temperature is higher) reduces the fuel temperature rise that results from the accident. The latter effect is more dominant in Branch Case 04a, so that the peak fuel temperature predicted for the accident is lower than the base case. Consequently, Branch Case 04g was run with the coolant inlet temperature set at 118 °F (47.8 °C), which is the lower end of the operating band, to assess what impact decreasing the inlet temperature would have on the accident consequences.

Initiating the accident with the inlet temperature at the lower end of the operating band results in a 0.15 MW increase in the peak reactor power relative to the base case, and causes a 0.6 °C increase in the predicted peak fuel temperature during the accident. The peak power reached during the accident is higher because there is less reactivity feedback from the coolant voiding when the inlet temperature is lower.

The background for Branch Case 04f, which assesses the impact of variations in the coolant pressure on the accident consequences, was described above in Section 4.3.1. As for the HEU fuel, setting the coolant pressure well above the normal operating band has a negligible effect on the peak reactor power or fuel temperature in the MOL plate. A careful review of the results for this accident analysis did find that the predicted peak fuel temperature in the BOL plate increased by 5.0 °C relative to the base case when the coolant pressure was increased. At the higher pressure, the saturation temperature of the coolant increases so that there is less nucleate boiling in the BOL channel during the accident. Thus, there was less heat transfer from the fuel plate to the bulk coolant, and the fuel temperature was higher. Nonetheless, this BOL fuel peak temperature of 245.3 °C is still well below the peak temperature of 264.0 °C for the MOL plate, and the accident consequences are not more severe when the coolant pressure is raised above that assumed in the base case.

The last branch case evaluated the impact of the insertion time for the reactivity in the accident analysis. The 0.6% $\Delta k/k$ reactivity inserted in this accident represents the Tech Spec limit on the total positive worth of all experiments in the center flux trap (CFT). Therefore, the accident postulates the ejection of the experiments in the CFT, which may be distributed along the 24 inch axial length of the fuel in the core. The 10 microsecond ejection time in the MURR SAR analysis [6] represents an extremely rapid time frame for this to occur. For example, assuming the experiments travel the fuel length of 24 inches in 10 microseconds requires an average speed of 200,000 feet/second. In Branch Case 06b, the reactivity insertion time in the model was increased from 10 microseconds to 0.1 seconds, which would be consistent with an average ejection speed of 20 fps. The longer insertion time decreases the peak power that is reached before the transient is curtailed by the negative reactivity feedbacks. The peak fuel temperature is about 1 °C lower relative to the base case.

In the Phase II analysis, MOL and EOL fuel plates were modeled since the peak temperature can occur in plates other than those at BOL conditions. Furthermore, modeling enhancements were made to improve the accuracy of the PARET/ANL predictions relative to the Phase I analysis, as described in Section 4.2.2. Lastly, a number of parameter variations were modeled in a series of branch cases to cover potential non-conservative variations of thermo-physical properties or core operating conditions that may affect the severity of the accidents.

The modeling variations in the Branch01, Branch03b, Branch04a, Branch04b, and Branch 04g cases are either within the experimental uncertainty for the U-10Mo thermo-physical properties or within the allowed operations of the MURR. It is unlikely that the oxide layer thickness will increase by 23% relative to that predicted under the nominal operating conditions (Branch02), as that would require the reactor to operate consistently at the limits of the operating band. Among all of the cases considered, the peak U-10Mo fuel temperature was 270.9 °C for the accidental step insertion of 0.6% $\Delta k/k$ in the LEU core (0.78 inserted).

In all of the HEU and LEU cases the fuel temperature remains well below temperatures where fuel blisters have been experimentally observed. In UAl_x fuel, blisters have been observed in the range of 480 to 598 °C when irradiated fuel plates, at relevant fission densities, were placed in a furnace [20]. Only preliminary blister furnace data is available with monolithic UMo alloy fuel. For the monolithic UMo fuel blisters have been observed in the range of 400 to 530 °C when irradiated fuel plates were placed in a furnace at fission densities relevant to MURR [21].

For the LEU MURR cores, EOL peak local fission density has been calculated to be between 1.1×10^{21} and 3.4×10^{21} fissions/cm³ where the highest burnup is found in the outer plates of the element [22]. Monolithic UMo plates exceeding these burnup levels, up to 7×10^{21} fissions/cm³, have been blister tested and exhibit a blister temperature no lower than 365 °C [21]. This preliminary UMo monolithic data was measured across a range of U-10Mo plate geometries, fabrication variables and irradiation conditions. Fuel plates specifically representative of MURR geometry and irradiation conditions will be blister annealed in future tests. Based on these preliminary measurements, there is significant margin between the temperature where blisters have been measured and the peak fuel temperature calculated in the most limiting LEU reactivity insertion accident.

4.3.2.2 *Other Step Reactivity Insertion Accidents*

Two other step reactivity insertion accidents were analyzed for the reference mixed-burnup LEU core that is typical for MURR operations. The MURR Tech Spec limit for the maximum worth of any single unsecured experiment is 0.25% Δk/k, and 0.10% Δk/k is the Tech Spec limit for the maximum worth of any single moveable experiment. The results of these accident analyses in the reference LEU core are summarized in Table 4.9. The peak reactor power and peak fuel temperature in these accidents are substantially lower than in the 0.6% Δk/k step insertion accident. The peak fuel temperature that occurs during the rapid ejection of a single unsecured experiment is 176.1 °C in an MOL plate. For the rapid ejection of a single moveable experiment, the peak fuel temperature is 166.4 °C, also in an MOL plate. In this case, the margin to fuel damage is larger than in the 0.6% Δk/k reactivity insertion accident previously discussed.

Table 4.9. LEU Reference Core 7A: 0.25% Δk/k and 0.10% Δk/k Step Reactivity Insertion.

Parameter	0.25% Δk/k	0.10% Δk/k
Reactor Power, MW	17.60	15.94
Peak Fuel Temperature, °C (Plate) ^a	176.1 (MOL)	166.4 (MOL)

^a The peak fuel temperature in the core occurs in a BOL, MOL, or EOL plate with the highest heat flux.

4.3.2.3 *Blade Withdrawal Accident*

The second class of reactivity accidents analyzed to assess the impact of fuel conversion is a ramp reactivity insertion accident resulting from continuous control blade withdrawal initiated from the reactor operating at steady-state and full-power. Even though the reactivity addition resulting from the simultaneous withdrawal of all four shim control blades follows the typical differential rod worth curve behavior (with reduced worths at the beginning and at the end of rod withdrawal, as shown in Figure 4.1), the maximum value allowed by the MURR Tech Specs was imposed during this accident analysis. Thus, a positive ramp reactivity insertion at the rate of $0.03\% \Delta k/k/\text{sec}$ was considered.

Table 4.10 summarizes the results of the analysis of this accident scenario in the reference mixed-burnup core loaded with LEU fuel. The blade withdrawal accident is initiated from the same steady-state conditions as the step reactivity insertion accident assumed in Sections 4.3.2.1 and 4.3.2.2. In the base case, the overpower trip setting of 15 MW is reached 4.33 seconds after the beginning of blade withdrawal, followed by a scram at 4.48 seconds that terminates the accident and safely shuts down the reactor. Prior to the scram, $0.13\% \Delta k/k$ reactivity is inserted by the blade withdrawal. However, the peak power reached during the blade withdrawal accident is 15.15 MW, which is less than the peak power reached during the $0.1\% \Delta k/k$ step reactivity insertion accident, and substantially less than the peak power in the worst-case $0.6\% \Delta k/k$ step reactivity insertion accident. The peak fuel temperature in the base case is predicted to be 163.0°C and occurs in the MOL plate. This is just 21.3°C above the maximum fuel temperature in the plate at steady-state, compared with a 122.2°C fuel temperature rise for the $0.6\% \Delta k/k$ step reactivity insertion accident.

In addition to the base case, a series of branch cases were also run to assess the impact of potential non-conservative variations on thermo-physical properties and reactor operating parameters. All cases predict essentially the same peak reactor power for the blade withdrawal accident (about 15.15 MW), with the exception of Branch Case 05 for the reactor operating in Mode II conditions at half-nominal power. The reason for this is that the reactivity feedback effects do not introduce enough negative reactivity to curtail the transient, so that the reactor power steadily increases until the time of reactor scram. The Branch Cases 01, 02, 03b, 04b, 04f, and 05 for the blade withdrawal accident all show similar behavior for the peak fuel temperature relative to the base case as seen for the $0.6\% \Delta k/k$ step insertion accident. See Section 4.3.2.1 for explanations of these behaviors.

Branch Cases 04a and 04g were run to assess the effect of variations of the coolant inlet temperature on the peak fuel temperature. The behavior in these branch cases is different than that observed for the step reactivity insertion accident. For the blade insertion accident, increasing the coolant inlet temperature from 125°F (51.7°C) to the LSSS temperature of 145°F (62.8°C) causes a 7.6°C increase in the peak fuel temperature relative to the base case, while the same variation in the $0.6\% \Delta k/k$ step reactivity insertion accident resulted in a 0.3°C decrease for the peak fuel temperature relative to the base case. In the analysis of the $0.6\% \Delta k/k$ reactivity insertion accident, it was observed that there are competing effects that influence the peak fuel temperature when the coolant inlet temperature is changed. See the discussion of Branch Case 04g in Section 4.3.2.1. For the blade withdrawal accident, increasing the coolant

inlet temperature to the LSSS value has a negligible effect on the peak reactor power because the reactivity feedback effects have little influence on the accident behavior during this slow ramp reactivity insertion. Thus, the peak fuel temperature is much more influenced by the increase in the steady-state fuel temperature caused by the increased coolant inlet temperature in this branch case.

Table 4.10. LEU Reference Core 7A: Blade Withdrawal Accident (0.03% $\Delta k/k$ /second Ramp Insertion of Reactivity).

		Reactor Power, MW	Peak Fuel Temperature, °C (Plate)	Effect of branch case on peak fuel temperature relative to base case for Core 7A, °C
Base Case ($T_{in} = 125$ °F, Flow = 3700 gpm, Pressurizer pressure = 74.3 psia, Pressurizer level = -7 inches)		15.15	163.0 (MOL)	
Branch01	Decrease κ_{U10-Mo} by 20%	15.14	167.0 (EOL)	+4.0 <i>More severe</i>
Branch02	Increase oxide layer thickness by 23%	15.14	167.5 (EOL)	+4.5 <i>More severe</i>
Branch03b	No coolant channel restriction with burnup	15.14	163.7 (MOL)	+0.7 <i>More severe</i>
Branch04a	$T_{inlet} = 145$ °F (LSSS temp.)	15.14	170.6 (MOL)	+7.6 <i>More severe</i>
Branch04g	$T_{inlet} = 118$ °F (Lower bound of operating band)	15.15	160.6 (MOL)	-2.4 <i>Less severe</i>
Branch04b	Flow = 3300 gpm (LSSS flow)	15.14	169.3 (MOL)	+6.3 <i>More severe</i>
Branch04f	Pressurizer pressure = 90 psia Pressurizer level = +4 inches	15.15	163.0 (MOL)	0.0 <i>Minimal</i>
Branch05	Mode II LSSS Operating Conditions	7.57	149.6 (MOL)	-13.4 <i>Less severe</i>
Branch07	Withdraw Reg. blade simultaneous with Shim blades	15.17	163.0 (MOL)	-0.1 <i>Minimal</i>

In Branch Case 07, the simultaneous withdrawal of the regulating blade along with the four shim blades was evaluated. This is an unlikely accident scenario. Simultaneously withdrawing the regulating blade with any of the shim blades is prohibited by administrative procedure at MURR. Additionally, inadvertently performing this type of control system withdrawal is highly unlikely, as the two blade types are controlled by separate switches. Nonetheless, this accident scenario was analyzed and was found to have only a very minor effect on the predicted peak fuel temperature. It is interesting to note that the peak fuel temperature is slightly lower in this case,

even though the rate of reactivity insertion is higher. The reason for this is because the overpower trip setting is reached about 0.8 seconds sooner in this branch case, so that less total energy is added to the system prior to scram relative to the base case. Consequently, there is a smaller fuel temperature rise.

In all cases, the peak fuel temperature occurs in either an MOL or EOL plate. However, the temperatures are substantially lower than for the step insertion accident, and well below the measured U-10Mo blister threshold temperature.

4.3.2.4 *Startup Accident*

A second type of ramp reactivity insertion accident that has the potential for creating conditions that can lead to core damage is the startup accident. Typical reactor startups are very controlled activities with the control blade withdrawal done in small incremental steps. Also, blade withdrawal is stopped at predetermined control blade heights prior to reaching the critical control blade position. This is done to take measurements of core multiplication data and predicting the next step in the control blade withdrawal sequence in the approach to critical.

The startup accident postulates that all the control blades are withdrawn simultaneously (in gang) until the reactor passes through the critical state without any automatic or manual (operator) action to halt the uncontrolled blade withdrawal. Once the reactor passes through the critical state, the power increase is very rapid during such a transient. Furthermore, since the power level and temperatures remain extremely low during the majority of the transient, no major reactivity feedback mechanisms exist to affect the reactor response to the transient, except for during the very end of the transient. Thus, the entire reactivity effect seen by the core is due to the withdrawal of the control blades in the startup accident.

A positive ramp reactivity insertion at the rate of $0.03\% \Delta k/k/\text{sec}$, corresponding to the MURR Tech Spec limit on the maximum rate of reactivity insertion for all four shim control blades operating simultaneously, was introduced, starting with a subcritical power level of 1.0 W and a shutdown reactivity value of $-4.2\% \Delta k/k$. The coolant inlet temperature, flow rate, and pressure are assumed to be the same as in the other accident analyses (see Table 4.6). No automatic or manual reactor scram occurs in the analysis of this accident. Branch cases such as those evaluated for the other accident scenarios were also executed. Furthermore, a case in natural circulation mode which is unique to the startup accident was also executed. The results for all cases are summarized in Table 4.11. The predicted reactor power and peak fuel temperature are reported at 150 seconds, unless otherwise noted.

It was reported in Reference 4 for the Phase I analysis that the startup accident in the all-fresh Core 5B1 reached the critical state at approximately 140 seconds if there was no scram or operator intervention to halt the uncontrolled blade withdrawal. The reactor power level at this point was 16 W. At 150 seconds (2.5 minutes), the reactor power was 60.05 W and the period was 4.80 seconds. However, if the analysis had included modeling of the minimum 8-second reactor period trip, a reactor scram would have been initiated at about 145 seconds and safely shut down the reactor without any fuel damage.

Table 4.11. LEU Cores: Startup Accident Results (0.03% $\Delta k/k$ /second Ramp Insertion of Reactivity).

		Reactor Power, W		Peak Fuel Temperature, °C (Plate)		<i>Effect of branch case on peak fuel temperature relative to base case for Core 7A, °C</i>
		All-Fresh Core (5B1)	Mixed-Burnup Core (7A)	All-Fresh Core (5B1)	Mixed-Burnup Core (7A)	
Base Case ($T_{in} = 125$ °F, Flow = 3700 gpm, Pressurizer pressure = 74.3 psia, Pressurizer level = -7 inches)		60.75	60.75	51.7 (BOL)	51.7	
Branch01	Decrease $\kappa_{U_{10-Mo}}$ by 20%	60.75	60.75	51.7 (BOL)	51.7	0.0 <i>Minimal</i>
Branch02	Increase oxide layer thickness by 23%		60.75		51.7	0.0 <i>Minimal</i>
Branch03b	No coolant channel restriction with burnup		60.75		51.7	0.0 <i>Minimal</i>
Branch04a	$T_{inlet} = 145$ °F (LSSS temp.)	60.75	60.75	62.8 (BOL)	62.8	+11.1 <i>More severe</i>
Branch04g	$T_{inlet} = 118$ °F (Lower bound of operating band)		60.75		47.8	-3.9 <i>Less severe</i>
Branch04b	Flow = 3300 gpm (LSSS flow)	60.75	60.76	51.7 (BOL)	51.7	0.0 <i>Minimal</i>
Branch04f	Pressurizer pressure = 90 psia Pressurizer level = +4 inches		60.75		51.7	0.0 <i>Minimal</i>
Branch05	Mode II LSSS Operating Conditions	60.75	60.74	62.8 (BOL)	62.8	+11.1 <i>More severe</i>
Branch07	Withdraw Reg. blade simultaneous with Shim blades	7.88 kW (@ 140 seconds)	7.88 kW (@ 140 seconds)	51.7 (BOL)	51.7 (MOL)	+0.1 <i>Minimal</i>
Branch08	Natural Circulation: atmospheric pressure, $T_{inlet} = 100$ °F, Power = 50 kW, period scram	89.7 kW (@ 7.421 seconds)		59.9 (BOL)		

In the Phase II analysis, the results are essentially the same as the Phase I results. The critical state is reached at 140 seconds, when the reactor power is 15.8 W. The total reactor power at 150 seconds is predicted to be 60.75 W and the reactor period is 4.78 seconds. There is no discernable effect on the peak fuel temperature in the accident analysis relative to the initial conditions, which remains essentially the same as the coolant inlet temperature throughout the first 150 seconds of the accident. For the mixed-burnup reference Core 7A, there is no

difference in the temperature for the peak BOL, MOL, and EOL plates. Likewise, the Branch Cases 1-5 have a negligible effect on the reactor power of 60.75 W that is reached 150 seconds into the accident. Any difference in the peak fuel temperature relative to the base cases among the branch cases is due to changes in the coolant inlet temperature.

In Branch Case 07, the startup accident is analyzed with the regulating blade being simultaneously withdrawn with the four shim blades. This occurs for only the first 24 seconds of the accident analysis, at which point the maximum worth of the regulating blade (0.6% $\Delta k/k$) has been inserted. This, however, has a significant effect on the reactor power level, as can be seen in Figure 4.4. The critical state is reached in Branch Case 07 at 120 seconds, when the reactor power level is 15.8 W. If the shim blades continue to be withdrawn at the Tech Spec limit, at 140 seconds, the reactor power is 78.8 kW and the reactor period is 0.90 seconds. However, as the results in Table 4.11 show, if the transient is terminated at this point or earlier (due to the 8-second period trip, for example) the fuel temperature rise is no more than 0.1 °C.

Figure 4.4. Reactor Startup Accident in LEU Core 5B1.

In Branch Case 08, the startup accident is modeled with the reactor operating at a steady-state, critical power level of 50 kW and cooled by natural circulation. It is envisioned that low-power startup physics testing of the MURR loaded with 8 fresh LEU elements will be conducted at the time of reactor conversion. Thus, this accident was analyzed for the Core 5B1 only. Furthermore, the reactor overpower trip setting of 62.5 kW is assumed to fail (125% of full-power for this operating mode), but the minimum reactor period trip setting of 8 seconds was applied in the analysis.

The core is cooled by natural circulation by removing the reactor pressure vessel cover and opening a flange in the invert loop so that water from the pool can be drawn as coolant water through the bottom of the core and exit back into the 20,000 gallon pool. The coolant inlet temperature in this analysis is assumed to be 100 °F, and the pressure at the top of the fuel meat in this case is assumed to be 20 psia [24].

The PARET/ANL manual recommends performing calculations for cores cooled by natural circulation in two steps. First, a steady-state case is run to compute the coolant flow rate through the core due to buoyancy effects (parameter IFLOW=4). This was done and a flow rate that is more than a factor of 100 less than the steady-state flow rate under normal operating conditions was computed. The manual then recommends using this steady-state flow as a constant value in a “forced-flow” calculation in PARET/ANL (IFLOW=1) for analyzing the effects of the transient in the core cooled by natural circulation. This will be conservative, as the coolant flow rate through the core during the transient will naturally increase as the reactor power increases and the coolant temperature rises.

The results of the Branch Case 08 accident analysis are shown in Figure 4.5. From the steady-state power of 50 kW, the control blade withdrawal results in a gradual increase in the reactor power. The power reaches 62.5 kW at 3.80 seconds, but the reactor is not tripped in the analysis until the 8 second period trip is reached at 7.27 seconds. Subsequently, the reactor is safely shut down and the maximum fuel temperature in the core increases from 53.5 °C at steady-state to a peak of 59.9 °C, a rise of 7.4 °C.

Figure 4.5. Reactor Startup Accident in LEU Core 5B1 in Natural Circulation Mode.

Finally, the initial core with all-fresh LEU fuel will contain a mixture of standard fuel elements and transition fuel elements with borated side plates to serve as reactivity hold-down [25]. The presence of the borated side plates cause a slight reduction in the peak local heat flux in the all-fresh startup core relative to the Core 5B1 which contained only standard elements. Consequently, the results calculated here provide an upper bound on the expected peak fuel temperature for the startup accident in the fresh LEU core.

4.4 Conclusions

The insertion of excess reactivity in the HEU-fueled and LEU-fueled MURR has been analyzed to assess the impact of a rapid step insertion of positive reactivity and the impact of a continuous control blade withdrawal accident. Previous Phase I results evaluated the accident consequences on fresh HEU or LEU fuel plates only. In the current Phase II analysis, the modeling was expanded to include depleted plates as well as fresh plates. Furthermore, a series of Branch Cases were modeled to cover potential non-conservative variations of thermo-physical properties or core operating conditions that may affect the severity of the accidents. No hot channel factors were applied in the reactivity insertion accident analyses.

The first set of accidents considered was the positive step reactivity insertion of up to 0.6% $\Delta k/k$ in reference HEU and LEU cores. This value is the MURR Tech Spec limit for the maximum allowed absolute reactivity worth of all experiments in the center flux trap. It also corresponds to the maximum allowed worth of each secured removable experiment and the sum of the reactivity worth of all unsecured experiments installed in the reactor. Smaller Tech Spec limits on the reactivity worth of individual unsecured experiments and individual moveable experiments also exist.

The Phase II modeling analysis showed that the peak fuel temperature in the HEU cores under these accident conditions always occurs in the fresh (BOL) plates. For the reference LEU cores with a mixture of elements at BOL, MOL, and EOL conditions, the peak fuel temperatures are in the depleted plates due to the degradation of the U-10Mo thermal conductivity with burnup, the growth of an oxide layer on the fuel plates during core residence, and the restriction of the coolant channel gap due to burnup effects (oxide layer, and fission-induced fuel swelling and creep). The peak fuel temperatures in both HEU and LEU cores remain below the measured blister threshold temperature for U-Al_x and U-10Mo fuel, respectively. Thus, the results of the analysis showed that the MURR can withstand these unexpected step reactivity insertions without damage to the fuel.

A blade withdrawal accident in the reactor operating at full power was also analyzed. The MURR Tech Spec limit for the reactivity insertion rate from withdrawal of the four shim blades was modeled as a 0.03% $\Delta k/k/\text{second}$ ramp insertion in the reference LEU cores. The accident in the HEU reference cores was not re-evaluated in the Phase II analysis since the Phase I analysis [4] already confirmed that no fuel damage occurs for the HEU fuel in this accident. The results for the LEU cores showed that the peak fuel temperatures reached in this accident are well below the measured U-10Mo blister threshold temperature.

The consequences of a startup accident were also evaluated for the reference LEU cores. In this accident, the four shim blades are withdrawn in an uncontrolled manner at the MURR Tech Spec limit from a sub-critical core at low power. Even if the accident is not terminated by operator intervention within 2.5 minutes of the start of blade withdrawal, there is only a negligible increase in the fuel temperature due to the accident. Furthermore, the startup accident was evaluated in a core loaded with 8 fresh LEU elements at a steady-state power of 50 kW and cooled by natural circulation. Low-power physics testing of the MURR core at the time of conversion will be conducted under these conditions. In the unlikely event that the reactor

overpower trip fails to terminate the accident, the trip that occurs at a minimum reactor period of 8 seconds will safely shut down the reactor with a peak fuel temperature no higher than 59.9 °C.

The results indicate that fuel conversion to a proposed U-10Mo monolithic fuel will result in no damage to fresh or depleted fuel plates for any of the reactivity insertion accidents postulated for MURR.

5.0 LOSS-OF-COOLANT ACCIDENTS

5.1 Background and Goals

“Chapter 13, Accident Analyses,” of the University of Missouri Research Reactor (MURR) Safety Analysis Report (SAR) [6] demonstrates for the HEU core that various facility design features, safety limits, limiting safety system settings (LSSSs), and limiting conditions for operation ensure that no credible accident can lead to unacceptable radiological consequences to people or the environment. In particular, Chapter 13 of the SAR demonstrates that for all credible loss-of-primary-coolant and loss-of-primary-flow accidents (LOCAs and LOFAs) unacceptable consequences to people and the environment would not occur. For the conversion of the MURR core from HEU fuel to LEU fuel it is necessary to provide an analogous demonstration for the LEU core that is to replace the HEU core and to demonstrate adequate safety margins for the LEU core. Also, the performance of the two cores must be compared using consistent modeling assumptions.

A LOCA is an accident in which water (liquid or vapor) is lost from the primary system. Such accidents not only include a rupture of a reactor main inlet or outlet pipe, but also the loss of coolant through the rupture of a valve or smaller pipe in which the amount of coolant lost is relatively small. However, a depressurization accident in which only nitrogen is lost from the pressurizer is not considered a LOCA. This accident would be classified as a LOFA because the loss of pressure would initiate a trip of the primary pumps along with a scram. Similarly, a loss of site electric power leading to a primary flow coastdown would also be classified as a LOFA. LOFAs are discussed in Chapter 6. To facilitate model reliability and simplicity and the efficient use of resources, a single unified analytical model of the reactor, the plant, and the plant protection system is used to simulate all LOCAs and LOFAs for a specific reactor core.

The HEU core accident analysis of the 2006 MURR SAR was performed with the aid of the RELAP5/SCDAP code, which is described in Reference 7. It should be noted, however, that a later version, Mod 3.4(bi7), was used in the SAR analysis, as it was the production version at the time. The MURR RELAP5 model that was used for that analysis is described in considerable detail in “Appendix C, Thermal-Hydraulic Transient Analysis,” of the MURR SAR. This substantial model explicitly represents all 25 coolant channels and all 24 fuel plates of the limiting fuel element and includes the primary coolant loop and the pool loop. The model is the result of collaboration in 2006 between the MURR staff and a private consultant to improve the model developed for a PhD research project. Separate RELAP5 input files of some 8000 lines each were produced for each accident scenario.

The current effort is accomplished in two phases. The first phase, Phase I [4], obtained an initial comparison of the performance of the HEU and LEU cores during LOCA events and made an initial assessment of the performance of the MURR with the proposed LEU core with regard to these severe accidents. During Phase I of the modeling a few improvements were made to the 2006 MURR RELAP5 model. Then an LEU element core model was substituted for the HEU one. The power distributions for both elements were taken from recent neutron physics analyses [1].

The knowledge gained during Phase I was used to provide an early indication of the performance of the LEU core during severe LOCAs and to guide desired model improvements implemented in the current phase, Phase II. In the steady-state neutron physics analyses [1], detailed power distributions were predicted for a set of 24 potentially limiting HEU core states and the analogous 24 LEU core states. For the Phase I accident analysis only the highest heat flux HEU core, Core 1B1, and the highest heat flux LEU core, Core 5B1, were modeled. These are beginning-of-life cores (all fresh fuel) with no xenon. The following three features are in these HEU and LEU models because they contribute to a higher nuclear hot spot peaking factor: 1) The MURR flux trap has no holder or samples and is filled with only water, which is an operating condition only used for a brief period at less than 100 kw to perform reactivity measurements; 2) Control blades C and D are at eight years of depletion and are positioned 1 inch higher than control blades A and B, per the Technical Specification limit; 3) Control blades A and B are fresh. The HEU Core 1B1 element with the highest heat flux, X7, was represented in the RELAP5 HEU model. Similarly, the LEU Core 5B1 element with the highest heat flux, X6, was represented in the RELAP5 LEU model. As explained in Chapter 3, for the Phase II analysis more typical HEU and LEU cores, which would actually be operated at full power, are analyzed. These are the reference Core 3A for the HEU and reference Core 7A for the LEU.

5.2 Accidents to Be Considered

Section 13.1 of NUREG-1537 postulates nine events or categories for research reactors to consider. Among these nine are the LOCA and the LOFA. Figure 5.1 provides a schematic representation of the MURR primary system loop. The solid red rectangle on the left side of the figure represents the reactor annular core. When operating, the flow goes downward through the core to the lower plenum. From there the flow follows the piping that is shown in black through the hot-leg exit isolation valve V507A and on to the primary pumps. (Although the figure shows a gap to avoid overlapping the V507B valve, the piping from the V507A valve to the pump is continuous.) In the plant there are two parallel pump paths, each with its own pump and check valve. After the check valves the two parallel paths rejoin for 7 feet before dividing into two paths again, each with a heat exchanger followed by a V540A or V540B diaphragm valve. Then these two parallel paths merge and follow a path to the V507B core inlet isolation valve. In the simulation model the two essentially identical parallel pump paths are combined into a single equivalent path, as are the two heat exchanger paths. After the V507B inlet isolation valve, the primary piping enters the pool and the flow goes through the V502 check valve to the upper plenum, which connects to the top of the reactor pressure vessel above the core.

The upper plenum, reactor core, and lower plenum are contained within the pressurized primary coolant annular volume located between the inner and outer reactor pressure vessels, which are not explicitly labeled in Figure 5.1. The shaded area in the figure labeled near the top “Inside of the Pool” indicates the pool water, which surrounds the outer reactor pressure vessel and piping adjacent to the vessel. The pool water also flows down through the inner pressure vessel. The surface of the pool is at atmospheric pressure, which is taken to be 14.3 psia. The reactor safety systems and pool water limit the loss of coolant due to a primary loop breach that occurs within the pool boundary. The V507A and V507B isolation valves close after a substantial drop in core outlet pressure occurs in order to minimize the potential for loss of coolant from the reactor

Figure 5.1. Diagram of MURR Primary System Loop.

vessel during a LOCA. As Figure 5.1 shows, the piping exiting the lower plenum rises back up to near the top of the vessel before turning back downward. This “inverted loop” piping arrangement is to minimize the loss of coolant from the reactor vessel should the reactor exit pipe break outside the pool.

The “Fixed Pressure Source” labeled in the middle of Figure 5.1 is the plant pressurizer, which is attached via 2-inch piping to Loop A, one of the two parallel heat exchanger paths. In the model, the two paths are combined into a single path, which has the hydraulic diameters of each path and the combined flow areas of the two paths. The pressurizer is attached to the combined path in the model. The pressurizer isolation valve, valve 527, located along the 2-inch line is also shown in the figure.

A primary loop breach that occurs within the pool results in a depressurization and ejection of primary coolant from the pressurized system to the open pool. The primary loop breaches of greater concern are the ones that occur outside of the pool, especially those that occur between the exterior of the pool wall and either of the two main isolation valves (V507B and V507A).

The six light green vertical arrows in Figure 5.1 indicate locations in the model where pipe breaks can be simulated. Although not shown in the figure, at each arrow there is a fixed-pressure air sink at 14.3 psia that is separated from the pipe at that location by a closed valve whose full open diameter matches the pipe to which it is attached – 12, 8, or 2 inches. (All of these pipes are Schedule 40.) In a LOCA simulation one or more of these closed valves are assumed to instantaneously go to its full open position, connecting the pipe to the adjacent fixed-pressure sink modeling a fully ruptured pipe. It was determined that the cold-leg LOCA caused by a double-ended break at the V507B valve produces higher peak fuel temperatures than does the double-ended break at the V507A valve, which is the hot-leg LOCA. These are the only two LOCAs that are analyzed in the LOCA section of the MURR SAR, Section 13.2.3. Both are analyzed in this report.

A LOCA could also be initiated by a rupture of the diaphragm in either of the flow control diaphragm valves, valves 540A and 540B. This LOCA happened in the MURR plant on April 12, 2008. Although it is a less severe accident for the plant than the cold-leg LOCA initiated by a double-ended break of a 12-inch pipe, it is also analyzed in this report. In the analysis the diaphragm-rupture LOCA is modeled as a single 8-inch diameter breach at the 540 valves. Although this accident is more severe for the plant than a LOCA initiated by a rupture of the 2-inch pipe in the pressurizer line, the 2-inch rupture is also analyzed. Here severity is based on physical damage to the plant, amount of coolant lost, and difficulty to repair. As explained later in this chapter, some of the less severe accidents produced higher peak fuel temperatures than did the more severe ones. The simulation of the inadvertent loss of pressure at the pressurizer provided in the section entitled “Section 13.2.4 Loss of Primary Coolant Flow” of the MURR SAR is a simulation of a LOCA initiated by a rupture of a 2-inch pipe in the pressurizer line. Thus, the MURR SAR simulation uses a LOCA to approximate a LOFA. This is reasonable in this instance.

In summary, the following four LOCAs are analyzed for both an HEU core and an LEU core:

1. Cold-leg LOCA – double-ended 12-inch diameter break at the core side of the V507B cold-leg isolation valve.
2. Hot-leg LOCA – double-ended 12-inch diameter break at the core side of the V507A hot-leg isolation valve.
3. Cold-leg LOCA – 8-inch diameter breach in the 8-inch cold-leg piping at the flow control diaphragm valve, V540 in Figure 5.1.
4. Cold-leg LOCA – 2-inch diameter breach in the 2-inch cold-leg piping that connects the pressurizer to the 8-inch cold-leg piping.

Because all of these accidents are initiated by a depressurization of the primary loop, the automatic actions taken by the plant protection system are the same in each instance. A reactor scram is promptly initiated, the primary pumps are tripped, the two parallel redundant anti-siphon valves (V543 in Figure 5.1) are opened, the two primary loop isolation valves (V507A and V507B) are closed, the two parallel redundant valves to the in-pool heat exchanger (V546 in Figure 5.1) are opened, and the pressurizer isolation valve V527C (V527 in Figure 5.1) is closed. The scram rapidly reduces the reactor power to decay heat levels, while the closing of the V507A, V507B, and V527C valves is designed to minimize the loss of coolant from the plant.

The anti-siphon system contains compressed air that is injected into the flanged closed natural convection inlet pipe extending upward from the top of the hot leg inverted loop by opening the 543 valves, V543 in Figure 5.1. In the event of a hot-leg break immediately upstream of the V507A valve, this compressed air is designed to evacuate the down-flow portion of the hot leg leading to the V507A valve and prevent the siphoning of water from the reactor vessel. Similarly, the V502 check valve (V502 Check Valve in Figure 5.1) on the primary inlet pipe riser is designed to limit siphoning from the cold-leg side of the reactor vessel.

The in-pool heat exchanger is designed to facilitate decay heat removal from the core to the pool in a loss-of-flow accident when the V507A and V507B isolation valves are closed. The system is designed so that when these isolation valves are closed and the in-pool heat exchanger valves are open, water can circulate by natural circulation between the core and this heat exchanger. After the flow coasts down, the buoyancy due to core heat causes the core flow direction to reverse. Heated coolant from the core travels upward and is cooled in the in-pool heat exchanger, which is immersed in the pool, and then travels downward and returns to bottom of the core via the reactor normal outlet plenum. However, the air injected into the primary loop by the opening of the anti-siphon valves during a LOCA and water vapor (until it condenses) can collect at the top of the in-pool heat exchanger and severely impede or stop the natural circulation of water between the core and the in-pool heat exchanger. Thus, the long-term decay heat removal in the LOCA is largely through the reactor pressure vessel walls. The in-pool heat exchanger is more useful during LOFA and during normal reactor shutdowns.

Heat removal through the pressure vessel walls is enhanced by forced pool flow over the outer surfaces of the reactor vessel, specifically in the island tube at the core center and in the control blade channels that surround the core. This flow is driven by the pool pump, which is not tripped as a result of a LOCA. The control blade channels do not extend significantly above the core. Hence, virtually the entire outer convex surface of the reactor vessel above the core is cooled by free convection from the wall surface to the pool water. This free convection can be very effective in transferring decay heat to the pool water. Also, when there is no net flow through the reactor core, recirculation among the core coolant channels, with upward flow in the warmer channels and downward flow in the cooler channels, may substantially facilitate heat removal.

During normal operation the 546 valves at the top of the in-pool heat exchanger are closed and stop the flow through this heat exchanger, which is shown in red in Figure 5.1. Then the primary flow follows the path shown in black in the direction of the arrows.

5.3 Model

5.3.1 Model as Provided in the 2006 MURR SAR

The structure of the RELAP5 model used to represent the MURR reactor with its HEU core is described in considerable detail in “Appendix C, Thermal-Hydraulic Transient Analysis” of the 2006 MURR SAR. This model was the starting point for the Phase I modeling effort and was extended further during the current Phase II effort. Thus, the current models are improved versions of the MURR SAR model. There are HEU and LEU core versions.

Fuel failure can be caused by the cladding/fuel meat temperature exceeding the fuel plate blister limit. So the potential for fuel damage is conservatively assessed by predicting the peak fuel plate centerline temperature. As indicated earlier, for the HEU core all 24 fuel plates and 25 coolant channels of the highest power element are explicitly modeled. Each fuel plate is divided into four axial nodes. The first and last node are each 7.75 inches long and the middle two nodes each are 5.0 inches long, for a total plate length of 25.5 inches. The fuel meat and its two cladding layers, one on each side, are explicitly represented in each node. Thus, in the analysis $4 \times 24 = 96$ fuel centerline temperatures are predicted as a function of time for each transient analyzed. In this model the geometry of the 0.75 inches of unfueled length at either end of the fuel plate is lumped in with the first and last nodes of the fuel plate and the power generated in the top and bottom nodes are each smeared over 7.75 inches instead of 7.00 inches. This conserves the total power generated in the fuel plate but reduces the heat flux in these two nodes to 90.3%. The middle two nodes have higher heat flux, with the third node being the highest. All of the power of each fuel plate is distributed among the four axial nodes of the plate. Obviously, this model with only four nodes per plate cannot explicitly represent variations in power along the azimuthal direction of the curved fuel plates. However, as covered in more detail in Section 5.8.4 and shown in Figure 5.29, the peak fuel temperatures obtained with vertical one-inch mesh intervals are only slightly above those obtained from the RELAP5 model, where five one-inch nodes are combined to form the third node in the RELAP5 model. The four RELAP5 fuel plate mesh intervals were selected to achieve this.

5.3.2 Recent Modifications to the MURR RELAP5 Model

5.3.2.1 *Primary Loop Piping Model and Minor Corrections and Improvements*

The entire primary path in the MURR RELAP5 model was reviewed to ensure that the piping diameters, lengths, elevations, and flow areas are appropriately represented. The values used in the SAR RELAP5 model were used to create earlier versions of Figure 5.1 and Figure 5.2. Then the values in the figures were compared with those in the plant drawings and, in some instances, with the plant itself. Corrections to Figure 5.1 and Figure 5.2 were made where needed. These figures represent what is in the model. Modeling practicality required some deviations and approximations relative to the plant, which were chosen judiciously. For example, the elevation of the valve of the anti-siphon system, V543 in Figure 5.1, is 2.00 inches lower than in the plant. It is connected to the middle of branch 406, as shown in Figure 5.2. This branch is a single node in the model that is 2.00 feet long. Connections can be made only at the ends or the midpoints of the nodes. In the SAR RELAP5 model the valve connection was at the top of the node, which in Figure 5.1 would be 10 inches too high. Locating the connection in the middle of the node reduces the positioning error to just 2.00 inches too low.

The elevation of the core centerline is 606'-2" above sea level, as indicated in the upper right of Figure 5.1. At Sandborn Field on the Columbia Campus, with an elevation around 710 feet, the average atmospheric pressure during 2004 through 2013 was 14.71 psia. The lowest daily average atmospheric pressure recorded over this time period was 14.3 psia. Therefore, this, rather than the standard atmospheric pressure of about 14.7 psia, is taken as atmospheric pressure in the modeling.

Figure 5.2. Primary System Pipe Sizes in the MURR RELAP5 Model.

In Figure 5.1 and Figure 5.2 the elevations are represented to scale, but the piping lengths, in general, are not to scale. In Figure 5.2 a form of shorthand was used to represent the dimensions of each piping path. For example, alongside the core outlet riser pipe, the piping dimension is given as “100(2-3), 12” diam., 9.834’”. The 100 is the RELAP5 component (pipe) number. The “(2-3)” implies that these are nodes 2 and 3 of RELAP5 component 100. The “12” diam.” implies that this is a 12-inch diameter pipe. All of the pipes in the primary loop are Schedule 40 pipes unless otherwise noted. Therefore, one can deduce from a table of pipe schedules that this pipe has an outside diameter of 12.75 inches and a wall thickness of 0.406 inches. Hence, the inside diameter and the flow area can be calculated. The “9.834’” in the dimension implies that the length of the two nodes taken together is 9.834 feet long. If the change in elevation is not given in the figure, the component is assumed to be a vertical or horizontal. When this is not the case, the elevation change is the “ Δh ” dimension. For example, the reactor outlet plenum has the label “501 (br), 0.6048 ft², 6.437’, $\Delta h = -5.937$ ’”. “501” is the component number in the RELAP5 model. The “(br)” implies that it is a branch component in RELAP5. (A branch in RELAP5 is a single volume in RELAP5 with up to nine built-in junction points for connecting

other volumes to the branch.) The “0.6048 ft²” is the flow area. “6.437” is the length of the node that is used for calculating the volume of the node. The elevation change in the normal flow direction is 5.937 feet downwards. The magnitude of the elevation change is less than the volume length because in the plant the inlet pipe is below the top of the volume. Contrary to the figure, in the RELAP5 model the inlet pipe is connected to the top of the branch rather than at 0.500 feet below the top. However, in the model the change in elevation is only 5.937 feet. Thus, in the model the elevation change and the nodal volume are correctly represented, but the flow traverses the entire length of the volume rather than only the lower 5.937 feet of it. This slight modeling approximation is deemed minor and in line with other modeling approximations.

In Figure 5.2, the point where one piping segment ends and an adjacent one begins is sometimes marked by a black or blue line. A line to mark the transition is not used where there is an obvious change in direction. There are only four blue marker lines and they are thicker than the black ones. These are to indicate where one path in the plant splits into two or two merge into one. Hence, there is a blue line upstream of the pump to note the transition from a 12-inch pipe to two 8-inch pipes. The pipe dimension “2 × 8” diam.” signifies two Schedule 40 8-inch pipes in parallel. There is another blue line where the two pump paths merge, followed by another where the path in the plant splits for the two parallel heat exchanger paths. The fourth blue line is after the valves that are downstream of the heat exchangers. Of course, there is only one pressurizer in the plant, which is represented as a fixed pressure source in the model and is connected to only one of two parallel paths, Loop A in the plant. The horizontal path downstream of the V507A valve, which is interrupted by the V507B valve in the figure, is a total of 31.832 feet in length, as indicated in the figure.

In the RELAP5 model the heat transfer between the pool and the components immersed in it is explicitly represented. The reactor vessel dimensions and heat transfer areas were reviewed and corrected as needed. These areas affect the prediction of the long-term cooling of the reactor vessel, but do not affect the peak fuel temperatures in the most severe LOCAs, which are achieved within a few seconds of the start of the upset.

The corrections in piping dimension and elevation and the corrections in reactor vessel heat transfer area are judged to not have a noticeable effect on the peak fuel temperatures.

An aesthetic improvement to the RELAP5 model is that the fuel plates in the core were represented as being curved. In the 2006 SAR models and the Phase I models they were approximated as flat plates. Since the fuel plates are thin relative to their radii of curvature, the effect on the temperature results should be very small. In Phase I it was incorrectly concluded that curved heat structures that are not 360° arcs could not be represented. Later it was realized that in representing the heat transfer area of a solid structure, a 180° arc, for example, could be represented as a 360° one that is half as long.

In Phase I an error was introduced in the RELAP5 LOCA model of the LEU core in which the thickness of the aluminum part of the clad and the thickness of the fuel meat were made twice as thick as they should have been. This has a small, but observable, effect on steady-state fuel temperatures, but could have a very noticeable effect on transient fuel temperatures since heat capacitance is important in transients. A larger heat capacitance produces lower transient temperature rises. The error was corrected in the current LEU model. Additional verification

measures were undertaken. As a spot check, the fuel plate thickness for fuel plates 2 and 23 were deduced from the RELAP5 output to make sure that all of the layers, including the oxide layer, have the desired assumed thicknesses for this analysis. Also, the initial steady-state temperatures of one axial node of each of two fuel plates were hand calculated and compared with the RELAP5 output. Lastly, as described in Section 5.8.4, the steady-state fuel centerline temperature in each fuel plate of a single LEU element was compared with a prediction made with another computer code.

5.3.2.2 *RELAP5 Reactor Trip and Plant Protection System Model*

The choice of an HEU or LEU core does not affect the configuration of the plant protection system. A systematic review of the MURR plant protection system as it affects the RELAP5 models under consideration was performed. The current trip values for pressure and pressure drop will change with an LEU core. The six trips of concern for operation with the current HEU core are as follows:

- Trip 1 – core outlet pressure (PT-944A & PT-944B) ≤ 26.0 psig
- Trips 2 and 3 – primary flow (FT-912A&E, FT-912G&H) $\leq$ limiting safety system setting (LSSS) flow. (The LSSS flow for the HEU core is 3200 gpm of water at 155 °F. The LSSS flow for the LEU core is 3300 gpm of water at 145 °F.) The actual plant trips are set conservatively slightly higher than the LSSS conditions.
- Trip 4 – pressure at the junction of the two primary heat exchanger loops (PT-943) ≤ 57.5 psig
- Trip 5 – pressure drop between valves V507B and V507A (DPS-929) ≤ 24.8 psia
- Trip 7 – core outlet isolation valve V507A starts to close
- Trip 8 – core inlet isolation valve V507B starts to close

The trip numbers are how they are labeled in the RELAP5 model and the instrument numbers correspond to how they are labeled in the MURR facility. The first four trips are indicated in blue near the bottom of Figure 5.1. More specific details, including delay times, are provided for these six trips in Figure 5.3 through Figure 5.8, respectively. A concerted effort was undertaken to obtain accurate upper limits for delay times. In these six figures the value to the left of the “[” symbol is the additional delay time prior to taking the action to the right of the “[”. For example, in Trip 1 when the core pressure drop drops to 26.0 psig, there is a 0.8 s delay to de-energize the relays 2K13 and 2K28. There is a 0.0 s delay after the opening of these relays, before a scram signal is sent. Then after a 0.075 s delay the blades start to drop. All of the actions listed in the six trips are the start of the action. The V507A and V507B isolation valves, for example, require a few seconds before they start closing and take a few additional seconds to go from fully open to fully close.

Trip 6 is based on the liquid level in the pressurizer, which is not included in the above list because the liquid level in the pressurizer is not part of the RELAP5 model. This trip closes valve V527C, labeled V527 in Figure 5.1, which isolates the pressurizer from the main coolant line. This valve is also closed by either of the primary coolant isolation valves starting to close--Trips 7 and 8. In both the MURR SAR RELAP5 model and in the current models, the timing of the closing of the V527C valve is important because the pressurizer is treated as a constant

Figure 5.3. Trip 1: Core Outlet Pressure.

Figure 5.4. Trips 2 and 3: Primary Flow.

Figure 5.5. Trip 4: Core Inlet Pressure.

*In the RELAP5 model this trip is not active during the hot-leg LOCA.

Figure 5.6. Trip 5: Core Pressure Drop.

Figure 5.7. Trip 7: Core Outlet Isolation Valve Starts to Close.

Figure 5.8. Trip 8: Core Inlet Isolation Valve Starts to Close.

pressure source rather than a volume that loses pressure as the transient progresses. Thus, if this valve is not closed in a timely manner, inappropriately higher pressures can be predicted for the core, which, in turn, affects the temperatures predicted in the core. The decreasing pressurizer level and pressure are not modeled. The V527C valve closes when either the V507A or the V507B isolation valve starts to close. Trip 1 closes the V507A and V507B valves, which, in turn, closes the V527C valve via Trip 7 or Trip 8. The effect on the peak fuel temperature for the most severe accident, the double-ended pipe break at the inlet isolation valve, should be insignificant because in this accident the peak fuel temperature is reached within 0.34 s.

The most effective way to limit the peak fuel temperature is to minimize the time it takes from the start of the event until the control blades start to drop. The trip sequence that moves the control blades the quickest during a cold-leg LOCA is Trip 5. The combination of 0.25 s to de-energize the relay, 0.0 s to send the signal to drop the blades, and 0.075 s before the blades start to move, implies that the plant protection system internal built-in scram delay is 0.325 s. Since the pressure drop across the core drops to the trip point within a few ten-thousands of a second after the cold-leg pipe ruptures, this trip is the one that causes the scram. In the hot-leg LOCA, the break at the outlet should temporarily increase the pressure drop across the core and thereby delay the activation of Trip 5. However, because of what may be an artifact of the simulation, there may be a temporary dip or oscillation in pressure drop across the core which causes the core pressure drop to reach the trip setting. Therefore, the trip logic in the model is constructed so that whenever there is a pipe break upstream of the V507A valve, Trip 5 is rendered inactive.

In the current RELAP5 models for the HEU and LEU cores, all of the trips on the above bullet list are explicitly represented in the model. The quicker response time of Trip 5 compared to Trips 1 and 4 is due the vintage of the electronic components. Updating the components associated with Trips 1 and 4 is being evaluated, so that the peak fuel temperatures may be reduced in re-analysis after possible equipment replacement.

During startup LEU physics measurements, the core outlet and inlet pressures and differential pressure will be determined. New trip values for Trips 1, 4, and 5 will be determined based on these measured values and the LEU primary coolant core inlet LSSS flow rate of 3300 gpm. For example, the pressure drop trip value of Trip 5 may not be 24.8 psi. However, this will have no noticeable effect on the results that RELAP5 would predict for the most severe LOCAs. The RELAP5 model results for the most severe LOCAs show that it takes no more than about 0.0003 s for the pressure or pressure drop to reach the pressure or pressure drop that causes the scram trip point to be reached. The scram for the double-ended break cold-leg LOCA is initiated by Trip 5, which has a 0.325 s delay until the control blades start dropping, as indicated by Figure 5.6. The RELAP5 modeling of the actual scram is predicted to start at no later than 0.3253 s for the HEU core or the LEU core. Further details are provided at the end of Section 5.7.1. Similarly, the scram for the hot-leg LOCA is initiated by Trip 1, which has a total delay until the control blades start dropping of 0.875 s, as indicated by Figure 5.3. The RELAP5 modeling of the actual scram is predicted to start at 0.8753 s for both the HEU and the LEU core. Thus, an adjustment of even several psi in the trip value would have essentially no effect on the predicted peak temperatures for these LOCAs.

5.3.2.3 *Improved Core Channel Hydraulic Diameters, Laminar-Flow Friction Shape Factors, and Relative Roughness*

The original MURR RELAP5 models used RELAP5 default values for the hydraulic diameters of the core channels and the laminar-flow friction shape factors. The RELAP5 default is that the hydraulic diameter is the diameter of a circular duct of the same flow area. In thin rectangular channels of very small aspect ratio, the hydraulic diameter is approximately twice the channel thickness, which can be far from the default value. Therefore, for the Phase II analyses of the HEU and the LEU core, new hydraulic diameters were explicitly calculated and used in the models. These were calculated as four times the flow area divided by the wetted perimeter of the channel. The original model made all of the hydraulic diameters too large, with the effect getting more pronounced going from the inner vessel wall to the outer vessel wall as the channel flow area increased. Making the hydraulic diameter of a particular channel larger reduces the hydraulic resistance of that channel and thereby incorrectly increases its velocity. At the initial steady-state condition, where the total flow is fixed, the net effect is to redistribute flow away from the smaller-area channels near the inner vessel wall and into the larger-area channels near the outer vessel wall. Since the limiting channels predicted in the MURR SAR are the ones near the inner vessel wall, the error would tend to cause higher peak fuel centerline temperatures to be predicted.

For laminar flow, the Moody friction factor, f , is given by $f = C/Re$, where Re is Reynolds number and C is a constant. The RELAP5 default value for C is 64 because it is based on the assumption that the duct is circular. For rectangular ducts, Kakac et al. [26] propose a reasonably close approximation for C as 4 times the value of C_{Fanning} given by Equation 3.158 on page 3.46 of Reference 26:

$$C_{\text{Fanning}} = 24 \times (1 - 1.3553 a + 1.9467 a^2 - 1.7012 a^3 + 0.9564 a^4 - 0.2537 a^5), \quad (5.1)$$

where a is the duct aspect ratio. The factor of 4 in the calculation of C is needed for the RELAP5 application because the equation in the reference is for the Fanning friction factor, which is $1/4^{\text{th}}$ of the Moody friction factor. The RELAP5 code assumes that f for laminar flow is given by $64/(Re \Phi_s)$, where Φ_s is an input shape value, whose default value is 1.0. Therefore $\Phi_s = 64/C$. For both the HEU and LEU cores a value of Φ_s for each channel was provided for each channel of each fuel element. Although its variation from channel to channel and with burnup was explicitly represented in the RELAP5 models, in all instances the value was observed to always be close to 0.7.

The maximum allowed absolute roughness for the MURR fuel plates, based on existing MURR drawings, is 63×10^{-6} inches, or 5.25×10^{-6} feet. The value used in the original model is 10×10^{-6} feet. Although this value is about twice as large as it should be, the effect on the core flows, and more importantly, on the peak fuel temperature, should be very small. In the current RELAP5 models of the HEU and LEU cores, the absolute roughness is set to 63×10^{-6} inches.

The flow area and hydraulic diameter of each fuel coolant channel in the RELAP5 models was explicitly represented. The narrowing of channels due to burnup was also included in these representations. The flow area of each channel in the MURR HEU or LEU core is calculated as

the product of the channel arc length and the channel thickness. The channel arc length is calculated at the average of the two bounding radii and is the 45° arc length diminished by 0.340 inches. This 0.340 inches accounts for the arc length occupied by the two 0.150-inch thick side plates and the 0.040 inches between the side plates of adjacent elements.

5.3.2.4 *Core Heat Transfer Areas and Nodal Structure*

As described above, each fuel plate in the HEU core is divided into four axial nodes in the RELAP5 model and is modeled as an arc rather than approximated as a flat plate. The LEU plates were modeled in the same manner. For both fuel types the aluminum clad and the fuel meat are explicitly represented as distinct layers. For the LEU fuel the nominal 1-mil thick zirconium interlayer between the fuel meat and 6061-Al clad is explicitly represented. The radii of the fresh HEU and LEU fuel plates used in the RELAP5 models are provided in Table 5.1. Additionally, for both fuel types the oxidation layer on each surface of the burned fuel plate is explicitly represented as a distinct layer. The oxide layer thicknesses used in the analysis are the values shown in Table 4.4 for the convex surface of HEU plate 1 or concave surface of LEU plate 23. The heat transfer areas of the plates are calculated to be at the oxide-water interfaces.

Table 5.1. Inner and Outer Radii of Fresh Fuel Plates, inches.

Plate	HEU		LEU	
	Inner	Outer	Inner	Outer
1	2.7700	2.8200	2.7555	2.7995
2	2.9000	2.9500	2.8925	2.9365
3	3.0300	3.0800	3.0295	3.0735
4	3.1600	3.2100	3.1665	3.2105
5	3.2900	3.3400	3.3035	3.3475
6	3.4200	3.4700	3.4395	3.4835
7	3.5500	3.6000	3.5755	3.6195
8	3.6800	3.7300	3.7115	3.7555
9	3.8100	3.8600	3.8475	3.8915
10	3.9400	3.9900	3.9835	4.0275
11	4.0700	4.1200	4.1195	4.1635
12	4.2000	4.2500	4.2555	4.2995
13	4.3300	4.3800	4.3915	4.4355
14	4.4600	4.5100	4.5275	4.5715
15	4.5900	4.6400	4.6635	4.7075
16	4.7200	4.7700	4.7995	4.8435
17	4.8500	4.9000	4.9355	4.9795
18	4.9800	5.0300	5.0715	5.1155
19	5.1100	5.1600	5.2075	5.2515
20	5.2400	5.2900	5.3445	5.3885
21	5.3700	5.4200	5.4815	5.5255
22	5.5000	5.5500	5.6185	5.6625
23	5.6300	5.6800	5.7555	5.8045
24	5.7600	5.8100		

The arc length of the fuel meat is the 45° arc length at the centerline radius of the fuel meat reduced by 0.492 inches. This distance provides room for two 0.150-inch thick side plates and 0.040 inches between the side plates of adjacent elements. Where the fuel plate is inserted into each grooved side plate there is an unfueled region between the edge of the side plate and the edge of the fuel meat, which is nominally 0.070 inches. For practical assembly tolerances, the current HEU aluminide fuel elements require that the fuel plate arc length be slightly smaller than the arc length between the bottom of the groove of one side plate and the bottom of the groove of the corresponding opposite side plate. Therefore, for both fuel types the nominal fuel plate arc length is reduced by an additional 0.012 inches, and this is taken as a reduction in the fuel-meat portion of the plate.. (This can be thought of as a 0.006-inch gap between the edge of the fuel plate and the bottom of the groove for each side plate.) Combining these dimensions yields $2 \times 0.150 + 0.040 + 2 \times 0.070 + 0.012 = 0.492$ inches.

For the RELAP5 input, the heat transfer area at each surface of each fuel node is calculated as $2 \times \pi \times r \times L \times F$, where, r is the radius at the surface (water-oxide or water-aluminum interface), L is the node length (7.75 or 5.00 inches), and F is the ratio of the plate arc length to the 360° arc length. F is taken to be the ratio $(\pi/4 \times r_{\text{avg}} - 0.492 \text{ inches}) / (2\pi \times r_{\text{avg}})$, where r_{avg} is the average of the plate inner and outer radii. Technically, r should be used in place of r_{avg} in the calculation of F . However, the difference in the resultant heat transfer area is virtually insignificant. Having only one value of F for both surfaces of each plate, regardless of oxide thickness, reduces the opportunity for input error. In the RELAP5 input for the fuel node heat transfer areas, values of $L \times F$ were provided.

5.3.2.5 *Secondary Sides Were Added to Primary and Pool Heat Exchangers*

In the original MURR RELAP5 model the flows on the secondary sides of the primary and pool heat exchangers were not modeled. Instead the heat removal rate from each of these two heat exchangers was specified as functions of time to obtain the desired coolant temperatures. These functions had to be properly set by a trial-and-error method at the initial steady-state conditions so the heat removal rate from each loop was correct. Each function reduced the heat removal rate to relatively low values over the first 5 s of the transient so that excessive heat removal would not follow the scram. The desired temperature of the coolant in the cold leg of each loop during the initial steady-state condition was approximately achieved via a bounding node in each loop where temperature and pressure were set. In the RELAP5 code this node is referred to as a “time-dependent volume.” Although this approach is quite adequate, a different approach, which was fairly easy to implement, was used. This approach is to include a secondary side to each heat exchanger. For both heat exchangers a counter-flow heat exchanger was modeled in which the initial steady-state flow on the secondary side was approximately equal to that on the primary side. The secondary flows were reduced to relatively low values over the first 5 seconds of the transient. A trial-and-error method was used to obtain the inlet temperatures to the secondary sides of the heat exchangers that produced the desired cold-leg temperatures for the primary and pool loops, which in the RELAP5 models were observed at the first primary-side node after the heat exchanger. RELAP5 was executed multiple times to calibrate the steady-state solution by adjusting the secondary side inlet temperatures (nodes 910 and 751 of the heat exchangers) until the desired cold-leg loop temperatures were obtained (node 1 of pipe 115 and node 5 of pipe 700).

5.3.2.6 *Fuel Thermal Properties*

All of the thermal properties in the RELAP5 input were represented in tabular form. The HEU and LEU fuel meat thermal properties are as provided in Chapter 2. Equation 2.1 was evaluated to obtain tabular values of the thermal conductivity for fresh LEU fuel meat. For burned LEU fuel meat the fresh values were multiplied by 0.78, 0.75, or 0.65 depending on whether the burnup was 77, 96, or 180 MWd, respectively. The tabular values of aluminum-6061 thermal conductivity and heat capacity used in the RELAP5 analysis are based on values read directly from graphs provided in Reference 11. Tabular values of zirconium thermal conductivity were obtained by evaluating Equations 2.6 and 2.7. The oxide thermal conductivity and heat capacity are the same as those used in the PARET/ANL reactivity insertion analysis (See Section 2.5).

5.3.2.7 *LEU and HEU Core One-Element Models*

Figure 5.9 shows the nodal structure of the RELAP5 model of the HEU core, which is the same as that used in the SAR RELAP5 analysis. The figure includes the upper plenum, branch 501, and the lower plenum, branch 575, of Figure 5.1 and Figure 5.2. The blue areas are coolant. The pink areas are fuel plates. The yellow areas are the aluminum inner and outer reactor vessel walls. The concave side of the inner vessel wall is cooled by the coolant flow through the flux trap. This flux trap coolant flow is shown as a dotted blue region. Similarly, the coolant flow that cools the convex surface of the outer vessel wall is also shown as a dotted blue region. The upper part of this region is the reactor pool, the middle part is the control blade channels, and the bottom part is the reactor tank, as indicated in Figure 5.9.

Figure C.4 of Appendix C of the MURR SAR provides a nodal diagram of the HEU element that is similar to Figure 5.9. A notable difference between the two nodal diagrams is that all of the fuel plate heat structure numbers in Figure 5.9 are ten times greater. Thus, 601, 602, 603, etc. in the SAR figure are replaced by 6010, 6020, 6030, etc. in Figure 5.9. The Figure 5.9 nodal numbers are what are actually in both the SAR and the current RELAP5 models. The three-digit numbers are an editorial convenience in the SAR text and can be a source of confusion in understanding the RELAP5 model.

The nodal structure shown in Figure 5.10 for the LEU core is the same as that for the HEU core, except that the 24th fuel plate and the 25th coolant channel have been eliminated. The 24th coolant channel rather than the 25th is bounded by the outer reactor vessel wall.

The nominal fuel plate, clad, fuel meat, and coolant channel thickness for all 23 plates and 24 coolant channels of the LEU core CD35 are tabulated in Table 1.1 of Reference 1. The clad thicknesses given in the table include the 0.001 inches of zirconium that is between the aluminum cladding layer and the fuel meat. For example, plate 1 is shown as having 17.5 mils (0.0175 inches) of clad. This corresponds to a 0.001-inch (1-mil) thickness of zirconium and a 0.0165-inch layer of aluminum cladding on each side of the fuel meat.

plate	channel	Pool	Control Blade Channels				Reflector Tank	channel	plate
		702	715(2)	715(3)	715(4)	715(5)	720		
		5011	5243	5253	5263	5273	5751		
	25	UPPER PLENUM (501)	526(1)	526(2)	526(3)	526(4)	LOWER PLENUM (575)	25	
	24		6240	6490	6740	6990		24	
	23		525(1)	525(2)	525(3)	525(4)		23	
	22		6230	6480	6730	6980		22	
	21		524(1)	524(2)	524(3)	524(4)		21	
	20		6220	6470	6720	6970		20	
	19		523(1)	523(2)	523(3)	523(4)		19	
	18		6210	6460	6710	6960		18	
	17		522(1)	522(2)	522(3)	522(4)		17	
	16		6200	6450	6700	6950		16	
	15		521(1)	521(2)	521(3)	521(4)		15	
	14		6190	6440	6690	6940		14	
	13		520(1)	520(2)	520(3)	520(4)		13	
	12		6180	6430	6680	6930		12	
	11		519(1)	519(2)	519(3)	519(4)		11	
	10		6170	6420	6670	6920		10	
	9		518(1)	518(2)	518(3)	518(4)		9	
	8		6160	6410	6660	6910		8	
	7		517(1)	517(2)	517(3)	517(4)		7	
	6		6150	6400	6650	6900		6	
	5		516(1)	516(2)	516(3)	516(4)		5	
	4		6140	6390	6640	6890		4	
	3		515(1)	515(2)	515(3)	515(4)		3	
	2		6130	6380	6630	6880		2	
	1		514(1)	514(2)	514(3)	514(4)		1	
			6120	6370	6620	6870			
			513(1)	513(2)	513(3)	513(4)			
			6110	6360	6610	6860			
			512(1)	512(2)	512(3)	512(4)			
			6100	6350	6600	6850			
			511(1)	511(2)	511(3)	511(4)			
			6090	6340	6590	6840			
			510(1)	510(2)	510(3)	510(4)			
			6080	6330	6580	6830			
			509(1)	509(2)	509(3)	509(4)			
			6070	6320	6570	6820			
			508(1)	508(2)	508(3)	508(4)			
			6060	6310	6560	6810			
			507(1)	507(2)	507(3)	507(4)			
			6050	6300	6550	6800			
			506(1)	506(2)	506(3)	506(4)			
			6040	6290	6540	6790			
			505(1)	505(2)	505(3)	505(4)			
			6030	6280	6530	6780			
			504(1)	504(2)	504(3)	504(4)			
			6020	6270	6520	6770			
			503(1)	503(2)	503(3)	503(4)			
			6010	6260	6510	6760			
			502(1)	502(2)	502(3)	502(4)			
		5010	5020	5030	5040	5050	5750		
		705(1)	705(2)	705(3)	705(4)	705(5)	705(6)		
		Flux Trap							
plate	channel							channel	plate

Figure 5.9. HEU Reactor Core Nodal Structure Used in One-Element RELAP5 Model.

plate	channel	Pool	Control Blade Channels				Reflector Tank	channel	plate
		702	715(2)	715(3)	715(4)	715(5)	720		
		5011	5243	5253	5263	5273	5751		
		UPPER PLENUM (501)	525(1)	525(2)	525(3)	525(4)	LOWER PLENUM (575)		
			6230	6480	6730	6980			
			524(1)	524(2)	524(3)	524(4)			
			6220	6470	6720	6970			
			523(1)	523(2)	523(3)	523(4)			
			6210	6460	6710	6960			
			522(1)	522(2)	522(3)	522(4)			
			6200	6450	6700	6950			
			521(1)	521(2)	521(3)	521(4)			
			6190	6440	6690	6940			
			520(1)	520(2)	520(3)	520(4)			
			6180	6430	6680	6930			
			519(1)	519(2)	519(3)	519(4)			
			6170	6420	6670	6920			
			518(1)	518(2)	518(3)	518(4)			
			6160	6410	6660	6910			
			517(1)	517(2)	517(3)	517(4)			
			6150	6400	6650	6900			
			516(1)	516(2)	516(3)	516(4)			
			6140	6390	6640	6890			
			515(1)	515(2)	515(3)	515(4)			
			6130	6380	6630	6880			
			514(1)	514(2)	514(3)	514(4)			
			6120	6370	6620	6870			
			513(1)	513(2)	513(3)	513(4)			
			6110	6360	6610	6860			
			512(1)	512(2)	512(3)	512(4)			
			6100	6350	6600	6850			
			511(1)	511(2)	511(3)	511(4)			
			6090	6340	6590	6840			
			510(1)	510(2)	510(3)	510(4)			
			6080	6330	6580	6830			
			509(1)	509(2)	509(3)	509(4)			
			6070	6320	6570	6820			
			508(1)	508(2)	508(3)	508(4)			
			6060	6310	6560	6810			
			507(1)	507(2)	507(3)	507(4)			
			6050	6300	6550	6800			
			506(1)	506(2)	506(3)	506(4)			
			6040	6290	6540	6790			
			505(1)	505(2)	505(3)	505(4)			
			6030	6280	6530	6780			
			504(1)	504(2)	504(3)	504(4)			
			6020	6270	6520	6770			
			503(1)	503(2)	503(3)	503(4)			
			6010	6260	6510	6760			
			502(1)	502(2)	502(3)	502(4)			
		5010	5020	5030	5040	5050	5750		
		705(1)	705(2)	705(3)	705(4)	705(5)	705(6)		
		Flux Trap							
channel	plate							channel	plate

Figure 5.10. LEU Reactor core Nodal Structure Used in One-Element RELAP5 Model.

Since in the RELAP5 model each HEU cladding thickness was subdivided into three layers, adding the zirconium layer to the LEU model was relatively easy. For the LEU fuel plates, the layer of aluminum closest to the fuel foil was converted to zirconium and made 0.001 inches thick. The thicknesses of all of the aluminum and fuel foil layers were adjusted to correspond to the LEU CD35 values. All of the core coolant channel parameters, including flow area and hydraulic diameter, were also adjusted to correspond to those for the LEU CD35 core.

Table C-3 of the MURR SAR, which is repeated below as Table 5.2, provides the relative reactor power level as a function of time post trip, assuming the conservative 120 days full-power operation. The same power history and relative power level as a function of time was used in the current analysis.

5.3.2.8 *Modeling the Effects of Fuel Burnup*

The SAR RELAP5 analysis did not consider the effects of fuel burnup on the thermal hydraulic performance of the core. The current model does for both the HEU and LEU cores. Burnup causes 1) oxide to form on the fuel plate surfaces, 2) the fuel meat to swell, resulting in fuel plate swelling, and 3) the fuel thermal conductivity to decrease. The oxide growth and plate swelling restrict the coolant channels. In a core with elements of differing burnups, coolant mass flow rates are redistributed from the more burned elements to the less burned ones that have higher power peaking factors.

For the current analysis two types of core models were developed. The first is the one that was used in the SAR RELAP5 analysis where a single fuel element is scaled up to represent all eight elements of the core operating at 100% of the total core model power level. Each channel has eight times the flow area of that of its single channel counterpart and each fuel plate has eight times the fuel, heat transfer area, and power of its single element counterpart. This model has no mechanism to redistribute flow from more burned elements to less burned elements, when there is a difference in burnup between fuel elements.

In all of the MURR cores to be analyzed, there are two fresh elements, two EOL elements, two at one intermediate burnup, and two at another intermediate burnup. Therefore, a second type of core model, which was created only for the LEU core, was designed to be able to model the redistribution of flow from more burned elements to less burned elements. This “two-element model” has two elements modeled in parallel. One element in the model represents the assumed limiting element. A larger element in the model is scaled to represent the remaining seven reactor elements in the core. This can result in overly conservative predictions of the fuel temperatures in some cases. For example, when the two element model is used to study an end-of-life (EOL) element in a mixed-burnup core, too much flow is redistributed from the EOL element with restricted coolant channel gaps to the other element in the model which has the channel gap thicknesses of fresh fuel. This results in conservative fuel temperature predictions for the EOL element. This issue would have been avoided by appropriately sizing the flow area, hydraulic diameter, and the laminar flow friction factor value of Φ_5 (See Section 5.3.2.3) of each coolant channel of the larger element to include the effects of burnup in each of its constituent MURR elements. For example, all of the seven MURR element channel 1 flow areas that are

Table 5.2. Relative Power vs. Time after Scram.

Time, s	Relative Power
0	1
0.3	0.1792
0.7	0.1145
1	0.0943
2	0.0846
3	0.0771
5	0.0668
6	0.063
7	0.0598
8	0.0572
9	0.0549
10	0.0529
20	0.041
30	0.0373
40	0.0352
50	0.0336
60	0.0324
70	0.0314
80	0.0305
90	0.0298
100	0.0292
120	0.0281
180	0.0278
200	0.0274
240	0.0259
300	0.0242
400	0.0221
420	0.0218
540	0.0201
600	0.0195
800	0.0178
1000	0.0166
2000	0.0133
4000	0.0106

part of the large element in the model could be added together to determine the flow area of channel 1 of the large element in the two-element model. However, this viable approach was not taken.

Instead, a separate hydraulics model was developed and implemented using a spreadsheet to determine the appropriate core flow rate to use in conjunction with the one-element RELAP5 HEU and LEU models. These RELAP5 models were used to produce all of the results. Although a two-element model could be used to model the impacts of additional natural circulation, conservative results are expected based on the one-element modeling performed in

this work. The two-element model is described in more detail in Section 5.4. The separate hydraulics model used in the one-element model is described in Section 5.5.

5.3.2.9 *Unified Models*

The original MURR RELAP5 used a complete input file of approximately 8000 lines with a few necessary line revisions for each different transient analysis. In each of these files the steady-state solution was obtained via a 500-s pseudo transient, which was immediately followed by the transient itself. The file contained the entire model and the added volumes and valves needed to simulate the particular accident rupture that initiated the transient.

The current modeling puts all of the added volumes and valves needed to model all six potential breaks and represent all of the LOCAs that are to be considered into a single model. As described in Section 5.3.2.2, the revised plant protection system model includes all of the trips that are needed to model all of the LOCAs to be studied.

The RELAP5 code has a very useful feature that always the RELAP5 solution to be continued from where it left off with desired changes in the model going forward in time. The model and the completed part of the transient solution are saved in restart/plot file. This is a binary file that has a “.r” at the end of its name. The file to continue the transient is a text (or ASCII) file which requires a minimum of statements since the model is contained in the “.r” file. Typically, these text files contained about 40 statements, including many comment statements. This continuation feature was found to be extremely helpful. First a steady-state solution was found and saved in a “.r” file. Then a copy of the “.r” file was used as the starting point for each LOCA or LOFA to be studied.

RELAP5 obtains steady-state solutions by starting with initial guesses for all unknown variables in the model and executing a pseudo-transient solution in which all of the boundary conditions and forcing function are held constant. Therefore, steady-state solutions were obtained by executing a 1000-s steady-state with none of the pipe break valves opened. As explained in Section 5.3.2.5 a trial-and-error method was used to obtain the desired initial reactor coolant inlet temperature and desired initial pool coolant temperature. In this trial-and-error method, the inlet temperatures on the secondary sides of the primary and pool heat exchangers were adjusted until the desired primary inlet and pool temperatures were obtained. This trial-and-error method required solving the 1000-s steady-state several times. Perhaps a more elegant method to be used in the future would be to incorporate controllers in the model to perform the required heat exchanger secondary-side inlet temperature adjustments automatically so that the desired initial primary inlet and pool temperatures could be obtained in a single pseudo-transient solution rather than in multiple trial-and-error 1000-s steady-state solutions.

Each transient solution was obtained as a continuation of a 1000-s pseudo-transient steady-state solution. In the continuation solutions a 100-s null transient was simulated before any of the pipe break valves were opened. This was to guarantee and demonstrate that the accident simulation started from a steady-state condition before the LOCA initiator was activated.

The RELAP5 code allows the user to specify a delay time before a particular controller action is taken. In the model each of the six break valves, shown in green in Figure 5.1, are programmed to open instantaneously immediately after its delay time has expired. In the steady-state solution the delay times for all of these six valves were made extremely long so that none of these valves opened during the 1000-s pseudo-transient solution. In the RELAP5 text continuation files the delay times for the break valves that needed to open to simulate the particular LOCA were reset so that those break valves opened after 100 seconds into the restart solution and the others remained closed. In an analogous manner, a continuation of the 1000-s pseudo-transient solution was also used to obtain the LOFA solutions of Chapter 6. In their RELAP5 text continuation files all six break valve delay times were set to be very long and other specific forcing functions were activated at 100 s into the transient continuation solution.

After a unified model was developed for the HEU core, it was used to develop a unified model for the LEU CD35 core. Six unified models were made – one each in which the single fuel element represented in the model was either an HEU (beginning-of-life) BOL element, an HEU MOL (middle-of-life) element, an HEU (end-of-life) EOL element, an LEU BOL element, an LEU MOL element, or an LEU EOL element. The SNAP code [27] was used extensively in understanding and modifying the RELAP5 models. The SNAP code provides a visual nodal diagram of the RELAP5 model and has menus that allow the values of the parameters used in the model to be easily found and changed.

All of the RELAP5 results produced for this report were generated with RELAP5/MOD3.3, Patch 03 [17], which was distributed for the US NRC by Information Systems Laboratories of Rockville, MD and received in 2010. The code was executed in batch mode from a PC command window on a PC with a Windows 7 operating system.

5.3.2.10 *Improved In-pool Heat Exchanger Model*

The in-pool heat exchanger consists of ten vertical finned aluminum tubes that are approximately 5 feet long. Each tube has 14 internal vertical fins and 28 external vertical fins. The ten tubes are connected in parallel by two 6-inch diameter Schedule 40 curved header pipes, which are located in two parallel horizontal planes. The tubes are arranged along the header pipes in a 90° arc with a radius of curvature of 3.5 feet. Figure 5.11 provides data used by earlier investigators in modeling the in-pool heat exchanger and some of their derived quantities, such as areas and film coefficients. The dimensional data was used in the current analysis. In the RELAP5 model, the ten parallel tubes were represented by a single tube that was scaled to represent all ten tubes. The fins were included by increasing the heat transfer areas in the model. Figure 5.12 shows the RELAP5 model of the circulation loop between the reactor core and the in-pool heat exchanger. This figure adds details that could not fit in Figure 5.2. In Figure 5.12 the two horizontal header pipes and the vertical finned tube are shown as red rectangles.

Since the header pipes for the in-pool heat exchanger are 6-inch Schedule 40 pipes, the outside diameter is 6.625 inches, the wall thickness is 0.280 inches, and the inside diameter is 6.065 inches. The top header is 7 feet long. The bottom header is 8 feet long. There is an additional 26 inches of 6-inch pipe that connects the lower header to the vertical 12-inch diameter

- Top horizontal header pipe:
 - Pipe center line is 5' 2.5" above the bottom header center line
 - Pipe diameter 6 inches
 - Curvature radius 3.5 ft
 - Length of horizontal curved pipe 7 ft
 - Distance from center line of 6" riser pipe going to valve 546 center line is 1"
- Bottom horizontal header pipe:
 - Pipe center line is 2' 6" above the center line of the 12" horizontal primary pipe going into the top of the pressure vessel
 - Pipe diameter 6 inches
 - Curvature radius 3.5 ft
 - Length of horizontal curved pipe 8 ft
 - 26" long horizontal straight piece of 6" pipe connects the 8' curved pipe to the 12" diameter vertical natural convection extension above the inverted loop
- Heat Exchanger tubes:
 - Tube OD 1.90 inches
 - Tube ID 1.712 inches
 - Number of tubes 10
 - Finned length 4.5 ft
 - External Fins:
 - Number 28
 - Thicknesses 0.05 inches
 - Height 0.5 inches
 - Internal Fins:
 - Number 14
 - Thicknesses 0.05 inches
 - Height 7 - 0.5 inches
7 - 0.375 inches
 - Internal Net Flow Area 0.01389 ft^2
 - Internal Equivalent Diameter 0.0377 ft^2
 - Total Outside Surface Area $2.831 \text{ ft}^2 / \text{ft}$.
 - For water at 180 °F with the finned tube properties the inside film coefficient is 396 BTU/hr - ft^2 - °F
 - The average value for the outside of the finned tube was 146.5 BTU/hr - ft^2 - °F
 - So the over-all heat transfer coefficient is 55 BTU/hr - ft^2 - °F

Figure 5.11. In-Pool Heat Exchanger Data Provided by Earlier Investigators.

Figure 5.12. RELAP5 Model of the Circulation Loop between the Reactor Core and the In-Pool Heat Exchanger.

extension of primary piping that extends up 2.5 feet above the inverted loop. In the heat exchanger, all of the flow approaches the first tube, and about a tenth of the flow goes into the first tube. Nine-tenths of the flow continues through the header to the second tube, and a tenth of the flow goes into that tube. Flow continues in a like manner to all ten tubes. The three-node model that exists in MURR SAR RELAP5 for the in-pool heat exchanger was transformed into a new one in which the first and last nodes are 6-inch horizontal pipes and the middle node represents the 10 vertical finned tubes.

An analogous situation exists at the lower (exit) header where the flows from each of the ten tubes combine and exit through the end of the header after the last tube in the flow path is reached. The flow behavior in the headers can only be approximated unless all ten vertical finned tubes are explicitly represented. As an approximation, since there is only one vertical tube in the model, each header was assumed to be half as long. Thus, the top 6-inch horizontal pipe was assumed to be 3.5 feet long and the bottom one was assumed to be 4 feet long plus the

additional 26 inches in length, for a total of $6'-2'' = 6.167'$. The flow areas of the two horizontal headers are each 0.2006 ft^2 .

Although the inside diameter of each finned tube is 1.712 inches, its hydraulic diameter is only 0.4528 inches. This is because the hydraulic diameter is defined as four times the flow area divided by the wetted perimeter. If there were no internal fins, the hydraulic diameter would be the inside diameter. However, the fins reduce the flow area and increase the wetted perimeter.

Hydraulic resistances are important in flows driven by natural convection. The form- or K-losses are indicted in blue in Figure 5.12. These losses can be a result of such things as elbows. Some of the K-loss values were taken from the SAR RELAP5 model. The RELAP5 code uses built-in form losses where abrupt changes in area are indicated in the code input. These are in addition to the K-losses shown in the figure. Some of the locations of the K-losses are designated by blue circles. There is a 7.02-inch orifice near the top of the riser pipe, which is shown in the figure between node 3 of pipe 100 and the bottom of branch 101. It is modeled by RELAP5's abrupt area change model.

The improved heat transfer due to the fins was taken into account in the RELAP5 model by multiplying the heat transfer area without fins by a factor of 2.00 for the inside area and by a factor of 4.00 for the outside area. These values are obtained from the added fin surface area and the heat transfer efficiency as discussed in the following six paragraphs. RELAP5 provides a parameter, which it calls a "fouling factor", which can be used to facilitate these heat transfer area increases. The heat transfer area and the film coefficient appear together as a product in the RELAP5 model. This product is multiplied by the RELAP5 fouling factor. When no value of fouling factor is provided in the input, the default value of 1.0 is used by the code. The values of the fouling factors were produced by performing an analysis of the fins.

A performance factor for a fin, called the efficiency of a fin, η , is defined to be the heat transfer rate from the fin divided by the heat rate if the fin were uniformly at the temperature at the base of the fin, T_b . If the temperature of the surface of a structure, such as fin, were uniformly at temperature T_b , then by definition the rate of heat transfer from the surface to an adjacent coolant with bulk temperature T_a would be $h \times A \times (T_b - T_a)$, where h is the film coefficient and A is the area of the surface. Pages 106 thru 109 of Rohsenow and Choi [28] derive a formula for η for a fin that has a uniform cross-sectional area, S , length L , perimeter P , thermal conductivity k , and uniform film coefficient h and for which the temperature of the fluid is taken to be T_a . This relationship is

$$\eta = \tanh(BL)/(BL), \quad (5.2)$$

where

$$B = [h P / (k S)]^{1/2}. \quad (5.3)$$

This fin efficiency can be used to estimate the effective heat transfer area of the fin, which is the actual surface area of the fin times the fin efficiency.

It is important to carefully define terms in calculating η . L is the distance from the base of the fin (located at the tube surface) to the tip of the fin, which is furthest from the tube surface. This distance is referred to as the fin height in Figure 5.11 and is 0.5 inches for the external fins and for half of the internal fins. The fin height is 0.375 inches for the other half of the internal fins. The cross-sectional area of each fin is a rectangle that is 4.5 feet by the 0.05-inch fin thickness. These dimensions must be used in determining S and P in Equation 5.3 for B . The thermal conductivity of the fins was taken to be 90 Btu/hr-ft-F, which is reasonably consistent with the 6061-Al thermal conductivity data obtained from Reference 11 described in Section 2.3.

The total effective heat transfer area on the inside of the tubes, $A_{\text{effective}}$, is the sum of 1) the effective heat transfer area of the fins on the inside of the tubes and 2) the bare tube peripheral area on the inside surfaces of the tubes that is not covered by internal fins. In the RELAP5 input this effective heat transfer area is represented by two input parameters, a fouling factor and a heat transfer area, A_{clean} , which is the heat transfer area of the inner surface of the tubes calculated as if the fins did not exist. The fouling factor was calculated as $A_{\text{effective}}/A_{\text{clean}}$. Thus, the only RELAP5 input parameter that is affected by the presence of the fins is the fouling factor. An analogous approach was taken to represent the total effective heat transfer area on the exterior of the tubes.

Because the 4.5-foot fin length covers all but about 2 inches of the length between the bottom of the upper and the top of the lower horizontal header, the tube length was taken to be 4.5 feet in calculating the total effective heat transfer areas. This simplifies the calculation and only slightly underestimates the required effective areas.

Values for h were determined from the RELAP5 output. This, however, posed a problem because the RELAP5 values for h depend on the unknown fouling factors. This problem can be solved by an iterative process. The in-pool heat exchanger performance is important for the long-term decay heat removal during an upset that does not trigger the opening of the anti-siphon valves and does not cause water vapor to be produced in the reactor vessel, such as a loss of flow due to a loss of site electric power. LOCAs, on the other hand, depressurize the primary system and cause air from the anti-siphon system and water vapor to collect in the in-pool heat exchanger, which impede the flow through the in-pool heat exchanger. Therefore, a loss-of-flow accident that is due to a loss of site power was chosen for determining the two required fouling factors. The particular case employed a one-element model with the LEU core represented by element 6 of Core 5B1. The initial reactor power was assumed to be 12 MW. Since element 6 in Core 5B1 has 8.91% more power than the average, the core power in the RELAP5 model was 8.91% greater. Core 5B1 was used because this analysis was performed before the final decision was made to use the reference mixed-burnup Core 7A for the LOCA analysis. The particular core used in the analysis is expected to have a minimal effect on the calculation of the fouling factor.

The values for h observed in the RELAP5 output vary with time over the 2500 s of the upset simulation. Higher values of h yield lower values of fin efficiency and fouling factor. Between about one and a half minutes to two minutes into the transient, values of h of about 415 and 124 Btu/hr-ft²-F were reached for inner and outer surfaces, respectively, and tended to gradually decline thereafter. Based on these values, fin efficiencies of 0.49 and 0.61 were calculated for the taller (0.50 inch) and shorter (0.375 inch) internal fins, respectively, and 0.74 was calculated for the external fins. These produced fouling factors of 2.1 and 4.2 for the inner and outer

surfaces, respectively. These values were rounded down to 2.0 and 4.0, respectively. The RELAP5 documentation is not clear as to whether or not the values of heat transfer coefficient in the output include the fouling factor. A hand calculation performed for a case with a fouling factor of 2.0 indicates that the output values do include the fouling factor. Thus, for the calculation of Equation 5.3, it is necessary for the RELAP5 output values of h to be scaled to remove the values of fouling factor assumed in the code input. It is also worth noting that the values of h used above, 415 and 124 Btu/hr-ft²-F, are in the range of the corresponding values of 396 and 146.5 Btu/hr-ft²-F provided near the bottom of Figure 5.11.

Although the in-pool heat exchanger headers are oriented horizontally, in the RELAP5 model the natural convection heat transfer coefficient used is the Churchill-Chu correlation for vertically oriented surfaces. This is because the single volume in the model that represents the pool, volume 702 in the RELAP5 model, is oriented vertically.

Although very unlikely, the only safety issue here is that the heat transfer coefficient correlation used for the headers in the RELAP5 simulation may be substantially optimistic. It is easy to demonstrate that this is not the case. Table 12.2 of Reference 26 recommends correlations for free convection from vertical surfaces and horizontal cylinders, among other situations. For vertical surfaces, this handbook recommends:

$$Nu_L = \left\{ 0.825 + \frac{0.387 Ra^{(1/6)}}{\left[1 + \left(\frac{0.492}{Pr} \right)^{(9/16)} \right]^{(8/27)}} \right\}^2 \quad (5.4)$$

and indicates that the applicable range is $10^{-1} < Ra < 10^{12}$, where Nu_L is the Nusselt number, Ra is the Rayleigh number, and Pr is the Prandtl number. This is the Churchill-Chu correlation used by RELAP5 for vertical surfaces. For horizontal cylinders, Table 12.2 of Reference 26 recommends another Churchill-Chu correlation, which has an applicable range of $10^{-5} < Ra < 10^{12}$ and is given by:

$$Nu_L = \left\{ 0.6 + \frac{0.387 Ra^{(1/6)}}{\left[1 + \left(\frac{0.559}{Pr} \right)^{(9/16)} \right]^{(8/27)}} \right\}^2 \quad (5.5)$$

For Equation 5.4 the characteristic length is the length of the vertical surface. For Equation 5.5 the characteristic length is the outer diameter of the pipe. Section 4.2.3.1.6 (p. 122) of Vol. IV of the RELAP5/MOD3.3 manual indicates that the properties are evaluated at the bulk fluid temperature. Spreadsheet calculations were made over a range of pool temperatures between 100 to 150° F. (100 to 120° would have been more than sufficient.) For surface to coolant temperature differences of 1° F, Equation 5.5 predicted a film coefficient that is 94% of that of Equation 5.4. When this temperature difference was increased to 10° F, this ratio increased from 94% to 95.5%. Thus, the use of Equation 5.4 in place of Equation 5.5 in RELAP5 is of very minor consequence.

5.3.2.11 *Corrected Pressure Vessel Heat Transfer Areas*

In the development of the two-element model, described in Section 5.4 below, it was necessary to apportion the inner and outer pressure vessel heat transfer areas so that one-eighth of these areas is allocated to the assumed limiting element and the other seven-eighths is allocated to the other element in the model. This prompted a review of all of the vessel heat transfer areas in the one-element model. In a LOCA simulation these areas affect only the long-term peak fuel temperatures, but not the absolute peak, which is achieved very early in the transient. Design drawings were consulted in the review to check and correct dimensions as needed. The values of heat transfer area provided in the RELAP5 code output were carefully compared with the corresponding heat transfer areas calculated separately via a spreadsheet. For the one-element model, these calculations are extremely simple and amount to taking the product of the circumference and the length of a cylinder, where the circumference is obtained by the product of π and the diameter. A quick inspection of the yellow regions of Figure 5.9 or Figure 5.10 makes it clear that there are 24 areas of concern here, six on each surface of each pressure vessel lateral surface. The six required lengths are those of the upper and lower plenum, which are lengths provided in Figure 5.2 (6.437 and 2.271 feet, respectively) and the 7.75 and 5.0 inch length used for the fuel heat structures. The inner diameter of the inner island tube (or inner pressure vessel) is taken to be 4.500 inches, except over the upper plenum, where it is taken to be 3.400 inches. The outer diameter of the island tube is taken to be 5.320 inches, except over the upper plenum, where 5.288 inches is used. The inner diameter of the outer vessel wall is taken to be 11.800 inches, except over the upper plenum, where 11.925 inches is used. The outer diameter of the outer vessel is taken to be 12.550 inches everywhere.

During the cold-leg LOCAs the long-term cooling of the core relies on heat transfer through the reactor vessel walls. The heat transfer through the outer wall of the upper plenum to the pool is of particular importance. Here, heat transfer on both sides of this wall is by natural convection. The natural convective length on the inner surface of this wall was taken to be the upper plenum elevation change of 5.937 feet, as shown in Figure 5.2. The General Electric design drawing 237E220 indicates that the top of the reactor vessel is at an elevation of 614' – 3". The same drawing indicates that the core centerline is at an elevation of 606' – 2". The top of the reflectors is 18 inches above the core centerline, which is an elevation of 607' – 8". Therefore, the top of the beryllium and graphite reflectors is 6-feet 7-inches below the bottom of the reactor pressure vessel head (the difference between 614' – 3" and 607' – 8"), and the natural convective length on the outer surface of the vessel was taken to be 6.58 feet.

5.3.2.12 *Vent Tank Model*

The need for a rudimentary vent tank model occurred during the analysis of a loss-of-flow accident. When a LOFA caused by a loss of site power was first analyzed, the primary system pressure was observed to gradually rise above 1,000 psia as the coolant temperature inside the reactor vessel rose, and to gradually decline as the coolant temperature subsequently declined. A careful inspection of Figure 5.1 led to the cause of this behavior. In this accident the two core main isolations valves, V507A and V507B, close and the anti-siphon valve does not open. The volume of fluid trapped on the core side between the two isolation valves has no vapor and does not have an adequate expansion volume, as would exist in the reactor. In the plant, the vent tank,

which is located near the top of the pool, provides this function. When the reactor systems are operating, the vent tank is automatically maintained about half-full of water with pressurized gas above it. One 0.5-inch tube from the top of the in-pool heat exchanger upper header connects up to the vent tank. A second 0.5-inch tube connects upward from the top of the end pipe extending above the hot leg inverted loop to the vent tank, as shown in Figure 5.1 and Figure 5.2. A third 0.5-inch tube connects the vent tank to a location just below the pressure vessel head at the top of the primary pressure vessels. These tubes, which are not included in the RELAP5 models, allow air that would otherwise be trapped near the top of these three primary loop high points to slowly rise through the 0.5-inch tubes and enter the vent tank gas volume. In the LOCA simulations the needed expansion volume is provided by the vapor/condensate that is produced and the air that comes from the anti-siphon system. Although the high pressures should have little effect on the peak fuel temperatures achieved in a LOFA, they make the simulation look suspect. Therefore, a rudimentary vent tank model was added.

In the RELAP5 model, time-dependent volume 915, which is large and contains water at 120° F and 55 psia, was added and connected to the top of branch 406, which is above the outlet header. A motor control valve was added between volume 915 and branch 406. This valve remains fully closed unless valve V507A, valve V507B, and the anti-siphon valve are all closed and the pressure in branch 406 is greater than 55.0 psia. When all of these conditions are met simultaneously, the added motor control valve opens and stays open. Although the vent tank model is in effect inactive in the LOCA simulations, it is included in the model used for LOCA simulations in keeping with the concept of a single unified model that can be used in the simulation of many upsets.

5.3.2.13 *Improved Anti-Siphon System Model*

Figure 5.13 provides a sketch of the anti-siphon system. In the lower left there are two 4-inch valves, V543A and V543B, that connect to opposite sides of a 12-inch primary coolant and pipe extension above the inverted loop. This connection point is 10 inches below the lower horizontal header of the in-pool heat exchanger that also connects to the end pipe extension. As can be seen in Figure 5.13, the two valves connect to two 4-inch pipes that merge into a single 4-inch pipe, which joins a ¾-inch pipe just above the “refuel level”. The ¾-inch pipe is 45 inches long. After two 90° bends it connects to a closed 25-gallon tank that is filled with air. When the anti-siphon valves are open and the air volume has depressurized, water can enter and replace some of the air in the anti-siphon system. The right edge of the figure shows a dry well with a level sensor to measure the water level in the anti-siphon system and a venting line and valve through which the water can be forced out by adding air to the 25-gallon tank. The added volume for the level detection and venting is less than 4 gallons.

In the 2006 SAR RELAP5 model, the anti-siphon system is a volume of air that is 4.242 feet long and 1.396 square feet in cross-section, which corresponds to 44.3 US gallons. This is connected at the elevation of the lower header of the in-pool heat exchanger to the primary coolant loop by a 4-inch valve.

Figure 5.13. Anti-Siphon System Air Volume.

For the current model the following three changes have been made: 1) the location of the valve (V543A and V543B combined) has been moved 12 inches down to the middle of branch 406, as indicated in Figure 5.2 and described in Section 5.3.2.1; 2) the valve area has been doubled since there are two 4-inch valves instead of one; and 3) the air volume has been approximated by a series of three connected volumes, as indicated in Table 5.3. As the table indicates, the first volume, which is furthest from the valve, is the 25-gallon tank and is nominally 16 inches in diameter and 30 inches long, the second volume is the ¾-inch Schedule 40 pipe, and the third volume is the combined volume of the 4-inch pipes and the drywell. The volume of the 4-inch pipes in the third volume is about three times that of the drywell. Thus, merely adding the drywell volume to volume of the 4-inch pipes to form a single volume is a reasonable approximation. The air volumes were calculated from available drawings. The total volume, 40.3 gallons, as indicated in Table 5.3, is about 9% less than was used in the earlier model. In the model, the lengths of the first and third volumes are oriented vertically and the length of the second volume is oriented horizontally.

Table 5.3. Anti-Siphon System Model Volumes.

Number	Description	Length, feet	Diameter, feet	Area, ft ²	Volume, feet ³
1	25 gallon tank	2.5	1.305	1.338	3.345
2	¾-inch pipe	3.75	0.06867 (0.824")	0.003704	0.014
3	4-inch pipe & drywell	10.0	0.5079	0.2026	2.026
Total		16.25			5.385 (40.28 gal)

5.4 Two-Fuel-Element LEU Model

None of the results in this report are from the two-element RELAP5 model. It has essentially all of the model improvements that were included in the one-element models, except for the improvement to the anti-siphon system, which replaces one air volume with three. This improvement was added after it was decided to obtain all of the results with the one-element model. The two-element model was helpful in verifying and checking portions of the one-element model. The two-element model is briefly described for future reference.

After the one-element LEU model was produced it was expanded to produce the two-element LEU model. With the one-element model all the coolant channels connect to one common volume (501 of Figure 5.10) just above and another (575 of Figure 5.10) just below the fuel plates, which then connects to total primary flow paths just above and below the core. With the two-element model all the coolant channels in a single element connect together in a 3.5 inch long volume on each end of the fuel element between the end of the fuel plates and the end of the fuel element end fitting, volumes 501 and 575 in Figure 5.14 and volume 301 and 375 in Figure 5.15. Then the coolant from the two different fuel elements combines together just above and below the fuel elements' end fittings in common volumes just above and below the element end fittings, volumes 201 and 275 in Figure 5.14 and Figure 5.15. The upper and lower common volumes then connect to the total primary flow paths just above and below the core. Figure 5.14 shows the core nodal structure used for the assumed limiting LEU element of the two-element model.

[illegible]

Figure 5.14. LEU Reactor Core Nodal Structure for Assumed Lighting Element of Two-Element RELAP5 Model.

[illegible]

Figure 5.15. LEU Reactor Core Nodal Structure for Combined Seven Elements of Two-Element REAP5 Model.

All of the fuel coolant channels and fuel plates in Figure 5.14 are the same and have the same numbers as in the one-element LEU model of Figure 5.10. The differences are in the upper and lower reactor plena. Figure 5.14 has two upper plena. Plenum 201 is the reactor upper plenum, which is common to both fuel elements in the model. It is 3.5 inches shorter than plenum 501 in the one-element RELAP5 models. Plenum 501 of the two-element model is 3.5 inch long and represents the region from the top of the fuel plates to the top of the side plates in the element and belongs only to the element. Similarly, the Figure 5.14 model has two lower plena. Plenum 575 is 3.5 inches long and belongs only to the element. Plenum 275 is 3.5 inches shorter than the lower plenum in the one-element RELAP5 models and is common to both elements in the model. Figure 5.15 is analogous to Figure 5.14 and provides the nodal numbers for the element in the two-element model that represents seven elements combined.

The yellow reactor vessel heat structures show in Figure 5.14 and Figure 5.15 that are along the length of the fuel elements are unique to a particular model element. In determining the heat transfer area for each of these structures the circumference of the reactor vessel wall was divided into two parts with one-eighth of the circumference apportioned to the model element that represents only one reactor element and the other seven-eighths apportioned to the other model element that represents the other seven reactor elements. Reactor plenum heat structures 2010, 2011, 2750 and 2751 appear in both Figure 5.14 and Figure 5.15 because these heat structures are common to both model elements.

Figure 5.1 and Figure 5.2 are correct only for the one-element models. The core, the lower 3.5 inches of the upper plenum, and the upper 3.5 inches of the lower plenum would have to be altered in obvious ways to represent the two elements, three upper plena, and the three lower plena of the two-element model.

The input text file for the RELAP5 (one-element) model used in the MURR SAR contained some 8000 lines. The input statements in RELAP5 have statement numbers and may be in any order in the input. When more than one statement of the same number appears, the last instance is used by the code and the others are ignored. Thus, a safer way to modify the input is to add statements to the end and leave the original ones in place. Thus, during Phase I the input file length increased considerably. After sufficient confidence and understanding of the structure of the new HEU and LEU RELAP5 models was achieved it became possible to better organize and streamline the input statements of the models. Many of the superseded statements were eliminated. Also, instead of organizing the statement by RELAP5 component sometimes statements that provided the same quantity for similar components were grouped together. For example, the cards that contained the hydraulic diameters of an element were listed together. This drastically reduced the number of comment cards, since one comment card could serve an entire group of statements. Thus, an input for a two-element model was produced in which it was easier to find and alter key parameters and was substantially shorter than 8000 lines.

In the development of the two-element model a decision was made to have one of the two elements represent the assumed limiting MURR element and the other model element to represent a combination of the other seven MURR elements. It is possible to change the inputs to the two-element model so that one of the model elements represents a combination of two core elements of nearly the same burnup and the other model element represents the other six MURR

elements. All of the needed model structure – fuel plates, coolant channels, and control blade channels – are already in the two-element model. However, it would be necessary to change the values of many of the parameters.

The two-element model with the appropriate effects of burnup on the geometry of each model element could have been used. Section 5.3.2.8 describes how the flow area and hydraulic parameters of the model element that represents seven core elements could have been determined. For each BOL, MOL, and EOL element to be represented as the assumed limiting, a unique model of the other model element, which represents the remaining seven elements, would have had to have been produced. This would have avoided the need for a separate hydraulics model and would have produced a single model where the primary flow rate in the model would have been the total primary flow and the core power would have been based on the elements represented. The only increase in power relative to true one would have been due to the azimuthal power peaking factor for each fuel plate. The downside, however, is the effort needed to determine the input parameters for the second element and to produce a core model that is twice as big and requires considerably more computational time. Done properly, however, both the one-element and the two-element model should produce equivalent results and be equally defensible. If desired, in the future one could pursue the use of the two-element model. Also, the assumed limiting element in the model could be made to represent a pair of elements and the larger element of the model could be made to represent the combination of the other three pairs of elements in the core.

5.5 Separate Hydraulics Network Model to Predict Element Flows in Mixed-Burnup Cores

Table 5.4 provides the element burnups and channel reduction at maximum burnup for both the HEU and the LEU core. Consistent with Reference 2, it was assumed that all channel thicknesses were linearly reduced with element burnup with a maximum internal channel reduction of 10 mils at 150 MWd for the HEU core and 8 mils at 180 MWd for the LEU core. The channel reduction of the end channels were assumed to be half those of the internal channels. A way to visualize this is to assume that the curved centerline of each fuel plate remained at a fixed radius while the concave radius of the plate gets shorter and the convex one gets longer by the same amount. Given the dimensions of each coolant channel of each fuel element in a core, it is a simple matter to determine the total flow area of each element and its total wetted perimeter. The hydraulic diameter of the element was taken to be four times the total flow area divided by the sum of the wetted perimeters of all of the individual channels.

Since the elements appear in burnup pairs with each element of the pair at about the same power, each pair was assumed to form a single parallel hydraulic path. Although coolant temperature has at most a second order effect on the flow distribution among the paths, the average coolant temperature of each path was determined as the average of the inlet and outlet temperatures for the path. The power produced by each path was included in the model so that these average temperatures could be determined. This was accomplished by assuming that the power produced by the path is $\dot{m} C_p \Delta T$, where $\dot{m}$ is the path flow rate, C_p is the specific heat capacitance, and ΔT is the coolant temperature rise.

Table 5.4. Element Burnup and Channel Reduction.

	HEU	LEU
Burnup, MWd		
Elements 1 & 5	0	0
Elements 2 & 5	81	96
Elements 3 & 7	65	77
Elements 4 & 8	142	170
At End of Life		
Burnup, MWd	150	180
Channel Reduction, mils		
Internal	10	8
End	5	4

The hydraulic network has one unknown flow rate for each of the four parallel paths. In addition, the pressure drop that is common across all four flow paths from the inlet to the outlet of the network is also unknown. There are four pressure-drop-versus-flow relationships - one for each path. A fifth equation is obtained by recognizing that the sum of the four path flow rates is equal to the known total flow rate.

The pressure drop across each path, ΔP , is assumed to be a result of wall friction and is represented as:

$$\Delta P = f_i L/D_i \rho_i V_i^2/2. \quad (5.6)$$

Here f_i is path Moody friction factor as obtained from the transcendental Colebrook equation, L is the 25.5-inch length of the fuel plates, ρ_i is path coolant density, and V_i is path coolant velocity. The Colebrook equation also needs a value of plate absolute roughness, which was taken to be 63 micro inches. It also needs the Reynolds number, which is $\rho_i V_i D_i / \mu_i$, where μ_i is coolant viscosity. The flow rate of each path was needed to determine the average temperature rise of each path. The average temperature of each path was used to determine the coolant specific heat capacity, viscosity, and density for the path. These properties were approximated by polynomial curve fits. All of the required functional relationships were represented in a spreadsheet to find a simultaneous solution to the entire set of equations.

In the above pressure drop relationship form, or K-losses, are ignored. As explained in Chapter 13 of Reference 2 these losses are very small compared to the wall friction ones and have a very minor effect on flow distribution among the channels of a particular element. Similarly, they should have a very small effect on the distribution among the eight elements of the core.

For the one-element RELAP5 model the flow rate of the MURR element of interest was multiplied by a factor of 8. In effect there were eight copies of the element of interest in the RELAP5 model. The total core flow rate in the RELAP5 model rarely matched the total core flow rate in the reactor. However, this is not a problem because the thermal hydraulic conditions in the model element during the first few seconds of the upset, which is when the peak fuel temperature occurs, are correct. Because the total flow rate and power in the model do affect the predicted long-term fuel temperatures other means are used to address this matter (e.g., Section 5.8.2).

Because in the simulation the core in effect is modeled as eight BOL, MOL, or EOL elements there is another concern to address. The pressures and pressure drops that are predicted in the simulation are out of sync with their counterparts in Trips 1, 4, and 5 of Figure 5.3, Figure 5.5, and Figure 5.6, respectively.

In addition, some of the trips are not specified yet for an LEU core. They are based on operating experience with an HEU core. The pressure drop for the LEU core is on the order of 5 psi less than that for an HEU core. Consequently, the initial core pressure drop predicted by the LEU EOL simulation of 22.5 psi is less than the 24.8 psi pressure drop trip. Thus, the trip criterion is satisfied at the initiation of the upset and no time is required for the pressure drop to reach the trip. Technically, this represents an insignificant modeling error. As explained at the end of Section 5.3.2.2, only a few ten-thousands of a second are required for the pressure drop to reach the trip point, so there should be essentially no effect on the predicted peak fuel temperatures.

The use of the maximum value of channel gap thickness reduction in the EOL elements redistributes the most flow from the EOL elements to the BOL ones. This assumption may be too optimistic for the BOL elements and may cause predicted BOL peak fuel temperatures to be too low. This issue is addressed in Section 6.4 after all of the LOCA and LOFA peak fuel temperatures have been presented.

5.6 Analysis to Demonstrate the Effectiveness of the Anti-Siphon System

A potential mechanism for siphoning water out of the primary vessel is a break in the 12-inch hot-leg pipe immediately upstream of the V507A valve. Refer to Figure 5.1 and consider a double-ended break at this location. One could conceive of water being siphoned from the upstream portion of the double-ended break and gradually being displaced by air entering the downstream portion of the break. In theory, this could continue until the reactor vessel is virtually empty. In practice, this may be rather difficult to achieve. There is a very long highly resistive flow path that would have to empty before the incoming air could reach the top of the reactor vessel. The V507B core inlet isolation valve will fully closed within 10 s of the initial break and thereby block the source of air needed for the siphon to function. Moreover, the 502 inlet check valve may close even sooner and block the source of air.

In spite of these obstacles to uncovering the fuel by siphoning water from the hot leg, prudent design has dictated the inclusion of an anti-siphon system to evacuate the water in the downward flow portion of the hot leg and thereby absolutely preclude siphoning via the hot leg. As Figure 5.3 indicates, a reduction in core outlet pressure to 26.0 psig causes the two parallel redundant anti-siphon valves, indicated as V543 in Figure 5.1, to open, which, in turn, connects the 40-gallon volume of compressed air in the anti-siphon system to the top of the core outlet header.

In the simulations to demonstrate the effectiveness of the anti-siphon system, conditions most conducive to siphoning are selected. Hot water at atmospheric pressure is closer to boiling than is cold water. Any steam that is produced in the hot-leg downcomer by boiling can serve the same function as injected air at the same location in preventing siphoning. Therefore, the reactor is assumed to be at zero power with the coolant at a very low temperature of 86° F. In the same spirit, the mass of air in the anti-siphon tank was kept to a minimum by assuming an initial air

pressure of 26 psig (40.3 psia) and an initial gas temperature of 105° F. The MURR Technical Specification 3.4b requires the minimum anti-siphon system tank pressure to be 27 psig, and by procedure the pressure is kept at 36 psig when the reactor is operating. Thus, the pressure assumed in the simulations is 1 psi below the allowed minimum.

The one-element RELAP5 model of the MURR core was used in the simulation. The transient is initiated by a double-ended break at the exit isolation valve, valve V507A. In order to simulate an overly optimistic source of air to facilitate siphoning, a double-ended break is assumed to simultaneously occur at the inlet isolation valve, valve V507B. Thus, the air needed to promote siphoning does not need to travel through the primary pumps or the primary heat exchangers, but instead is introduced very close to the core inlet.

The initial steady-state pool temperature was set at 105° F to be consistent with the initial anti-siphon tank temperature. Since the primary system temperature is 86° F, which is 19° F lower than the pool, and there is no power generated in the reactor plant, there will be some heat transfer from the pool through the vessel walls and the primary piping into the primary system. Thus, the initial condition is not a true steady-state condition in that the pool temperature slowly drifts down toward the primary system temperature. However, the small drift in pool temperature will have no discernable effect on the siphoning of water from the hot leg.

For the siphon simulation, middle-of-life (MOL) elements, at 65 MWd for the HEU core and at 96 MWd for the LEU core, were used because they would better represent the average core resistance than would BOL or EOL elements. The simulations were executed both with the anti-siphon system active and without it being active so that the effectiveness of the anti-siphon system could be assessed. The separate spreadsheet hydraulics model described in Section 5.5 above was used to predict the flow rate for the one-element model used to simulate a zero-power condition with a primary coolant inlet temperature of 86° F.

5.7 Results

The one-element HEU BOL RELAP5 model was used to determine the azimuthal hot stripe fuel centerline temperature histories for each of the four axial fuel nodes of each of the 24 fuel plates in the assumed limiting HEU BOL element for Core 3A. In a similar manner, the assumed limiting HEU MOL element and the assumed limiting EOL element were analyzed for Core 3A. In an analogous manner LEU Core 7A was analyzed.

5.7.1 Cold-Leg LOCA Caused by a Double-Ended Break at the V507B Inlet Isolation Valve

The results of the cold-leg LOCA caused by a double-ended break at the reactor inlet are summarized in Table 5.5 for the HEU core. In the BOL and MOL elements in the HEU core, the peak temperature occurred in plate 1, which is the closest to the central flux trap, and in plate 2 in the EOL element. The results for the LEU core are summarized in Table 5.6. For the BOL and MOL LEU elements, the limiting plate is plate 23, the outermost plate, which is closest to the outer pressure vessel. For the EOL LEU element, the limiting plate is plate 22. In every HEU and LEU case the peak temperature occurred in the third of four axial fuel plate nodes,

which is the location of the peak heat flux. The first node is at the top of the fuel plate. For each peak fuel temperature, Table 5.5 and Table 5.6 also provide the initial steady-state temperature at the location of the peak, along with the rise in temperature at that location from the steady-state value to the peak value and the time after the break that the peak occurred.

Table 5.5. HEU Core Results for a LOCA Caused by a Double-Ended Break at the Reactor Inlet.

Element Burnup	Peak Temp. in Accident, °F	Steady-State Temp. at Location of Peak, °F	ΔT at Location of Peak, °F	Time Peak Occurs, s
BOL, 0 MWd	256	226	30	0.340
MOL, 65 MWd	250	222	29	0.340
EOL, 150 MWd	237	210	27	0.340

Table 5.6. LEU Core Results for a LOCA Caused by a Double-Ended Break at the Reactor Inlet.

Element Burnup	Peak Temp. in Accident, °F	Steady-State Temp. at Location of Peak, °F	ΔT at Location of Peak, °F	Time Peak Occurs, s
BOL, 0 MWd	289	268	21	0.330
MOL, 96 MWd	308	286	22	0.330
EOL, 180 MWd	314	290	24	0.340

Table 5.5 shows that the peak HEU core temperature, 256° F, occurred in a BOL element. Figure 5.16 shows the steady-state fuel centerline temperature distribution for this HEU BOL element. Similarly, Table 5.6 shows that the peak LEU core temperature, 314° F, occurred in an EOL element. Figure 5.17 shows the steady-state fuel centerline temperature distribution for this LEU EOL element. Figure 5.16 shows that the highest HEU steady-state fuel temperature occurs in node 3 of plate 1 and has a value of 226 °F. Similarly, based on Figure 5.17, the highest LEU steady-state temperature of 290 °F occurs in node 3 of plate 22.

In interpreting the transient results for the cold-leg LOCA due to a double-ended break it is helpful to keep the following sequence of events in mind:

1. Double-ended break at isolation valve V507B (time = 0.0 s)
2. Rapid depressurization of the primary coolant system
3. Rapid reduction in core flow rate
4. Reactor scram (time = 0.325 s)
5. Peak fuel temperature reached (time = 0.340 s)
6. In-pool heat exchanger valves start to open at 0.45 s and are fully open at 0.96 s
7. Primary coolant circulation pumps trip (time = 1.0 s)
8. Anti-siphon system injects air into the 12-inch core outlet piping inverted loop (time = 1.0 s)
9. Core inlet check valve V502 closes to prevent coolant flow back towards the pipe break (time = 1.2 s)
10. Main isolation valves V507A and V507B start closing at 4.5 s and are fully closed at 10 s

Figure 5.16. HEU BOL Steady-State Fuel Centerline Temperatures at Full Power.

Figure 5.17. LEU EOL Steady-State Fuel Centerline Temperatures at Full Power.

Figure 5.18a and Figure 5.18b, which are the same except for the time scale, provide the fuel centerline temperature history at the limiting location in the HEU core (BOL element 5, plate 1, node 3) and in the LEU core (EOL element 4, plate 1, node 3). The initial rise in temperature from the steady-state condition is due to the rapid decrease in flow that is caused by the break. In both cases the reactor scram at 0.325 seconds causes the peak centerline temperature to decline shortly thereafter, at 0.340 seconds.

Figure 5.18a. HEU and LEU Core Fuel Centerline Temperature at the Limiting Locations for a Cold-Leg LOCA (Double-Ended Break, 0 to 15 s).

Figure 5.18b. HEU and LEU Core Fuel Centerline Temperature at the Limiting Locations for a Cold-Leg LOCA (Double-Ended Break, 0 to 350 s).

The LEU curve ends at 13 s because the solution failed prematurely. This is believed to be a problem within the RELAP5 code that sometimes occurs when non-condensable gases are introduced. The non-condensable gas is the air that was injected by the activation of the anti-siphon system. Page 67 of Volume V of the RELAP5/MOD3.3 manual [17] states:

... experience has shown that calculational difficulties may be encountered during periods when a mixture of noncondensable gas and steam is appearing or disappearing (i.e., at very small void fractions). Circumventing these difficulties has required the analyst to manually (e.g., on restart) insert or remove vapor to artificially aid its appearance or disappearance.

Because the alleged problem can simultaneously appear at the exit of several core coolant channels in the model at various times early in the simulation, no practical good remedy has been found. An error message for this case indicates that the pressure in the coolant node that initiated the solution failure had a pressure of 1.03981E+07 Pa (1508 psia). The HEU simulation failed at 1159 s after the pipe break. This failure may be due to another cause because the failure occurred relatively late in the transient and the pressure at the node that initiated the failure showed no variation from its long-term trend when the failure occurred. The HEU problem probably could have been run to completion by rerunning it with a modification in the time step sizes near the time of failure. This was not done because 1159 s is sufficient.

Both the HEU and the LEU cases were executed a second time with the anti-siphon system not activated and ran to completion at 2500 s. An active anti-siphon system enables air to rise into the in-pool heat exchanger and prevents natural circulation between the core and the in-pool heat exchanger. When the anti-siphon system is not active, vapor rises into the in-pool heat exchanger and serves the same function.

The fuel centerline temperature histories at the location of the peak were compared for the two HEU solutions. Both behaved the same through the scram and the immediate drop in temperature that followed. The small rise and decline in fuel temperature between about 1 and 4 s that occurred after the activation of the anti-siphon system, Figure 5.18a, occurred only in the solution in which the anti-siphon system was activated. Longer term the solution in which the anti-siphon system was not activated tracked close to and often a little above the one with the anti-siphon activated.

The HEU results shows that activating the anti-siphon causes the 502 reactor inlet check valve to close at 1.2 s instead of 4.8 s. This causes less mass to enter the vessel at the outset of the upset. During the first 10 s while the reactor outlet isolation valve V507A is open, activating or not activating the anti-siphon system appears not to substantially affect the total amount of mass exiting the reactor vessel. This was surmised from a plot in which the two predicted flow rates through valve V507A were compared for the two cases. The shapes of the two curves were dramatically different, but the integrated area under each curve appeared to be about the same. Thus, it appears that activating the anti-siphon system results in less coolant mass and energy remaining in the vessel after the V502 inlet check valve and the V507A outlet isolation valve have closed. At 1100 s after the pipe break the outlet plenum pressure is about 15.2 psia if the anti-siphon system was activated and is about 16.6 psia if it was not. This may explain why not

activating the anti-siphon system causes slightly higher long-term peak fuel temperatures. Higher pressures correspond to higher coolant saturation temperatures.

The motivation for considering an inactive anti-siphon system is two-fold. One is to estimate long-term behavior when the RELAP5 code solution failed very early in the simulation due to the presence of non-condensable gases, i.e., anti-siphon system air, in the simulation. The second was to obtain some insight into the effect of the timing of the activation of the anti-siphon system on the results. If activating it or not activating it had no effect on the short-term peak fuel temperature results, as has been observed, then the timing is probably not important. There are no reactor scenarios where an anti-siphon system failure is to be considered.

As Figure 5.18b shows for the HEU core, the longer-term temperatures are relatively constant with a gradual downward bias with time as the decay power, Table 5.2, slowly declines. Far out in time (many hundreds of seconds) there is a very gradual decrease in fuel centerline temperature with time. This trend would be easier to observe in the HEU graph if the figure were extended further in time and is particularly obvious for both fuels in the simulations in which the anti-siphon system is inactive and the simulations were run to 2500 s. For the HEU case with the anti-siphon inactive, the corresponding temperature at 2500 s is 213 °F, and is 222 °F for its LEU counterpart.

As indicated above, the peak fuel temperature in the cold-leg case occurs at 0.34 s after the break, which is 0.66 s before the primary pumps trip at 1.0 s. Although these pumps and the reactor vessel are hydraulically connected via the intact hot-leg, the core flow drops precipitously at the outset and causes the fuel temperature to peak, while the pump flow actually increases, Figure 5.19. The pump flow in this figure is at the primary pump inlet and the inlet flow is in pipe 137 as it meets branch 139 in Figure 5.2. This is the total flow coming up the inlet riser in pipe 137. Both the pump and inlet riser flow decline sharply at 1.0 s because this is where the pump trips. The inlet flow goes to zero at 1.2 s because the V502 check valve closes then. The pump flow goes to zero when the V507A isolation valve closes. The HEU initial primary flow through the BOL element case is considerably greater than the flow through the LEU EOL element case. Recall that in this RELAP5 model the initial reactor flow is eight times the predicted flow of the assumed limiting element instead of the actual total flow rate through the eight element core.

Because the pumps are running at their initial steady-state speed until they trip at 1.0 s, at first glance, one would expect both the pump and the vessel flow to remain essentially the same and near their initial values. However, this does not happen because of cavitation in the outlet leg. At the top of the outlet riser (pipe 102, node 1 in Figure 5.2) during most of the first second of the transient the pressure is nearly constant at about 3.7 psia for the LEU case and 3.1 psia for the HEU case.

The reason for this severe vacuum is that the pressure at both ends of the break is the atmospheric pressure of 14.3 psia. Fluid is drawn toward and out of the primary loop through both ends of the break. The fluid in the core is being pulled from two directions. One end of the break is pulling from the inlet side of the core and the pump is pulling on the outlet side of the

Figure 5.19. HEU BOL and LEU EOL Cold-Leg LOCA (Double-Ended Break) Pump Inlet and Reactor Vertical Inlet Pipe Flows.

core. As Figure 5.1 indicates, the break is at the lowest point in the primary loop. The top of the outlet rise is 18.626 feet higher. The difference in gravity head plays an important role here. A stagnant 18.626 feet column of liquid water would have a pressure at the top that is about 8 psi lower than that at the bottom.

At 3.1 psia water boils at 142.7 °F and at 3.7 psia it boils at 149.8 °F. The initial water temperature at the top of the outlet riser is 144.8 and 151.4 °F for the HEU and LEU cases, respectively. Thus, the coolant boils and generates vapor in the outlet leg at the outset of the transient. This causes the core flow to precipitously drop as vapor is generated between the core outlet and the pump inlet. This also temporarily reduces the load on the pump and allows its flow to increase, as observed in Figure 5.19. As the amount of vapor between the core outlet and the pump inlet tends to stabilize it is possible for the core flow to recover somewhat. This may be the reason for the LEU inlet flow recovery seen later in the first second in Figure 5.19 (red curve). Since the HEU has a lower outlet temperature, its propensity to generate vapor is less than in the LEU case. This may be the reason that the HEU does not show the same inlet flow recovery during the first second.

Figure 5.20 shows the flow rate relative to the steady state flow rate in channel 2 of the HEU BOL element and channel 23 of the LEU EOL element. The initial steady-state velocity at the core centerline is 26.2 ft/s for the HEU channel and 22.7 ft/s for the LEU channel. The sudden increase and then decrease in flow in the LEU plot at about 0.9 s is a local phenomenon that is not seen at the same time in the LEU inlet riser flow of Figure 5.19. In LEU channel 23 there is a rapid generation of vapor. The void fraction in the bottom node, which by far has the largest vapor fraction of the four axial nodes, rapidly increases at about 0.2 s, reaches a peak of about 90% at about 0.6 s, and returns to zero just before 0.9 s. The disappearance may be a result of the drop in power due to the scram at 0.325 s.

Figure 5.20. Flow Rate Relative to Steady-State in Channel 2 of the HEU BOL Element and Channel 23 of the LEU EOL Element for a Cold-Leg LOCA (Double-Ended Break).

For all six double-ended break cold-leg LOCA simulations whose results are provided in Table 5.5 and Table 5.6, the scram was initiated by Trip 5 (Figure 5.6) which is caused by the core pressure drop being equal to or less than 24.8 psi. For the HEU BOL, MOL, and EOL cases the initial core pressure drop was 31.6, 29.1, and 26.8 psi, respectively. For the LEU BOL, MOL, and EOL cases the initial core pressure drop was 25.8, 23.8, and 22.5 psi, respectively. Of these six cases the maximum time to reach the trip pressure drop was 0.0003 s. However, the effect on the peak fuel temperature is very small because the pressure drop decreases instantaneously and precipitously with the pipe break. As Figure 5.18a indicates, the rise from the initial steady-state temperature to the peak is essentially linear. The immediate drop in temperature after the peak is due to the scram. The slope of this rise can be estimated from the results in Table 5.5 and Table 5.6. For example, the for the HEU BOL case the rise is 30 °F and occurs over 0.34 s, which corresponds to 88.2 °F/s. Similarly, the HEU MOL and EOL cases have slopes of 85.3 and 79.4 °F/s, respectively. The LEU BOL, MOL, and EOL cases have slopes of 63.6, 66.7, and 70.6 °F/s, respectively. Thus, each extra 0.0001 s needed to reach the trip core pressure adds less than 0.01 °F to the peak fuel temperature and the effect of 0.0003 s is not significant.

5.7.2 Hot-Leg LOCA Caused by a Double-Ended Break at the V507A Outlet Isolation Valve

Because the peak fuel temperature in the cold-leg (double-ended break) LOCA occurred in the BOL element of the HEU core, as shown in Table 5.5, the same BOL element was assumed to be the limiting element in the study of the other postulated LOCAs in the HEU core. Table 5.7 summarizes the peak HEU fuel temperatures for all four postulated LOCAs. Similarly, the same LEU EOL element that produced the peak fuel temperature in Table 5.6 was assumed to be the

most limiting LEU element for all of the postulated LOCAs in the LEU core. Table 5.8 summarizes the peak LEU fuel temperatures for all postulated LOCAs.

Table 5.7. Summary of HEU Loss-of-Coolant Accident Results (BOL).

Break	Peak Temp. in Accident, °F	Location of Peak Temp., Plate	Steady-State Temp. at Location of Peak, °F	ΔT at Location of Peak, °F	Time Peak Occurs, s
Cold Leg, Double Ended (See Figure 5.18a)	256	1	226	30	0.340
Cold Leg, 8 Inch (See Figure 5.24)	243	1	226	17	0.340
Cold Leg, 2 Inch (See Figure 5.27)	267	2	219	47	309
Hot-Leg, Double Ended (See Figure 5.21)	234	2	219	14	21.3

Table 5.8. Summary of LEU Loss-of-Coolant Accident Results (EOL).

Break	Peak Temp. in Accident, °F	Location of Peak Temp., Plate	Steady-State Temp. at Location of Peak, °F	ΔT at Location of Peak, °F	Time Peak Occurs, s
Cold Leg, Double Ended (See Figure 5.18a)	314	22	290	24	0.340
Cold Leg, 8 Inch (See Figure 5.24)	302	22	290	12	0.330
Cold Leg, 2 Inch (See Figure 5.27)	291	22	290	1	0.200
Hot-Leg, Double Ended (See Figure 5.21)	290	22	290	0	0.0

For both the HEU and LEU hot-leg LOCA simulation the pump flow rate remains nearly constant during the first second after the break. The drop in pressure caused by the break causes the pressurizer to provide a modest amount of flow to the system of less than 50 gpm. Thus, the RELAP5 output shows that the flow at the top of the inlet riser during the first second of the transient is the pump flow increased by the modest addition from the pressurizer. Since the core flow remains at or above its initial value during the first second of the transient and the scram occurs at 0.875 s (Trip 1, Figure 5.3), it is expected that the peak fuel temperature occurs at the initial condition.

This is what is observed for the LEU core, but not for the HEU core. Figure 5.21 shows that there is an initial steep drop in temperature in both cores is due to the scram. The subsequent

temperature recovery is due to the primary pump trip, which occurs at 1.0 s. The peak HEU temperature for the hot-leg LOCA occurs after the temperature recovery produced by the pump trip where the fuel temperatures start to be governed by the saturation temperature. This peak is reached at 21.3 s after the break and is 14° F above the initial steady state at the location of the peak. Closer inspection shows that at about 21.0 s, substantial gas void fractions start to occur in all four coolant nodes on each side of plate 2. Peak gas void fractions of about 0.9 occur in node 3 on both sides of the plate. These gas void fractions go from 0.0, to their peak values, and back to 0.0 over about 1 s. The flow is near zero in both channels before the gas forms. The gas temporarily pushes flow upward in the upper half of each channel and downward in the lower half. Thus, the precise timing of the peak temperature in the simulation is a the result of the model predicting that voiding simultaneously occurs on each side of the limiting fuel plate node long enough to cause a temperature peak that is greater than all of the prior or future temperatures in the core.

Figure 5.21. Peak Fuel Temperatures for the Hot-Leg LOCA.

In the RELAP5 simulation for the LEU core a bit of an anomaly occurred. The scram occurred at 0.7035 s. This seemed rather odd since the trip that was expected to initiate the scram is the outlet pressure trip, Trip 1, Figure 5.3, and it has a built-in scram delay of 0.875 s. The problem was traced to the low flow trip, Trips 2 and 3, Figure 5.4, which has a built-in scram delay of 0.375 s. Further investigation showed that although the initial primary flow is 3700 gpm for the reactor, because an EOL element scaled up eight times is used to represent the core, the initial flow rate in the RELAP5 simulation is only 3455 gpm. This is much closer to the 3300 gpm trip flow rate than is 3700 gpm. Initial oscillations in flow caused the low-flow trip to initiate the scram. Since this is clearly an artifact of the simulation, the 3300 gpm trip was reduced to 3288 gpm and the simulation was executed again. Then the scram was initiated by the core outlet pressure trip at 0.8753 s. The results for this later case are presented in this report. The peak fuel temperature was unaffected since it occurred at the initial steady-state condition.

The cold-leg LOCAs to be considered next differ from the hot-leg LOCAs just considered in that in the hot-leg LOCA after the break the hot leg is always open to the atmosphere. In the cold-leg LOCA, by contrast, the core coolant is contained between a closed hot-leg isolation valve and a closed inlet check valve in the long-term. However, both double-ended break LOCAs are similar in that a considerable amount of steam is produced and removed through the break due to the rapid depressurization at the outset. This causes a considerable energy loss from the reactor vessel which helps to lower the long-term coolant temperatures in the core.

5.7.3 Cold-Leg LOCA Caused by an 8-Inch Break at a 540 Diaphragm Valve

For the HEU cold-leg LOCA caused by an 8-inch break at the 540 valve, the pressure at the top of the reactor outlet riser drops to 3.1 psia, as in its double-ended break counterpart, and cavitation does occur at node 1 of pipe 102, which corresponds to the top of the invert loop after the core. However, this does not have the adverse effect on reactor flow that it has in the double-ended cold-leg break case because the 8-inch break in the cold leg does not preclude the pumps from pushing flow into the reactor vessel inlet. As Figure 5.22 shows, the initial flow rate at the top of the reactor inlet riser and that at the pump outlet are the same. During the first second about half of the pump flow goes out the break (black curve). However, the drop in loop pressure causes a significant, but much smaller inflow from the pressurizer (purple curve). The net effect on the flow through the reactor inlet riser (red curve) is that during the first second it drops from 560 lbm/s to about 400 lbm/s. The initial rise in pump flow (blue curve) is due to a reduction in flow resistance due to outflow through the break and possibly due to the cavitation in the vessel outlet riser. The sudden drop in all of the flows except the pressurizer flow at 1.0 s is due to the pump trip. The sudden spike in the break and pump flows starting at about 1.5 s is due to the opening of the anti-siphon valve, which opens between 1.02 and 1.23 s and injects pressurized air into the top of the outlet header. Figure 5.23 shows the analogous results for the LEU core.

Figure 5.22. HEU BOL Primary Loop Flows for a Cold-Leg LOCA (8-Inch Break).

Figure 5.23. LEU EOL Primary Loop Flows for a Cold-Leg LOCA (8-Inch Break).

Figure 5.24 shows the peak fuel temperature for the cold-leg LOCA caused by an 8-inch break in the 540 diaphragm valve. This accident exhibits initial peak temperatures and long-term trends that are analogous to, but milder versions of those for the more severe double-ended cold-leg, which is shown in Figure 5.18a and Figure 5.18b.

Figure 5.24. Peak Fuel Temperatures for a Cold-Leg LOCA (8-Inch Break).

5.7.4 Cold-Leg LOCA Caused by a Break in the Pressurizer 2-Inch Line

The break in the pressurizer line, which is modeled as a 2-inch hole, occurs in the 2-inch diameter line that connects the pressurizer to one of two 8-inch main pipes in the primary loop. As indicated in Figure 5.1, at the location of the 2-inch break there are two competing pressure boundary conditions applied to the primary loop. One of them is the 14.3 psia sink produced by opening the break valve. The other one is caused by the 78.0 psia pressure at the fixed pressure source, which represents the pressurizer in the model. At the beginning of the transient the two pressures work together to determine the pressure at the common point in the model where they both join the 2-inch pipe. However, at 5 s into the transient the 527C pressurizer isolation valve closes and isolates the pressurizer pressure from the 8-inch header in the primary loop. As a result at about 15 s the pressure at the common point reaches 14.3 psia and remains there. A more severe version of this upset would be a double-ended break at the same location. This would separate the pressurizer from the remainder of the primary loop. Then the pressure in the primary loop at the break would immediately drop to atmospheric pressure at the start of the LOCA.

Figure 5.25, which is for the HEU BOL case, shows that the rate of flow through the hole in the 2-inch pipe is relatively small compared to the total loop flow. The flow to the top of the inlet riser remains very close to its initial steady-state value until the pump trips at 1.0 s, which causes this flow and the pump flow to coast down. The rapid changes in inlet riser flow between 1 and 2 s coincides with the opening of the anti-siphon valve. At 10 seconds both the inlet and the outlet isolation valves are fully closed. Hence, there is no flow through the inlet riser after 10 s. Figure 5.26 shows the analogous set of curves for the LEU case.

Figure 5.25. HEU BOL Primary Loop Flows for a Cold-Leg LOCA (2-Inch Break).

Figure 5.26. LEU EOL Primary Loop Flows for a Cold-Leg LOCA (2-Inch Break).

Figure 5.27 shows the peak fuel centerline temperatures for the cold-leg LOCA caused by the 2-inch break. There is a mild temperature rise of about 1 or 2 °F immediately after the break which is reversed by the scram. The long-term temperatures for the 2-inch break are significantly higher than those for the other breaks (at the same time after the initiation of the upset) because much less coolant and energy is removed from the system at the outset. Also, in the 2-inch-break LOCA the reactor vessel pressures remain higher than in the other cold-leg LOCAs. This helps to maintain a higher coolant temperature in the reactor vessel, which is limited by the saturation temperature, and, therefore, a higher fuel temperature.

Figure 5.27. Peak Fuel Temperatures for a Cold-Leg LOCA (2-Inch Break).

Since fuel failure temperatures are not being approached in Table 5.7 and Table 5.8, a higher temperature that is a result of a higher pressure and saturation temperature, may be preferable if it implies that local conditions are further from severe boiling that could lead to a critical heat flux condition.

5.7.5 Anti-Siphon System Test

The four criterion listed in Table 5.9 were selected to measure the propensity of the core to become uncovered due to siphoning. Differences in results between the HEU and the LEU core were extremely small. Therefore, the RELAP5 results in the table apply equally to and bound both.

Table 5.9. Results of Zero-Power Tests of Siphoning (Applies to both HEU and LEU).

Number	Criterion	Inactive Anti-Siphon	Active Anti-siphon
1	Time for check valve 502 to permanently close, s	≤ 3.6	≤ 1.2
2	Minimum liquid fraction in inlet plenum 501	≥ 0.88	≥ 0.87
3	Time for exit pipe 102 to empty all liquid, s	≤ 50	≤ 30
4	Time for flow at exit to go to zero, s	≤ 50	≤ 30

Criterion 1 is useful because once the inlet check valve (valve 502, which is shown in Figure 5.1) completely closes there is no external source of air to facilitate siphoning in the model. Criterion 2 is a measure of the minimum liquid level in the reactor inlet plenum, which is branch 501 in Figure 5.2. Initially the liquid fraction is 1.00, which implies that all 6.437 feet of the plenum vertical length is filled with liquid. A value of 0.87 implies that the length of the upper plenum that is filled with water is $0.87 \times 6.437 = 5.600$ feet. Since the elevation change in the upper plenum is 5.937 feet a liquid fraction of 0.87 implies that the water level in the upper plenum is $0.87 \times 5.937 = 5.165$ feet above the top of the fuel plates. Criterion 3 was evaluated by plotting the liquid void fraction histories in all seven nodes of pipe 102 of Figure 5.2 and noting at what time all seven liquid voids fractions were permanently zero. This is an indication of when the exit pipe is free of liquid. Criterion 4 is the time at which the flow rate exiting the broken hot-leg pipe 102 permanently goes to zero. This flow rate was evaluated at the junction between nodes 6 and 7 of pipe 102. Although the results with the anti-siphon system active are generally better than those with it inactive, for all practical purposes the differences are insignificant. The overriding conclusion is that the potential for the core to become uncovered due to the siphoning of water through a broken hot-leg pipe is virtually zero with or without the anti-siphon system active and that this is equally true for both the HEU and the LEU core.

5.8 Discussion

5.8.1 Natural Convection Length in the Coolant Channels

The RELAP5 code calculates a number of heat transfer coefficients at each surface of a solid, which it refers to as a “heat structure”, and in each instance uses the largest one in the simulation. These heat transfer coefficients include those for forced laminar flow, forced turbulent flow, and

natural convective flow. Natural convective flow requires a characteristic length. The default, which is what was used for the fuel plates in the above analyses, is the hydraulic diameter of the channel. For natural convection in vertical channels, the code uses the Churchill-Chu correlation for free convection from vertical flat plates. This correlation makes sense for the thin rectangular channels of the MURR only if the thermal and hydrodynamic boundary layers are small compared to the channel thickness.

By substituting various natural convection lengths into the Churchill-Chu correlation, one can show that shorter lengths lead to a higher heat transfer coefficients and that for very large lengths finite asymptotic values are reached. There is no way in RELAP5/MOD3.3 to exclude the natural convective heat transfer correlation among the correlations evaluated. If it turns out that a heat transfer correlation other than the natural convective one has the highest value, such as the one for turbulent forced flow, then it will be used and the details of the natural convective one are irrelevant.

Because of the above concerns, the cold-leg LOCA for the LEU core was solved two additional times, once with a natural convective length set to the fuel plate length of 25.5 inches and once with it set essentially to infinity, i.e., 10^9 feet. The peak transient temperature occurred at the same location, plate 22, node 3, at the same time and had the same value in all three cases. Similarly, the corresponding initial steady-state temperature was the same in all three cases.

5.8.2 Heat Transfer through the Reactor Vessel Surfaces

Throughout the hot-leg LOCA, because one end of the break always remains connected to the atmosphere, in concept, there is always a path to remove heat by expelling steam through the break. During the cold-leg LOCAs, on the other hand, long-term cooling of the core must rely only on heat removal through the reactor vessel walls and some of the pipe walls to the pool water. This heat removal includes heat transfer to the flux trap flow and to the control rod channel flow. However, the flow rates in these coolant paths are not well known and the geometries of these paths have only been approximated in the model. These flows and geometries affect the heat transfer coefficients in these paths. Rather than expend a substantial amount of analytical resources to better define these quantities, a simpler bounding approach is to repeat some of the analyses with no heat transfer from the core to either the flux trap or the control rod channels and from the lower plenum of the reactor to the reflector tank. (Refer to the boundary conditions indicated in Figure 5.9 and Figure 5.10.) This can be accomplished analytically by setting, what RELAP5 calls a “fouling factor” to a very smaller positive number, such as 10^{-9} , where heat flow is to be blocked. This factor is a multiplier on the calculated heat transfer rate through the wall. A zero value cannot be used here because it causes the default value of 1.0 to be used for the fouling factor, which results in normal heat transfer to be assumed.

This bounding approach can only lead to fuel temperatures that are the same or higher in the long term and can have no effect on fuel temperatures in the short term. This approach is more severe with regard to fuel temperatures than assuming that the pool pump has failed. Such a failure when combined with a LOCA is beyond the reactor design basis because it constitutes a double fault.

Thus, calculations were performed in which the fouling factor was set to 10^{-9} everywhere where the reactor pool coolant outside the vessel contacts a vessel surface except for the convex surface of the outer vessel wall of the upper plenum of the reactor. The specific locations in the RELAP5 model where this fouling factor was applied are at the concave side of heat structures 5010, 5020, 5030, 5040, 5050, and 5750 and the convex side of heat structures 5243, 5253, 5263, 5273, and 5751. These heat structures are shown in yellow in Figure 5.9 and Figure 5.10. These surfaces are outside reactor pressure vessel boundaries and allow the aluminum pressure vessel to be within the insulated boundary. The only issue here is whether the decay heat caused by 12 MW of initial steady-state operation can be removed with this added restriction and without excessive temperatures or pressures. The choice of HEU or LEU core does not matter since the core only serves as a source of power. If the heat removal is inadequate, during the longer term the decay power produced in the vessel will exceed the decay power removed and the coolant and fuel temperatures will gradually rise to unacceptable levels or the pressure will rise to unacceptable levels.

For this test, the double-ended cold-leg LOCA LEU EOL core would be the first choice if obtaining a solution beyond the 13 s shown in Figure 5.18a was not problematic. Therefore, the cold-leg LOCA produced by a single 8-inch break at the 540 valve was chosen. First a case in which the input file was modified to maintain the same primary inlet temperature and flow rate as before, but at a total reactor power of 12 MW, with 3.6% of this going to the reflector model and the remaining 11.568 MW deposited in the core was executed. Then the case was repeated with the heat structures insulated as indicated above. The simulation that produced the red curve in Figure 5.28 is the same as the one that produced the red curve in Figure 5.24 except that in Figure 5.28 the total reactor power has been reduced to 12.0 MW. The blue curve in Figure 5.28 is a result of insulating the 11 heat structures identified above.

Figure 5.28. Peak Fuel Temperatures for a Cold-Leg LOCA (8-Inch Break) with Total Reactor at 12 MW.

Fluid node 3 of channel 524 of Figure 5.10, which is immediately adjacent to plate 22, node 3 was examined more closely. For the base case from 400 to 2500 s the pressure was about 18.0 psia, which corresponds to a saturation temperature of 222.36 °F. During this time interval the coolant temperature of the node was always at least a few degrees Fahrenheit below this value. At 2500 s the difference was at least 15 °F.

For the insulated case for the same fuel node location and 400 to 2500 s time interval, as examined in the base case, the results show a positive void fraction which indicates boiling. The results show that the pressure is on a very slow decline over the longer term and is about 21 psia at 2500 s. The higher pressure relative to the base case is due to the reduced heat transfer through the vessel walls. The coolant temperature is also on a slow decline, since it cannot be greater than the coolant saturation temperature, which is decreasing with decreasing pressure. If the solution were extended further, both the pressure and temperature would continue to decline together as the decay heat very gradually decreases. This provides a clear indication that over time the peak fuel temperature will also continue to gradually decline. Thus, even in the insulated case, which relies only on heat transfer through the outer vessel wall of the upper plenum, there is sufficient heat removal to protect the fuel.

For the hot-leg LOCA the water level in the reactor vessel could be a concern. Therefore, the above pair of 12-MW simulations was repeated for the hot-leg LOCA. For the simulation with normal heat transfer through the vessel wall the liquid fraction near the end of 2500 s oscillated between about 0.80 and 0.82 in the reactor outlet plenum, branch 501 of Figure 5.2. The liquid height, or change in elevation, in this branch is the product of the liquid fraction and the volume height of 5.937 feet, which corresponds to the height that the cold leg inlet pipe centerline is above the top of the fuel plates.

For the insulated case the amplitude of the oscillation was much greater than in the normal heat transfer case. A minimum liquid fraction 0.30 was reached at 293 s and then it recovered. From about 500 to 2500 s the liquid fraction oscillated between about 0.5 and 0.8 and showed very little overall trend. The liquid fractions in coolant channel 23, which is RELAP5 channel 524 in Figure 5.10, were also examined, since it is between the highest heat flux plates, plates 22 and 23. All four nodes in the model showed that over much of the simulation there are major oscillations, with minimum values occasionally approaching 0, but always returning back to values approaching 1. Thus, there is severe boiling with bubbles that rise to the top of the channel. However, the highest fuel temperature observed in these two plates is 269.0 °F. It occurs at the node 3 of plate 22, fuel node 6720 in Figure 5.10. This peak was achieved at the initial steady-state condition. After the initial steep drop in fuel temperature, the fuel temperature at this location recovers and never exceeds 235.7 °F going forward. No fuel temperature in either fuel plate reaches a value higher than this value after the initial steep decline. Thus, for this extreme case, severe boiling helps to keep fuel temperatures very low and at acceptable values.

The loss of water from the system during this transient may be less of an issue since the parallel 550C/D check valves allow water to enter the primary system from the pool when the pressure gradient is favorable. These are not included in the RELAP5 models, but would add water at the location of branch 139 of the Figure 5.2 model. Page 13-35 of the MURR SAR indicates that

these check valves allow pool water at approximately 5.3 gpm to be admitted through the pair of valves when the static pressure difference is 17 feet of water. The RELAP5 results show that at 10 s the flow near the exit of the broken pipe, junction 6 of pipe node 102, is actually negative. After 10 s this flow is always very near to either side of zero and indicates essentially no net outflow over time. The volume of the outlet plenum, based on the Figure 5.2 dimensions, is 29.1 gallons. Thus, in the long-term, water is not being lost by boiling. Water being added through the 550C/D check valves will ultimately follow the normal reactor flow path to the bottom of the vessel and through the exit piping and out the break. As the pool drains down to the level of the check valves, the flow rate through the check valves should be very small. Thus, the flow rate out the 12-inch exit pipe should be too small to fill the exit pipe and create a siphon.

5.8.3 Effect of Uncertainty in LEU Fuel Meat Thermal Conductivity

The effect of the uncertainty in fuel meat thermal conductivity of the LEU fuel on peak fuel temperature was assessed. The RELAP5 simulation that produced the LEU EOL temperatures in Table 5.6 was rerun with the fuel meat thermal conductivity multiplied by a factor of 0.80. The rationale for a 20% reduction in EOL thermal conductivity is that provided for reactivity insertion accidents, Branch 01 of Table 4.5. The peak fuel temperature occurred at the same time and location in both LOCA cases. For the reduced thermal conductivity case, the initial temperature and peak temperatures for plate 22, node 3 are 298 and 321 °F, respectively. This is to be compared with 290 and 314 °F in Table 5.6. In the Table 5.6 case the temperature rose 23.90 °F to the peak. In the case with the reduced thermal conductivity this temperature rise was 23.62 °F. Thus, essentially all of the 7 °F increase in peak temperature is due to the higher initial steady-state temperature that is caused by the lower thermal conductivity.

5.8.4 Comparison of RELAP5 Fuel Temperatures with Those in the MURR Steady-State Thermal-Hydraulic Analysis Technical Basis Document

The LEU fuel centerline temperatures provided in Figure 5.17 for RELAP5 node 3 were also predicted with the PLTEMP/ANL code [16]. A beta version of PLTEMP v 4.2 of October 30, 2013 (SVN revision 65) which includes speedup improvements, extended Groeneveld 2006 CHF table, channel-dependent hot channel factors, and an improved double search algorithm was used in the analysis. Node 3 in the RELAP5 model covers the axial range from 12 to 17 inches from the top of the fuel meat. In the PLTEMP/ANL model all of the axial nodes are one inch long. Thus, nodes 13 through 17 in the PLTEMP/ANL model cover this same range, and the middle of node 3 in the RELAP5 model corresponds to the middle of node 15 in the PLTEMP/ANL model. Figure 5.29 compares the temperatures at this elevation for all 23 fuel plates of element 8 for LEU Core 7A. The same power, flow, and inlet coolant temperature assumptions were made in both calculations. Both calculations include the azimuthal peaking factors. The RELAP5 code input model is based on the input model used in the MURR steady-state thermal-hydraulic analysis technical basis document [2], but was adapted to the conditions and assumptions used in the RELAP5 analysis. RELAP5 provides the temperature at the centerline of the fuel meat. PLTEMP/ANL provides the maximum fuel temperature regardless of where its exact location is relative to the centerline. This difference should have only a very minor effect on the temperature comparison in this work. The largest difference between node 3 of the RELAP5 solution and

node 15 of the PLTEMP/ANL solutions is 2.89 °F and occurs in plate 6. In all instances the PLTEMP/ANL temperature is greater than the RELAP5 temperature.

Figure 5.29. Comparison of RELAP5 and PLTEMP/ANL Maximum Fuel Temperatures.

The bars shown in Figure 5.29 show the range of temperatures from minimum to maximum among the five PLTEMP/ANL nodes that correspond to node 3 in the RELAP5 model. The maximum PLTEMP/ANL temperature in the figure, 294.82 °F, is at the bottom of the axial range for plate 22, which corresponds to node 17 in the PLTEMP/ANL calculation. The RELAP5 temperature for node 3 of plate 22 is 290.23 °F. Thus, the PLTEMP/ANL and RELAP5 maximum/centerline temperature predictions are in close accord and the adverse effect on temperature of combining five 1-inch peaking factor nodes from the MCNP modeling to form one 5-inch peaking factor in RELAP5 is extremely small, i.e., a few degrees Fahrenheit.

5.8.5 LOCA Results Provided by the 2006 MURR SAR for HEU Fuel

Page 13-29 of the MURR SAR [6] provides the peak fuel temperatures calculated for the HEU core for both the double-ended break cold-leg LOCA and the double-ended break hot-leg LOCA. For the cold-leg break the peak of 311.7 °F is achieved in plate 3. This temperature is also indicated on page 13-30, where reference is made to Figure 13.9, which provides the fuel plate temperature histories for axial node 4 in the model, which is the bottom node of the fuel plate. On both pages 13-29 and 13-30 the time of the peak is given as 0.5 s after the break. The yellow plot on Figure 13.9 shows this peak value. The peak fuel temperature for the hot-leg LOCA is given as 281.2 °F. It occurs in plate 1 at 0.2 s after the break. Figure 13.20 shows this to be axial node 3. Table 5.7 above shows that the current analysis predicts 256 °F for the double-ended cold-leg LOCA and 234 °F for the hot-leg one.

The only loss-of-primary-flow transient considered in the MURR SAR is modeled in the same manner in the SAR as the 2-inch break LOCA whose results are provided in Section 5.7.4 above.

Page 13-58 of the SAR indicates that the highest fuel plate centerline temperature was predicted to occur in node 3 of plate 1 at 0.3 s into the transient, and is 280.3 °F. Table 5.7 above shows that the current analysis predicts 267 °F for this transient.

As summarized in the next section, there are considerable differences between the SAR RELAP5 model and assumptions, such as initial conditions. If there were agreement between the SAR results and the current results, it would be coincidental.

5.8.6 Changes since the MURR SAR

The SAR peak fuel temperature results identified in the previous section are considerably different in value core location, and the time that the peak occurred when compared to the current results, Table 5.7. This is to be expected given the many differences in the modeling assumptions. Key differences include:

Power Distribution and Initial Conditions

As described in Section 3.4 above, the power distribution and the initial conditions are considerably different from those of the SAR. The initial 30 °F higher core inlet temperature in the SAR will increase the reactor initial outlet temperature by essentially the same amount. This is particularly important in the double-ended cold-leg break because a higher outlet temperature should make cavitation at the core outlet in the cold-leg LOCA case substantially more severe.

Scram Delay Time

In the SAR analysis (page 13-29) the control blades start to move 0.1666 s after the break. This is considerably earlier than in the current representation of the reactor protection system, as described above in section 5.3.2.2. With the current analysis based on recent timing measurements of the electronic components, the scram occurs at 0.325 s for the cold-leg LOCA and at 0.875 s for the hot-leg LOCA. A later scram requires that the fuel absorb more energy during the upset and therefore causes higher fuel temperatures.

Core Coolant Channel Hydraulic Diameters

As described in Section 5.3.2.3 above, in the SAR model all of the core coolant channel hydraulic diameters were too large with the effect getting more pronounced with increasing flow area. This tended to incorrectly distribute the core flow among the coolant channels so that for the initial steady-state condition the channels near the flux trap received too little flow and those near the control blades received too much. Since the most limiting channels locations in the HEU core for the LOCAs is in the fuel plates near the flux trap, this would tend to make the peak fuel temperatures higher than they should be.

HEU Fuel Meat Heat Capacity

As indicated in Table 2.1 above, the HEU fuel meat heat capacity in the current analysis is considerably higher than in the SAR analysis. The temperature range of interest here is from the initial steady-state fuel temperature to the peak fuel temperature, or about 200 to 300 °F. In this range the fuel heat capacity is about 30% higher than was used in the SAR analysis. A 30% higher heat capacity enables 30% more decay energy to be absorbed by the fuel meat for the same fuel meat temperature rise. The aluminum clad and the coolant can also absorb substantial

amounts of decay energy. Clearly, a higher heat capacity causes lower fuel temperatures to be predicted.

5.8.7 Changes since the MURR Phase I Transient Analysis Report

As described in section 5.1 above, this report is the second phase of a two-phase effort to analyze transients in the MURR plant. The analysis of the LOCA transients provided in this report does not depend in any way on the analysis presented in the Phase I accident analysis report [4]. The second phase provided a valuable opportunity to rethink many of the modeling assumptions and to review and, in some cases, to obtain new measurements of some of the plant parameters used in the analysis. There are considerable differences in modeling assumptions between the two phases. These include:

- The LSSS reactor inlet coolant temperatures were used in both the Phase I and the SAR analysis rather than the 125 °F used in Phase II.
- As described in section 5.1 above, the Phase I analysis used the most extreme, highest heat flux HEU and LEU cores, 1B1 and 5B1, respectively, which are bounding, but not realistic. The Phase II analysis uses still conservative but closer to typical cores: reference Core 3A for the HEU and Core 7A for the LEU.
- The plant protection system delay times in Phase I, as indicated in Figures 3.5 through 3.10 of Reference 4, are considerably different from those used in the current analysis, Figure 5.3 through Figure 5.8. They were revised based on new measurement of the circuitry response times. A particularly important delay times is the scram delay, which is 0.400 s for the cold-leg LOCA in the Phase I analysis, but is 0.325 s in the current cold-leg LOCA analysis. In some instances the time until scram can have a substantial effect on the peak fuel temperatures.
- In Phase I, in the LEU models, but not in the HEU models, an error was made in which the fuel meat and aluminum clad thicknesses were inadvertently doubled. This was corrected in Phase II. As a verification calculation in Phase II, the thickness of the layers of the fuel plates were deduced from the RELAP5 output for a few limiting plates. This was to show that the sets of values provided in the core input were producing the desired fuel plate layer thickness in the RELAP5 model. Also, hand calculations of the temperature rises from the fuel plate surfaces to the center were made and compared with those produced by the RELAP5 code. The error in fuel meat thickness did not affect the power in the plate. However, the error did cause the initial steady-state fuel plate temperatures to be higher and the added heat capacity of the fuel plate caused the transient temperature rise to be lower.
- The Phase I analysis was performed with RELAP5-3D [29]. The Phase II analysis was performed with RELAP5/MOD3.3 [17]. As explained in section 4.2 of Reference 30, RELAP5/MOD3.3 has a newer low-pressure boiling model. This model better predicts void fractions in low-pressure boiling situations. The void fractions predicted by RELAP5/MOD3.3 can be considerably higher than those predicted by RELAP5-3D. The higher void fractions can result in lower flow rates. This, in turn, can result in more severe heat transfer conditions and higher fuel temperatures. The SAR analysis used the RELAP5/SCDAP code, which is described in Reference 7. It should be noted, however, that a later version, Mod 3.4(bi7), was used in the SAR analysis, as it was the production

version at the time. This code is believed to have a low-pressure boiling model that is similar to that in RELAP5-3D. At the outset of Phase II some of the Phase I LOCA analysis of the double-ended break at the cold leg was repeated with RELAP5/MOD3.3. Substantially higher fuel temperatures were observed. However, for the milder conditions assumed in the Phase II analysis, little difference between RELAP5-3D and RELAP5/MOD3.3 prediction are expected. In particular, the LEU EOL case of Table 5.6 was repeated with RELAP5-3D. Here again the peak fuel temperature occurred in node 3 of plate 22. In the RELAP5/MOD3.3 solution the peak fuel temperature was 314.13 °F and occurred at 0.340 s after the break. In the RELAP5-3D solution the peak temperature was 313.79 °F and occurred at 0.32 s after the break. (There is no data point between 0.32 and 0.33 s, where the peak must have occurred.)

5.8.8 Effect of Pressurizer Pressure on Peak Fuel Temperatures

In all of the LOCAs simulated, except the 2-inch break, the pressure drops to near atmospheric conditions within a few hundredths of a second. For the 2-inch break with the LEU core the peak fuel temperature is reached at 0.2 s and is 1 °F above the initial value. Then the fuel temperature drops rapidly and recovers to a value considerable below the peak. Thus, for all of the above LOCA cases small difference in initial pressurizer pressure should have essentially no effect on the peak fuel temperature. For the 2-inch break with the HEU core the peak fuel temperature is reached at 309 s and is influenced by the saturation temperature of the coolant, which is dependent on pressure. At 309 s the pressure in the core is about 35 psia. A 3 psi increase in pressure would raise the saturation temperature by 4.9 °F. A 3 psi decrease in pressure would lower the saturation temperature by 5.2 °F. Thus, the initial pressurizer pressure has a minimal effect of peak fuel temperature for this case.

5.8.9 New Homologous Pump Model (Not Used in the Section 5.7 Results)

The potential for the nuances in the primary pump model to affect the peak fuel temperatures was fully appreciated when it became necessary to explain the flow behavior in Figure 5.19 for the cold-leg LOCA. Until that point the primary pump model provided in the 2006 MURR SAR RELAP5 model was deemed sufficient. Since the MURR SAR [6] does not describe the model or provide a reference for it, it became necessary to develop a new model and to demonstrate that the results already presented would not be significantly affected by the replacement of the SAR model with a defensible new model of known origin. Therefore, pump data was obtained for the MURR primary pumps, the primary pump model used in RELAP5 to predict the result in section 5.7 was replaced with a new model, and the MURR RELAP5 model with the new primary pump model was used to simulate the HEU BOL and LEU EOL double-ended cold-leg LOCA, whose results are provided in Table 5.5 and Table 5.6, respectively. In each case, the fuel plate nodes that was found to be the most limiting one in the Table 5.5 or Table 5.6 simulation was examined. In each case the new simulation predicted a peak fuel temperature that was less than 0.2 °F below that of the old simulation. Thus, for all practical purposes the old and new predictions are the same. The details of the new pump model are provided in Appendix A. Hence, other than the test-case results in this paragraph, none of the LOCA/LOFA results in this report are based on the new model of the pumps.

5.9 Conclusions

The following four LOCAs were analyzed for both an HEU core and an LEU core:

1. Cold-leg LOCA – double-ended 12-inch diameter break at the core side of the V507B cold-leg isolation valve.
2. Hot-leg LOCA – double-ended 12-inch diameter break at the core side of the V507A hot-leg isolation valve.
3. Cold-leg LOCA – 8-inch diameter breach in the 8-inch cold-leg piping at the flow control diaphragm valve, V540 in Figure 5.1.
4. Cold-leg LOCA – 2-inch diameter breach in the 2-inch cold-leg piping that connects the pressurizer to the 8-inch cold-leg piping.

Six separate RELAP5 input models were created, three for the HEU core and three for the LEU core. Each model had only one MURR fuel element represented. In each, all of the element's coolant channels and fuel plates were explicitly and individually represented, as in the RELAP5 model used in the 2006 MURR SAR. In each set of three RELAP5 models there was one model with a BOL element, one with an MOL element, and one with an EOL element. Each element model was scaled up by a factor of eight to represent all eight elements in the MURR core. The initial steady-state flow rate of each of the eight MURR elements was determined in a separate spreadsheet model. In the RELAP5 models the individual BOL, MOL, or EOL element flow area and flow rate were each increased by a factor of eight.

In the six models, the axially varying heat flux distribution of each fuel plate of each fuel element was multiplied by the plate's azimuthal peak-to-average heat flux ratio so that the model represents the peak heat flux at each axial level of each fuel plate at full plant operating power of 10 MW for the HEU core and 12 MW for LEU core. The transient simulations are initiated from the conservative end of the normal operating band for both reactor primary coolant flow and inlet temperature.

The first listed LOCA was simulated with all six of the models. It was found that the BOL element RELAP5 model produced the highest fuel temperature among the three HEU models, Table 5.5, and that the EOL element RELAP5 model produced the highest fuel temperature among the LEU models, Table 5.6. Therefore these two-element models were used to study the remaining three listed LOCAs, Table 5.7 and Table 5.8.

For the double-ended cold-leg LOCA, the peak fuel centerline temperature for the HEU core was found to be 256 °F, which is only 30 °F higher than the initial steady-state temperature at the same location in the core. Similarly, the peak fuel centerline temperature for the LEU core was found to be 314 °F, which is only 24 °F higher than the initial steady-state temperature at the same location in the core. Thus, the difference between the HEU and LEU peak fuel temperatures is largely or entirely due to the difference in initial steady-state fuel temperature.

For the LEU core, the double-ended cold-leg LOCA produced the highest fuel temperature. For the HEU core the cold-leg LOCA caused by a 2-inch break produced the highest peak fuel

temperature of 267 °F, which is only 11 °F higher than in that for the double-ended cold-leg LOCA.

In conclusion, for all four LOCAs considered, both the HEU core and the LEU core have a substantial margin to fuel damage. Also, the effectiveness of the anti-siphon system has been demonstrated.

6.0 LOSS-OF-FLOW ACCIDENTS

6.1 Accidents to Be Considered

The MURR SAR considered five possible anomalies as potential initiators of loss of primary flow accidents (LOFA). These are:

- (a) Loss of facility electrical power (or coolant circulation pump power);
- (b) Inadvertent closure of coolant loop isolation valve(s);
- (c) Inadvertent loss of pressurizer pressure;
- (d) Locked rotor in a coolant circulation pump; and
- (e) Failure of a coolant circulation pump coupling.

The MURR SAR considered all five and concluded that the worst-case accident is (c). It presented results only for this upset. The SAR states: “The LOF accident caused by a loss of pressure in the primary coolant system pressurizer can be initiated by anomalies such as a break in any of the piping penetrations near the top of the pressurizer, a failure of the pressurizer relief valve, or pressurizer nitrogen vent valve V545 failing in the open position.” All of these pressurizer scenarios result in loss of pressure in the pressurizer, which causes a primary loop depressurization through the 2-inch diameter line that connects the pressurizer to an 8-inch pipe in primary loop, Figure 5.1 and Figure 5.2. In the MURR SAR, accident (c) was modeled in the same manner as the simulation of the cold-leg LOCA caused by a 2-inch break in the pressurizer line that is provided in Chapter 5 above. From a simulation perspective, this LOCA and the accident (c) LOFA are nearly the same. Differences are that 1) in the LOFA nitrogen is lost, but not coolant, and 2) the 2-inch break is about 12 feet closer along the 2-inch pipe to the main 8-inch primary header pipe where the pressurizer is connected to the primary loop than is the pressurizer.

The system depressurization accident (c) will cause the anti-siphon system to be activated and inject air into the top of the primary loop. Since there is no coolant lost in the case of the LOFA the long-term pressures may be a little higher than in a LOCA, as would be the long-term coolant temperatures. The higher long-term coolant temperatures should cause the long-term fuel temperatures to be a little bit higher, as well. Thus, the expected peak fuel temperatures for this LOFA are essentially those of a Cold-Leg LCOA 2-Inch Break (Figure 5.27) except that the longer-term temperatures are increased by a modest amount. Since this LOFA by no means rivals any of the LOCAs that have already been analyzed in Chapter 5 above, it will not be analyzed. Instead, accidents (a), (b), and (d) are analyzed.

The unique aspect to accident (c) is that the both the primary pumps and the pool pump coastdown at the outset of the accident. For accident (b) there is no reason to assume that both isolation valves will close simultaneously since they are independently controlled. Thus, accident (b) represents two separate accidents, one for the inadvertent closing of the inlet isolation valve, valve V507B in Figure 5.1, and one for the inadvertent closing of the outlet isolation valve, valve V507A in Figure 5.1. Intuitively, the case of both of the isolation valves closing simultaneously should be slightly worse than only one valve closing. In the model the flow area of valve V507A is reduced on a linear ramp and goes from its fully open to fully

closed position in 4.9 s. Valve V507B has the same behavior, except that it takes 5.0 s to fully close. Closing either valve stops the flow. Therefore, for modeling efficiency, in the initial simulation of accident (b) both isolation valves were assumed to close simultaneously. However, the timing of the primary pump coastdown, which can only be initiated by Trip 1 (core outlet pressure ≤ 26.0 psig), as seen in Figure 5.3, can influence the results. Closing the inlet isolation valve, but not the outlet one, may cause the coastdown to occur sooner. Therefore, the accident where only valve V507B closes was also studied for the both the HEU and LEU cores. The HEU case produced the same peak temperature at the same location, but at 16.9 s instead of 16.5 s. The LEU case produced a peak temperature of 293 °F instead of 294 °F. It occurred at the same location, but at 10.9 s instead of 10.6 s.

When accident (d) occurs the flow of one of two parallel main coolant pumps is suddenly lost. Since the RELAP5 model represents both coolants as a single pump, in the model the speed of this pump will be reduced to 60% of its initial speed in 0.001 s. The reason for the 60% value is provided in the next section. Accident (e) is similar to accident (d) in that in both accidents the flow of one of two parallel pumps is lost. In either case, backflow through the failed pump is avoided by a check valve at the pump outlet. In both cases the failure of the pump is almost instantaneous and results in a very quick drop in the combined flow of the two pumps. Thus, the simulation results for accident (d) bound those of accident (e).

6.2 Model

The models are the same unified RELAP5 input models that were described in Chapter 5. When those models were developed, Trip 99, as shown in Figure 6.1, was incorporated into them. The particular trip number, 99, was created for modeling convenience and has no relationship to any plant trip in the MURR plant. The steady-state RELAP5 solutions obtained for the initiation of the LOCA simulations are equally applicable to the initiation of the LOFA cause by Trip 99. The accident (a) simulation is accomplished by a RELAP5 restart problem in which Trip 99 is activated.

Figure 6.1. Trip 99: Site Electric Power is Lost

The RELAP5 simulation of accident (b) required adding two statements to the RELAP5 input restart file – one to trip the main inlet isolation valve (V507B) at the initiation of the transient and one to trip the main outlet isolation valve (V507A) at the initiation of the transient.

The RELAP5 simulation of accident (d) was accomplished by putting a revised version of the entire primary pump model in the RELAP5 input restart file. In this revised version the primary pump speed was specified so that at the start of the transient the primary pump speed was reduced from 1750 rpm to 60% of 1750, or 1050, rpm in a linear ramp over the first 0.001 s of the transient. The primary flow through the primary pump in the RELAP5 model leveled off at 63.2% of its initial value in the HEU case and at 62.7% of its initial value in the LEU case.

The choice of 60% is based on measured data in the MURR plant taken on May 26, 2014, in which one of the two parallel primary pumps was stopped. The measurement data indicate 3823 gpm through the pumps with both of the primary pumps operating and 2420 gpm, or 63.3%, with only one of the primary pumps operating. A value greater than 50% is to be expected since the flow area of the primary loop has not been halved or even reduced. The RELAP5 model value through the pump should be slightly higher than the measured 63% value because the in-pool heat exchanger valves open in the accident simulation and thereby reduce the primary loop hydraulic resistance. The measured data was taken with the in-pool heat exchanger valves closed, and so the 63% is slightly conservative in this respect.

Because the RELAP5 model for the LEU core is represented by an EOL element that has been scaled to represent eight EOL elements, the initial core pressure drop calculated by RELAP5 to be 22.5 psi is below the 24.8 psi setpoint of Trip 5 of Figure 5.6. This problem was corrected in the LEU locked-rotor analysis by resetting Trip 5 in this case to 17.5 psi, which is 5.0 psi below the initial core pressure drop. The RELAP5 results show that it takes only 0.0139 s to reach this trip. Based on the rate of peak fuel temperature rise of about 60 °F/s, each psi that the trip point is lowered would cause less than 0.2 °F increase in peak fuel temperature. Thus, the precise configuration of the pressure drop trip set point in MURR LEU cores after conversion is not significant to the results of the locked rotor LOFA. No adjustment was made to the core pressure drop trip in the HEU BOL case. Its initial core pressure drop was 31.6 psi, which is 6.8 psi above the trip.

6.3 Results

The same HEU and LEU core cases that were selected for RELAP5 simulation in Chapter 5 above were selected for the LOFAs, i.e., Core 3A for the HEU core and Core 7A for the LEU core. The HEU and LEU elements that were found in Chapter 5 above to be limiting for the cold-leg double-ended break LOCA were assumed to be limiting for the LOFAs. Hence, for the LOFA analysis HEU BOL element 5 and LEU EOL element 8 were analyzed with RELAP5.

Table 6.1 and Table 6.2 summarize the peak fuel temperature results for the HEU and LEU cases, respectively. For both the HEU and the LEU case, Figure 6.2 shows the peak temperature history at the core location where the peak occurred for the LOFA caused by a loss of site power. The initial temperature drop is due to the scram. The temperature recovery is due to the pump coastdown and the subsequent closing of the two main isolation valves, V507A and V507B. The

subsequent downturn in temperature between 10 and 25 s occurs at about the time that the coolant flows on either side of the limiting fuel plate are changing direction from downward flow to upward flow due to the flow circulation between the core and the in-pool heat exchanger. The flows in the adjacent channels reach a maximum in the upward direction between about 50 and 100 s and decline a little and then level off. This, with a time delay, is consistent with the temperature histories in Figure 6.2. Beyond the 500 s shown in the figure, the temperatures are on a gradual decline to the ends of the simulations at 2500 s.

Table 6.1. HEU (BOL) Peak Fuel Temperatures for Three Loss-of-Flow Accidents.

Accident*	Peak Temp. in Accident, °F	Location of Peak Temp., Plate, Node	Steady-State Temp. at Location of Peak, °F	ΔT at Location of Peak, °F	Time Peak Occurs, s
Loss of Site Power (a)	276	24, 3	203	73	22.1
Inadvertent Closing of Both Isolation Valves (b)	282	2, 2	199	83	16.5
Locked Rotor of One Primary Pump (d)	256	1, 3	226	20	0.35

*The letters in parentheses correspond to the accidents listed in Section 6.1.

Table 6.2. LEU (EOL) Peak Fuel Temperatures for Three Loss-of-Flow Accidents.

Accident*	Peak Temp. in Accident, °F	Location of Peak Temp., Plate, Node	Steady-State Temp. at Location of Peak, °F	ΔT at Location of Peak, °F	Time Peak Occurs, s
Loss of Site Power (a)	297	22, 3	290	7	14.5
Inadvertent Closing of Both Isolation Valves (b)	294	22, 3	290	4	10.6
Locked Rotor of One Primary Pump (c)	314	22, 3	290	24	0.35

*The letters in parentheses correspond to the accidents listed in Section 6.1.

Figure 6.3 is analogous to Figure 6.2, but is for the LOFA caused by the inadvertent simultaneous closing of both main isolation valves. The two figures are similar because both accidents are similar. In both the primary flow coasts down, the main isolation valves close, there is no system depressurization, and natural circulation between the core and the in-pool heat exchanger assists in long-term heat removal. In both accidents the peak fuel temperatures are reached after the isolation valves have closed. The HEU case for the inadvertent closing of the isolation valve is the only one for which the peak fuel temperature is at node 2 instead of node 3. Also, this temperature is the highest produced among the three HEU LOFA cases. The nodal location of the peak temperature is probably due to the upward natural convective flow, which enables the middle two fuel plate nodes to compete for most limiting. The flow reversal for the core coolant channels on either side of the limiting plate, plate 2, occur before 5.0 s and at about the same time that the flow reversal for the core occurs (at the inlet riser, branch 139 in Figure 5.2).

Figure 6.2. Peak Fuel Temperatures for a LOFA Caused by a Loss of Site Power.

Figure 6.3. Peak Fuel Temperatures for a LOFA Caused by an Inadvertent Closing of Both Isolation Valves.

Figure 6.4 shows the primary pump flow coastdown that is predicted by RELAP5 for each core for accident (d). In the simulation, the sudden severe reduction in pump speed causes a severe pressure oscillation in the primary loop, which damps out over time. The effects of this pressure oscillation can be seen in the pump flow coastdown.

Figure 6.4. RELAP5 Primary Pump Flow Coastdown for a LOFA Caused by a Locked Rotor of One Primary Pump (Simulated by a One-Pump Model with the Speed Suddenly Reduced to 60%)

Figure 6.5a and Figure 6.5b are the same except for the time scale. These figures show the peak fuel temperature histories for the sudden locking of the rotor of one of the two primary pumps. For the first second of the transient, Figure 6.5a looks very similar to Figure 5.18a, which is for the cold-leg LOCA caused by a double-ended break. The reason is that in both accidents there is an very sudden initial drop in flow. In the LOFA it is caused by a pump failure. In the LOCA it is caused by cavitation in the core outlet leg. The long-term temperatures in the LOFA are shown to level off at about 140 °F. This is because, as explained in Section 5.3.2.5, in the RELAP5 model the heat removal rate from the primary heat exchanger is reduced to a relatively very low value during the first 5 seconds of the transient and remains there. This is a very good assumption for a LOCA or LOFA where the isolation valves are fully closed after 10 s. However, this can be a pessimistic assumption of this LOFA since the secondary flow to the primary heat exchangers can be regulated to control long-term primary coolant temperatures.

The oscillation in fuel temperature seen in Figure 6.5b has about a one-minute period and damps out slowly. It is driven by an oscillation in the coolant temperature exiting the core, which oscillates in a similar manner, but with an initial minimum-to-maximum amplitude of about 10 °F or more. During the first 10 s of the transient, the coolant temperature at the core exit rises several degrees as a result of the initial sudden drop in flow and then drops about 20 to 25 degrees due to the scram. This variation in coolant temperature moves as a wave around the primary loop back to the core inlet with only modest attenuation. The time for the wave to traverse the primary loop can be estimate by dividing the volume of water in the loop (about 2000 gallons) by the flow rate (about 2000 gpm). Thus, the wave traverses the primary loop in about a minute, which matches the period of the temperature oscillation. It takes the coolant several traverses of the primary loop for the oscillation in coolant temperature to damp out

Figure 6.5a. Peak Fuel Temperatures for a LOFA Caused by Locked Rotor of One Primary Pump.

Figure 6.5b. Peak Fuel Temperatures for a LOFA Caused by Locked Rotor of One Primary Pump.

completely. In the simulation there is heat transfer through the in-pool heat exchanger, the reactor vessel walls, and the wall of the pipes that are in the pool. The in-pool heat exchanger flow is driven by the pump rather than by natural circulation.

The HEU locked rotor case was also executed with the final pump speed set to 50% of the initial value, or 875 rpm. This caused the primary flow rate to level off at 52.5% and the peak fuel temperature to be 250 °F, instead of 246 °F.

6.4 Discussion

In all three LOFAs the system pressure is maintained with no loss of coolant. This minimizes the potential for severe boiling leading to critical heat flux. In both the loss of site power LOFA and the one that is due to an inadvertent closing of the isolation valves, the in-pool heat exchanger is fully effective in the long-term removal of decay heat. Although not demonstrated by the simulations, in the locked-rotor LOFA the in-pool heat exchanger is not needed for long-term heat removal because the primary heat exchangers are functional. The peak fuel temperature for each of the LOFAs is governed by power-to-flow ratio and should not be affected by small changes in pressurizer pressure.

The highest HEU LOFA temperature, 282 °F, Table 6.1, is higher than its LOCA counterpart of 267 °F, Table 5.7. The highest LEU LOFA temperature, 314 °F, is the same as the highest LOCA temperature. In both LEU transients this peak temperature occurred during the first 0.35 s of the transient and is caused by a rapid reduction in primary flow.

The highest LOFA temperature for each core can be compared with the highest initial steady-state fuel temperature for the core. Figure 5.16 shows that the highest HEU steady-state fuel temperature occurs in node 3 of plate 1 and has a value of 226 °F. Similarly, based on Figure 5.17, the highest LEU steady-state temperature of 290 °F occurs in node 3 of plate 22. Thus, the peak LOFA accident temperature is only 56 °F above the maximum steady-state value for the HEU core and 24 °F above the maximum steady-state value for the LEU core.

Lastly, the effect of the coolant channel reduction on the peak fuel temperature in the LOCA and LOFA accidents was evaluated. The transient LOCA and LOFA results provided in the tables of Chapters 5 and 6 are based on the assumption of maximum channel gap thickness reduction due to burnup in the EOL elements – 10 mils in the HEU core and 8 mils in the LEU core. As explained at the end of Section 5.5, the use of the maximum coolant channel reduction in the EOL elements could cause the predicted peak fuel temperatures for the BOL elements to be too low. For the LEU core, this assumption causes the flow per element in the EOL element to be 6.6% below average and the flow for the BOL to be 6.8% above average. All four LEU MOL elements have flows that are within 1% of the average, with two above and two below. The situation is the same for the HEU core except that the EOL elements have flows that are 9.6% below the average per element and the BOL element have flows that are 10.0% above the average. In the one-element models, which use eight times the flow of one element to represent all eight, this redistribution of flow increases the initial steady-state flow rate in the RELAP5 solutions for BOL elements and decreases them in the solutions for EOL elements. If the reduction in gap thickness due to burnup in the EOL elements is less than assumed, then the peak fuel temperatures of the EOL elements would decrease and those of the BOL elements would increase. The effect on MOL elements would be minimal.

Additional cases were analyzed to assess the effect of the channel gap reduction with burnup. For these additional cases it was assumed that there was no channel reduction in any of the eight core elements due to burnup. For the new analysis with no channel reduction, in the separate hydraulics network model all four flow paths were assumed to have the hydraulic diameter and the flow area of the BOL path. For both the HEU and the LEU core the total flow rate to be used in the RELAP5 input when analyzing a BOL element was calculated to be above 3700 gpm by less than 0.05%. The reason it was not exactly 3700 gpm is due the BOL path having a slightly higher than core-average coolant temperature. Therefore, in the new analysis with no channel reduction, 3700 gpm was assumed in the RELAP5 one-element models.

In the results presented in Chapters 5 and 6, the highest HEU fuel temperature of 282 °F occurred in a BOL element during the LOFA due to the inadvertent closing of both isolation valves. The BOL element was analyzed again, with the one-element model, but with the total flow reduced to 3700 gpm. In the case with no channel thickness reduction due to burnup, the peak occurred at the same location as before, but 0.3 s earlier and 1 °F higher. However the steady-state temperature at that location was 4 °F higher than before. Therefore the transient temperature rise is slightly less with no channel thickness reduction. Thus, the increase in the peak HEU fuel temperature due to the uncertainty in channel reduction due to burnup is no greater than 1 °F and can be ignored.

The highest LEU fuel temperature of 314 °F occurred in an EOL element during two transients – the LOCA due to the double-ended break of the inlet coolant pipe and the LOFA due the locked rotor of one of the two main pumps. In a core with no channel thickness reduction due to burnup the peak fuel temperatures in EOL elements can only decrease since these elements will have more coolant flow. However, the peak fuel elements of the BOL elements can only increase. This potentially can enable the BOL elements to become limiting. Therefore, the two limiting transients were analyzed with the LEU BOL version of the one-element model. The initial total core flow rate was assumed to be 3700 gpm.

Chapter 5 provides the peak fuel temperature for the LEU BOL element during a LOCA due to the double-ended break of the inlet coolant pipe. This peak temperature is 289 °F. The lower flow in the a case with no channel reduction caused this peak to occur 0.01 s earlier, at the same location, and to be 3 °F higher, i.e., 292 °F. The initial steady-state temperature at that location was 5 °F higher than before. Since the new BOL value of 292 °F is less than the Chapter 5 EOL value of 314 °F, the core peak fuel temperature occurs in the EOL element and is predicted to be no greater than 314 °F for LEU LOCAs whether or not a channel reduction is assumed.

Chapter 6 does not provide LOFA results for LEU BOL elements. In a new analysis with no channel reduction of the LOFA due to the locked rotor of one of the two main pumps the peak fuel temperature was found to be 302 °F. It occurred in node 3 of plate 23 at 0.35 s into the transient. The initial fuel temperature at that location was 273 °F. As in the earlier analysis, the core pressure drop trip was set at 5.0 psi below its initial value. Its initial value in the new analysis was 22.7 psi. The new BOL value of 302 °F is less than the highest LEU LOFA temperature of 314 °F, as seen in Table 6.2. Thus, the core peak fuel temperature predicted to be no greater than 314 °F for LEU LOFAs whether or not a channel reduction is assumed.

6.5 Conclusions

All three LOFAs that were analyzed – the loss of site power, the inadvertent closing of both main isolation valves, and the locked rotor of one of two primary pumps – result in very mild thermal-hydraulic conditions for the fuel. The system pressure is maintained and the predicted peak fuel centerline temperature is only 282 °F (139 °C) for the HEU core and 314 °F (157 °C) for the LEU core, which are only 56 and 24 °F (31 and 13 °C) above the peak initial steady-state fuel temperatures for the HEU and LEU cores, respectively.

7.0 MAXIMUM HYPOTHETICAL AND FUEL HANDLING ACCIDENTS

The analyses of the reactivity insertion and loss-of-coolant/loss-of-flow accidents showed that no fuel damage is expected if these accidents were to occur. Consequently, there is no anticipated release of radioactive material as a result of these postulated accidents which would contribute to a radiation dose for MURR Operations personnel or any person outside of the MURR containment.

In this chapter, two additional accident scenarios are considered in which radioactive material is released. The corresponding radiation doses to workers and the general public are computed.

7.1 Maximum Hypothetical Accident

The modeling and analyses of the Maximum Hypothetical Accident (MHA) are described in detail for the HEU- and LEU-fueled cores in Reference 6 and 31, respectively. A summary description of the accident scenario, analysis, and consequences is provided here.

The MHA assumes conditions leading to consequences worse than those from any credible accident. In the MHA for the MURR, it is assumed that some accidental condition has caused fuel plates in the operating core to melt. The conditions that lead to this event are considered immaterial to the analysis. However, because of differences in the design and operating characteristics between the current HEU and the proposed LEU fuel elements, a different source term of radioactive material is considered in the accident analysis for the two cores. Specifically, the MHA for the HEU fuel assumes that four number-1 fuel plates in four separate elements melt. The number-1 plates are in the peak heat flux region of the HEU core. For the LEU core, the MHA assumes that four number-23 fuel plates in four separate elements melt. The number-23 fuel plates are in the peak heat flux region of the LEU core.

The radioactive material source term for the MHA was calculated by assuming full-power operation of the MURR core in twelve 10-day cycles over a 300-day period. The HEU core power level is 10 MW, so that the fission product inventory in the core corresponds to a total core burnup of 1,200 MWd. This will give a conservative radioactive source term for the accident analysis, since the typical HEU core burnup in the MURR is only about 650 MWd. A fuel cycle simulation for the MURR operating with the proposed LEU fuel showed that the weekly operations cycle for the converted core will be the same as for the current HEU fuel [1]. Thus, the radioactive source term for the MHA in the LEU core is still computed based on twelve 10-day cycles, but the core power level will be increased to 12 MW, which is the power uprate necessary to maintain the MURR experimental performance with the LEU fuel. Consequently, the fission product inventory will correspond to a core burnup of 1,440 MWd, while the LEU core burnup in typical weekly operations is anticipated to be approximately 750 MWd.

The whole-core fission product inventory for the HEU core MHA analysis in the SAR [6] was calculated using the ORIGEN code [32]. ORIGEN is a zero-dimensional isotope generation and depletion analysis code. The code is provided with several one-group cross-section libraries that are applicable to a variety of reactor types, including light-water moderated reactors, but none of

these prepared libraries were generated specifically for the MURR. However, the user does have the option of providing replacement cross-sections for any nuclides that are considered important for properly calculating reaction rates.

For the LEU core MHA analysis, the whole-core fission product inventory is calculated using the MONTEBURNS code [33, 31]. MONTEBURNS uses the space- and energy-dependent neutron flux calculated by a detailed MCNP model of the reactor to generate a one-group cross-section library needed by the ORIGEN depletion module. In this way, the one-group cross-sections used in the ORIGEN model are computed based on the neutron spectrum in the core at each depletion state point. The user does not need to compute a set of replacement cross-sections in advance. After each depletion step, the MCNP model is updated with a set of material cards that reflect the depleted fuel compositions. .

The calculated whole-core radioactive inventory for the HEU and LEU cores used for the MHA analysis is presented in Table 7.1. The radioactive fission product inventory is higher in the LEU core for all radioisotopes considered due to the 20% higher power level and/or burnup, as well as differences in the neutron spectrum in LEU vs. HEU fuel, cross-section data, and solution methods, as discussed above.

Table 7.1. Whole-Core Activity of Radioiodines and Noble Gases for MHA Analysis (Ci).

		HEU [6]	LEU
Iodines	¹³¹ I	1.7E+05	2.78E+05
	¹³² I	3.3E+05	3.77E+05
	¹³³ I	5.1E+05	6.60E+05
	¹³⁴ I	6.3E+05	7.41E+05
	¹³⁵ I	5.2E+05	6.15E+05
Noble Gases	⁸⁵ Kr	4.7E+02	5.45E+02
	⁸⁵ Kr _m	1.1E+05	1.53E+05
	⁸⁷ Kr	2.1E+05	2.43E+05
	⁸⁸ Kr	3.0E+05	3.44E+05
	⁸⁹ Kr	3.8E+05	4.36E+05
	⁹⁰ Kr	3.8E+05	4.34E+05
	¹³³ Xe	4.2E+05	4.67E+05
	¹³⁵ Xe	9.6E+04	1.65E+05
	¹³⁵ Xe _m	9.4E+04	1.03E+05
	¹³⁷ Xe	4.9E+05	5.84E+05
	¹³⁸ Xe	5.2E+05	6.03E+05
	¹³⁹ Xe	4.2E+05	4.89E+05

The source term for the MHA is based on the fraction of the whole-core inventory of radioactive material that is contained in the fuel plates that are damaged in the accident. For the HEU core, this fraction was calculated based on the fuel meat mass in the four number-1 plates that are assumed to melt and the power peaking factor in those plates. The number-1 plates are adjacent to the center flux trap. Reference 6 states that the four plates in the number-1 position comprise 1.3% of the fuel mass in the HEU core. A power peaking factor of 1.6 was applied to account for the fission rate and burnup of the number-1 plates, yielding 2.1% (1.3×1.6) as the fraction of the radioactive material in the whole core released in the MHA. The release of radioisotopes of krypton, xenon, and iodine are the major sources of radiation exposure to personnel in the containment building and will, therefore, serve as the basis for the source term for the dose calculations.

The source term for the MHA in the LEU core can be calculated in a similar fashion to that done for the HEU core. The peak heat flux in the proposed LEU element occurs in plate number-23, which is adjacent to the beryllium reflector. Thus, the MHA will be analyzed assuming that four number-23 plates in the LEU core melt, which contain 2.72% of the fuel mass in the core. A radial power peaking factor of 2.01 for plate 23 was obtained from calculations performed in Reference 1 and applied to account for the fission rate and burnup of the number-23 plates, yielding 5.47% as the fraction of radioactive material in the whole core released in the MHA. Again, the radioisotopes of krypton, xenon, and iodine will serve as the basis of the source term.

The MHA is assumed to occur with the primary coolant system operating, resulting in a quick dispersal of all fission products in the melted fuel plates throughout the system. It is conservatively assumed that 100% of the radioiodine and noble gas fission products from the affected plates are instantaneously and uniformly dispersed within the 2,000-gallon primary coolant system volume. There is a low leakage rate of coolant from the primary coolant system into the reactor pool, providing a path for Operations personnel in the containment building to be exposed to radioactive material from the MHA. This leakage is typically less than 40 gallons per week. However, for the purpose of the MHA analysis, a conservative leakage rate of 80 gallons per week (7.9×10^{-3} gpm) from the primary system to the pool was assumed.

7.1.1 Occupational Dose

For calculating the occupational dose to Operations personnel as a result of the MHA, it is assumed that workers remain in the containment building for 10 minutes after the start of the accident, during which the workers would secure the primary coolant system and perform evacuation procedures. During this 10-minute interval, the radioiodines that transfer from the primary coolant system into the pool are conservatively assumed to instantaneously and uniformly mix into the 20,000 gallons of bulk pool water. When the reactor and containment building ventilation system are in operation, the evaporation rate from the reactor pool is approximately 80 gallons of water per day (5.5×10^{-2} gpm). For the purpose of the MHA, the assumption is that a total of 40 gallons of pool water containing the radioiodines released from the fuel evaporates over 10 minutes into the containment building. The radioiodines are then assumed to uniformly mix into the containment building air volume of 225,000 ft³. The noble gases (krypton and xenon) released from the primary coolant system over the 10 minute interval are assumed to pass immediately through the pool water and enter the containment building air

volume. None of the assumptions related to the leakage rate of primary coolant to the pool, the pool evaporation rate, or the exposure time for Operations personnel will be affected by the conversion to LEU fuel.

The objective of the analysis is to present a worst-case dose assessment for a person who remains in the containment building for 10 minutes following the MHA. Submersion in the airborne noble gas and iodine radionuclides inside the containment building will result in a whole-body radiation dose, as well as a thyroid dose from inhalation of the radioiodine. Since decay of the radioisotopes is neglected in the analysis, the calculated airborne concentration will increase linearly over the 10 minute interval. Therefore, the average concentrations of the noble gas and iodine radionuclides for the dose calculations are best represented by the concentration existing at 5 minutes after the onset of the MHA.

The external dose due to submersion is computed using the Derived Air Concentration (DAC) for radionuclides reported in Table 1 of Appendix B of 10 CFR 20 [34]. However, four radionuclides in the MHA source term do not have DAC values published in 10 CFR 20. These are ^{89}Kr , ^{90}Kr , ^{137}Xe , and ^{139}Xe . For these nuclides, DAC values equivalent to those utilized in the HEU MHA analysis for the MURR SAR [6] were employed. The DAC values for ^{89}Kr , ^{90}Kr , and ^{137}Xe used in the MURR SAR were derived from a release of 10 CFR 835 prior to 2007, while the DAC value for ^{139}Xe was taken from Appendix B of 10 CFR 20 for “unlisted nuclides.” [31]

Table 7.2 summarizes the results of the dose to Operations personnel calculated for the HEU and LEU MHA. The HEU results are from Reference 6. The occupational doses resulting from the MHA in the LEU core are a factor of 3 to 4 higher than for the HEU core. This is because the assumed fraction of the core inventory contained in the damaged fuel plates is about a factor of 2.5 higher for the LEU core, and the 20% power uprate for the LEU core increases the core burnup and the whole-core fission product inventory. The results in Table 7.2 show that the occupational doses are well within the published regulatory occupational limit of 5 rem for the total effective dose equivalent (TEDE) for both HEU and LEU cores.

Table 7.2. 10-Minute Occupational Dose from Radioiodines and Noble Gases in Containment from MHA.

	HEU	LEU
Committed Dose Equivalent to Thyroid, mrem	7.5	29.6
Committed Effective Dose Equivalent to Thyroid, mrem	0.23	0.89
Deep Dose Equivalent from Radioiodines, mrem	0.052	0.16
Deep Dose Equivalent from Noble Gases, mrem	132	402
Total Effective Dose Equivalent (TEDE) from Radioiodines, mrem	0.28	1.05
Total Effective Dose Equivalent (TEDE), mrem	132.28	403.05
Annual Occupational Limit, mrem	5,000	5,000

Table 1 of Appendix B of 10 CFR 20 provides default DAC values for any “unlisted radionuclides” for which explicit data are not available in the table. This would include ^{89}Kr , ^{90}Kr , ^{137}Xe , and ^{139}Xe which are part of the MHA source term. If, instead of the DAC values utilized in the MURR SAR analysis, the default values for these nuclides are utilized in

the dose calculations, the TEDE is calculated to increase from 403 mrem to 1,403 mrem. This dose is still below the regulatory limit of 5 rem (5,000 mrem) published in 10 CFR 20.

It is also worth noting that the expected evacuation time for most occupants of the containment building is 2 minutes, instead of the 10 minutes assumed in the dose calculations. In that event, the worker will receive a much smaller dose due to two effects. First, there will be a 5-fold reduction in dose due to the shorter occupancy time (2 minutes vs. 10 minutes). Additionally, since the concentration of the radionuclides in the containment air increases linearly from the onset of the MHA, the average concentration to which the worker is exposed will also be reduced by a factor of 5. The combination of these two effects reduces the dose to Operations personnel who evacuate within 2 minutes by a factor of 25 from the calculated values summarized above and in Table 7.2.

7.1.2 Public Dose

To develop what would clearly be a worst-case scenario, the MHA analysis assumes that there is some leakage of the air from the isolated containment building and the outside atmosphere. It is highly probable that there will be no positive pressure differential between the inside of the containment building and the outside atmosphere, and consequently there will be no air leakage from the building to the outside environment, and consequently no radiation dose to members of the general public in the unrestricted area. Nonetheless, the assumption is made that an atmospheric pressure drop of 0.7 inches of Hg (0.33 psi) occurs at the onset of the MHA. The resulting pressure differential will result in an average leak rate of 5.2 scfm over a 16.5 hour period before the containment building pressure is equalized with the atmospheric pressure. Calculations were performed to compute the dose to individuals at various distances from the containment building during the entire 16.5 hour period and under the most conservative (worst-case) meteorological conditions to obtain the maximum public dose.

The concentration of noble gas and iodine radionuclides in the containment building air can be calculated in the same manner as described in the previous section. It is further assumed that 100% of the noble gases in the containment air are released to the environment. However, experimental data have shown that 75% of the radioiodine in the containment air is deposited in the containment, so that only 25% of the radioiodine is released to the environment.

The point of maximum dose for a member of the general public in the unrestricted area was found to be 760 m north of the containment building. Table 7.3 summarizes the TEDE calculated for the HEU and LEU MHA cases at that location. For the HEU MHA, the dose is taken from Reference 6. In that analysis, the concentration of radioiodines and noble gases leaking from the containment to the environment was calculated at 10 minutes after the initiation of the MHA, based on the assumption that Operations personnel secure the primary coolant system before evacuating the containment, which would stop any further leakage of contaminated primary system coolant into the pool, and consequently into the containment air, and ultimately into the environment. The same approach was used for the LEU case, yielding a dose of 0.05 mrem.

Table 7.3. Maximum Dose in the Unrestricted Area for 16.5 Hours Following the MHA.

	HEU	LEU		
		<i>DACs from Appendix B of 10 CFR 20 and Reference 6¹</i>		<i>DACs from Appendix B of 10 CFR 20</i>
Time from initiation of MHA at which concentration of radioiodines and noble gases leaking from containment are calculated	10 minutes	10 minutes	8.25 hours (Average of 16.5 hours)	8.25 hours (Average of 16.5 hours)
TEDE, mrem	0.03	0.05	2.45	8.58

¹ DAC values for ⁸⁹Kr, ⁹⁰Kr, ¹³⁷Xe, and ¹³⁹Xe are not published in Appendix B of 10 CFR 20. For these nuclides, DAC values equivalent to those utilized in the HEU MHA analysis [6] were employed. For all other radionuclides in the MHA analysis, DAC values from Table 1 of Appendix B of 10 CFR 20 were used. See discussion in Section 7.1.1 for more detail.

The public dose was also calculated for the case in which contaminated primary system coolant continues to leak into the pool at the rate of 80 gallons per week (7.9×10^{-3} gpm) throughout the accident, which would occur if Operations personnel were unable to secure the primary system before evacuating the facility. Consequently, the concentrations of radioiodines and noble gases in the containment building would continue to increase throughout the period of the accident. Thus, the average concentration of radioiodines and noble gases in the containment over the 16.5 hour time-period from the start of the MHA to when leakage to the environment ceases due to pressure equalization (i.e., the concentration at 8.25 hours) was used for calculating the dose to the general public. Because of the much longer time assumed for calculating the concentrations of radioactive isotopes in the containment air, the calculated dose in the unrestricted area is about a factor of 50 greater (2.45 mrem vs. 0.05 mrem). Lastly, results were calculated using the default DAC values for unlisted radionuclides from Table 1 of Appendix B of 10 CFR 20 for ⁸⁹Kr, ⁹⁰Kr, and ¹³⁷Xe, and ¹³⁹Xe, yielding a maximum dose of 8.58 mrem.

10 CFR 20 provides a guideline for the TEDE to members of the general public of less than 100 mrem annually, and 2 mrem/hour. The calculated maximum TEDE for a member of the general public resulting from the MHA is well below the annual limit, and is also below the hourly dose limit. Additionally, this dose assumes very conservative atmospheric conditions and a conservative burnup on the core to produce the radioactive inventory that was used to calculate the dose [31]. Lastly, the dose to the general public can be greatly mitigated with the order of a site evacuation by MURR.

7.2 Mishandling or Malfunction of Fuel Accident

Fuel handling accidents (FHA) cover a class of accident caused by events or scenarios that could cause a breach in the fuel cladding. Events which could cause an accident in this category include 1) a fuel handling event where a fuel plate is damaged or scratched severely enough to breach the cladding, 2) simple failure of the fuel cladding due to a manufacturing defect or corrosion, or 3) overheating of a fuel element with subsequent potential cladding failure due to a

reactivity insertion accident (RIA), loss-of-primary-coolant accident (LOCA), or loss-of-primary-coolant-flow accident (LOFA). The modeling and analyses of the FHAs are described in detail for the HEU- and LEU-fueled cores in Reference 6 and 35, respectively. A summary description of the accident scenario, analysis, and consequences is provided here.

7.2.1 Damage to a Fuel Element Due to Mishandling

All fuel handling is performed in accordance with Special Nuclear Material (SNM) Control and Accounting Procedures as outlined in the Operations Procedures. Irradiated fuel is handled with a specially designed remote tool that provides a positive indication of latching prior to movement of a fuel element. This feature is tested prior to any fuel handling sequence. Fuel elements are always handled one at a time so that they are maintained in a criticality-safe configuration.

New or irradiated fuel may be stored in any one of the 88 in-pool storage locations (not including the core). These storage locations are designed to ensure a geometry such that the calculated k_{eff} is less than 0.9 under all conditions of moderation, thus allowing sufficient convection cooling and providing sufficient radiation shielding.

Irradiated fuel does not leave the facility until it is loaded into a U. S. Nuclear Regulatory Commission (NRC) approved cask for shipment. An irradiated fuel element is not loaded into a shipping cask until a predetermined cooling period has elapsed. The cooling time ensures that a fuel element has decayed to a level where air cooling is adequate to maintain the fuel temperature below the design limits. Thus, in the event of a dropped cask or a loss of coolant water from the cask, the fuel element would not release fission products by a meltdown.

In accordance with the intent of the Phase II accident analyses, the possibility of dropping a fuel element while underwater and damaging it severely enough to breach the fuel cladding was considered. Only the innermost and outermost plates of a fuel element are exposed to receive any potential damage. The outermost plate was considered to be damaged with a release of fission products directly into the pool. In 34 years of operating history, MURR Operations personnel have completed approximately 35,000 fuel element handling moves. Only once has a fuel element been dropped, which resulted in the outermost fuel plate (plate 24 of an HEU element) being bent. There was no measurable release of radioactive material from that event and the element was retired. Based on that experience, it is conservatively assumed in the analysis of this type of fuel handling accident that the cladding on the outermost plate of the fuel element (plate 23 of the LEU element) is damaged and a section of fuel meat that is one-inch square and 5 mils thick is exposed, corresponding to 0.248 grams of ^{235}U . The plate is also conservatively assumed to have the same power history as the MHA.

The quickest time a fuel element can be brought to the reactor pool would be about an hour (60 minutes) after reactor shutdown. To be conservative, the accident assumes only a 30 minute decay of the fuel element. Table 7.4 provides a comparison of the radioiodine and noble gas activities per gram ^{235}U in the LEU fuel element at the end of irradiation, and after 30 minutes and 60 days of post-irradiation decay. The end of irradiation specific activity was calculated using the data in Table 7.1 and dividing by the fresh ^{235}U inventory of 12,054 grams. The

specific activity following decay was obtained from a spreadsheet model and confirmatory hand calculations to compute the radioisotope concentrations following 30 minute's decay.

Table 7.4. Specific Activity of Radioiodines and Noble Gases in LEU Fuel (Ci/gm-²³⁵U).

		End of Irradiation	30 Minutes Post- Irradiation Cooling	60 Days Post- Irradiation Cooling
Iodines	¹³¹ I	23.1	23.0	0.129
	¹³² I	31.3	26.9	
	¹³³ I	54.8	53.8	
	¹³⁴ I	61.5	41.4	
	¹³⁵ I	51.0	48.4	
Noble Gases	⁸⁵ Kr	0.0452	0.0452	0.0447
	⁸⁵ Kr _m	12.7	11.7	
	⁸⁷ Kr	202	153	
	⁸⁸ Kr	285	253	
	⁸⁹ Kr	362	0.0491	
	⁹⁰ Kr	360	4.20E-16	
	¹³³ Xe	38.7	38.6	0.0139
	¹³⁵ Xe	13.7	13.2	
	¹³⁵ Xe _m	8.54	2.20	
	¹³⁷ Xe	48.4	2.09	
	¹³⁸ Xe	50.0	11.4	
	¹³⁹ Xe	40.6	1.15E-12	

It is further assumed that the noble gases in the exposed part of the fuel plate are released directly into the containment from the pool, while the radioiodine mixes uniformly into the 20,000 gallons of pool water, followed by an evaporation rate of 20 gallons from the pool into the containment over a 5 minute period. This is the same evaporation rate as assumed for the MHA (40 gallons in 10 minutes). It should be noted that a 5-minute exposure is assumed for this accident, which is less than the 10-minute exposure assumed in the MHA. The reason for this is that in the case of the FHA, there is no need for Operations personnel to secure the primary system, so there will be a more rapid evacuation time in this accident scenario. The dose to a worker in the containment for 5 minutes following the accident was calculated to be 349 mrem if the DACs from the HEU SAR are utilized, and 391 mrem if the default DACs from 10 CFR 20 are used. These doses are encompassed by the MHA.

The FHA with the exposure of a one-inch square by 5 mil thick section of fuel meat was also analyzed for not requiring containment integrity for handling fuel with decay greater than 60

days. The specific activity for the spent fuel after 60 days post-irradiation cooling is provided in Table 7.4. Note that most of the iodines and noble gases used in the MHA analysis and previous FHA analysis will have decayed to very low concentrations due to their short half-lives, and thus were not used in this analysis. This analysis was performed by MURR in response to a “request for additional information” in the NRC review of the previous SAR [23].

For this analysis, the exposure time is set to 60 minutes. It is assumed that a one-inch square by 5 mil portion of the fuel meat is exposed. As for the previous analysis, it is assumed that the noble gases in the fuel meat are released directly into the containment and form a uniform concentration. It is conservatively assumed that 80 gallons of pool water contaminated with radioiodine will evaporate into the containment during the 60-minute exposure time. 80 gallons is approximately the daily evaporation of pool water. The radioiodine in this pool water is assumed to uniformly distribute in the containment. The occupational dose to a worker present in the containment for one hour with a breached fuel element with 60 days of decay in the HEU-SAR was calculated to be 0.79 mrem. For the LEU fuel, the dose is calculated to be 0.90 mrem. The final values are well within the published regulatory limit and, in fact, less than 1% of any annual occupational limit

It should be noted that the thickness of the cladding on the LEU plate 23 is 16 mil, which is thicker than the 15 mil cladding on the HEU fuel plates. The thicker cladding would decrease the likelihood of fuel release from the mishandling accident. However, no credit was taken for the thicker cladding on the LEU fuel plate.

7.2.2 Fuel Element Malfunction Due to Cladding Deformation or Corrosion

Corrosion can result in aluminum cladding failure in two ways: pitting and oxide film formation. Oxide film forms as a result of essentially uniform corrosion, while pitting occurs when the corrosion rate is accelerated in a local area. Pitting corrosion is not catastrophic in nature and can be detected by conventional monitoring techniques in place at the MURR. To place an upper limit on a pitting event with the same rationale from the MURR SAR (Ref. 13.26 within the SAR), the pit diameter is six (6) times the depth, the surface area of a pit where it penetrates the 0.016-inch fuel plate cladding would be 0.096 inches in diameter. To be conservative, the analysis assumes a pit diameter of 0.20 inches, and an instantaneous release of fission products (100% release fraction) from the fuel meat volume equal to the pit area times the fuel meat thickness (0.017 inches). This volume contains 0.027 grams of ^{235}U compared to the 327.87 grams contained in the number-23 plates of four fuel elements. Based upon these assumptions, the release would be equivalent to 8.23×10^{-5} times the release of the MHA. A ten (10) minute exposure in containment would be less than 1 mrem for both MURR SAR and Default DACs, demonstrating a pit being well encompassed by the MHA.

7.2.3 Overheating of Fuel with Subsequent Potential Cladding Failure

Overheating of the MURR fuel with subsequent potential cladding failure has been analyzed in detail in Chapters 4 through 6 for RIAs, LOCAs, and LOFAs. All of these analyses concluded that there are no operational or environmental impacts from these accidents, as they did not result in any fuel melting or fission product release, and are easily bounded by the MHA.

7.2.4 Conclusions

Three scenarios were proposed for this section. In each case, the analysis of the accident conditions and consequences led to a conclusion that the risks were measurably less than, and well bounded by, the previously analyzed MHA.

8.0 CONCLUSIONS

Safety analyses of the MURR core fueled with a monolithic alloy of uranium and 10 wt% molybdenum (U-10Mo) were previously conducted to establish the steady-state safety basis of the reactor using the CD35 fuel element design [1, 2]. The thermal-hydraulic analyses of the reactor required detailed power distributions and geometrical considerations in order to identify regions of interest for safety analyses. In order to understand the impacts of accident and transient scenarios in a core fueled with an LEU U-10Mo element, a set of accident analyses have been performed.

Insertion of uncontrolled and/or unanticipated positive reactivity to either a critical reactor or during a reactor startup is one of the nine (9) postulated accident-initiating scenarios required to be analyzed for potential radiological consequence per the NUREG-1537 guidelines [3]. Following the guidance provided in NUREG-1537, two different accident scenarios for an insertion of positive reactivity were evaluated for reference HEU and LEU (CD35) cores. The HEU analyses were for 10 MW initial steady-state power, while the LEU analyses were for 12 MW initial steady-state power. Several positive step reactivity insertions, based upon the various maximum reactivity limits given in the MURR Technical Specifications, were considered. Additionally, a continuous ramp insertion of positive reactivity based on the simultaneous continuous withdrawal of MURR's four shim control blades was considered. The exact mechanisms or events that could cause these reactivity insertions can vary, but could include an inadvertent rapid insertion or removal of an experiment from the center test hole, reactor or equipment malfunction, or operator error. Preliminary analyses of these accidents were performed to assess the accident consequence for fresh fuel plates only [4]. In the current work, fresh and depleted fuel plates were modeled. Furthermore, unnecessary conservatism were eliminated from the modeling, where such enhancements are reasonable and clearly defensible. The reactivity insertion transient analyses were performed using the PARET/ANL code.

For the most limiting reactivity insertion accident, which is a 0.006 $\Delta k/k$ step insertion of reactivity in a reference critical core at full power, the peak HEU U-Al_x fuel meat temperature is calculated to be 194 °C (381 °F) in a fresh fuel plate. In various cases with uncertainties, the peak fuel temperature rose to 203 °C (397 °F).

The same accident is also the most limiting for the LEU fuel, resulting in a peak U-10Mo temperature of 264 °C (507 °F) in the limiting plate of a MOL element and 262 °C (504 °F) in the limiting plate of an EOL element. This case included a reduction in thermal conductivity, which at end-of-life was estimated to be 65% of the fresh value. The peak LEU fuel temperature modeled in various cases with uncertainties rose to 271 °C (520 °F). The additional cases were explored in order to estimate the sensitivity of the calculated fuel temperature to uncertainties in the measured thermal conductivity of irradiated fuel (up to 50% of the fresh fuel value), the thermal resistance of the oxide layer that forms on the plate surface, initial steady-state operating conditions, and other modeling assumptions that could affect the analysis.

The MURR SAR demonstrates that for all credible loss-of-primary-coolant and loss-of-primary-flow accidents (LOCAs and LOFAs) unacceptable consequences to people and the environment

would be avoided. For the conversion of the MURR core from HEU to LEU fuel, an analogous demonstration is made for the LEU core that replaces the existing HEU core. Both HEU and LEU cores are analyzed and compared, and are shown to have adequate safety margins.

The LOCAs are distinguished from the LOFAs by having a loss of primary coolant. In the LOFAs there is no loss of primary coolant. The following four LOCAs were analyzed:

1. A double-ended break of the main 12-inch pipe at the reactor inlet
2. A double-ended break of the main 12-inch pipe at the reactor exit
3. An 8-inch hole in the reactor inlet pipe at a flow control valve
4. A 2-inch hole in the pipe that connects the pressurizer to the reactor inlet pipe.

The following three LOFAs were analyzed:

1. A loss-of-site power
2. Inadvertent closure of both main isolation valves
3. Locked rotor in one of two main coolant pumps

In addition, analysis is provided with the aid of the RELAP5 code to demonstrate the effectiveness of the anti-siphon system.

The very substantial RELAP5 simulation model of the MURR plant provided by the MURR SAR explicitly represented all of the core coolant channels and fuel plates of single fuel element. This model assumed that the core geometry was that for a core in which all elements were fresh. In the current analysis, this model is replaced with an updated set of models in which for HEU or LEU the single fuel element represented in the model is either a beginning-of-life (BOL), middle-of-life (MOL), or end-of-life (EOL) element.

Improvements to the SAR RELAP5 model include:

1. Additional detail in an accurate representation of the plant protection system
2. Improved reactor core hydraulic parameters
3. The effects of fuel burnup are incorporated
4. An improved in-pool heat exchanger model
5. An improved anti-siphon system model
6. LEU and HEU fuel plate thermal properties updated based on experimental measurements

The initial steady-state reactor conditions assumed in the current RELAP5 analysis represent the most severe for normal operation. The current analysis shows that the highest peak fuel temperature for all of the LOCA/LOFA transients is 139 °C (282 °F) for the HEU fuel, which is only 31 °C (56 °F) above the initial steady state peak value. Similarly, for the LEU fuel, the highest transient peak fuel temperature is 157 °C (314 °F) which is only 13 °C (24 °F) above the initial steady state peak value.

In all of the HEU and LEU RIA, LOCA and LOFA accidents analyzed, the fuel temperature remains well below temperatures where fuel blisters have been experimentally observed. In UAl_x fuel used for HEU elements, blisters have been observed in the range of 480 to 598 °C (896 to 1108 °F) when irradiated fuel plates, at relevant fission densities, were placed in a furnace [20]. Only preliminary blister furnace data is available for monolithic UMo alloy fuel being developed and qualified for the LEU elements. For the monolithic UMo, fuel blisters have been observed in the range of 400 to 530 °C (752 to 986 °F) when irradiated fuel plates were placed in a furnace at fission densities relevant to MURR [21].

For the LEU MURR cores, the EOL peak local fission density has been calculated to be between 1.1×10^{21} and 3.4×10^{21} fissions/cm³ where the highest burnup is found in the outer plates of the element [22]. Monolithic UMo plates exceeding these burnup levels, up to 7×10^{21} fissions/cm³, have been blister tested and exhibit a blister temperature no lower than 365 °C (689 °F) [21]. This preliminary UMo monolithic data was measured across a range of U-10Mo plate geometries, fabrication variables and irradiation conditions. Fuel plates specifically representative of MURR geometry and irradiation conditions will be blister annealed in future tests. Based on these preliminary measurements, there is significant margin between the temperature where blisters have been measured and the peak fuel temperature calculated in the most limiting LEU reactivity insertion accident.

In conclusion, for all of the RIAs, LOCAs, and LOFAs considered, both the HEU core and the LEU core demonstrate a substantial margin to fuel damage. Together with the preliminary blister measurements, results indicate that fuel conversion to a proposed U-10Mo monolithic fuel will result in no damage to fresh or depleted fuel plates for any of the reactivity insertion, loss-of-coolant, or loss-of-flow accidents postulated for MURR.

Dose consequences to workers and the general public from two types of postulated accidents that result in the release of radioactive material were also evaluated for the reactor converted to LEU fuel, and to provide a comparison to the doses from the existing HEU fuel in the MURR core.

The first of these postulated accidents that results in a release of radioactive material is the Maximum Hypothetical Accident (MHA). In the MHA, an assumption is made that four fuel plates in the peak heat flux region of the core overheat and melt, releasing the inventory of noble gasses and radioiodines in those plates. The occupational dose to workers in the MURR containment was calculated in the SAR to be 132 mrem for the HEU fuel. Using a similar set of assumptions for the LEU-fueled core, the occupational dose is calculated to be 403 mrem. This increase is due to 1) the 20% core power uprate from 10 MW to 12 MW needed for the conversion to LEU fuel to maintain experimental performance at the MURR and 2) the assumption of a larger fraction of the total fission product inventory released in the LEU MHA based on the fuel element geometry and core power distribution. In both cases, the calculated occupational dose is well below the regulatory limit for radiation workers of 5 rem.

The dose to the general public from the MHA was also calculated. The dose calculated in the SAR for the HEU core was 0.03 mrem. Using the same set of assumptions as for the HEU-fueled core, the dose at the location of maximum dose was calculated to be 0.05 mrem for the

LEU-fueled core. This is well below the regulatory annual limit of 100 mrem for members of the general public.

Fuel handling accidents (FHA) are categorized into the following events or scenarios: a fuel mishandling event that breaches the cladding on a fuel plate, simple failure of the fuel cladding due to a manufacturing defect or corrosion, and overheating of a fuel element with subsequent potential cladding failure as might occur in LOCA or LOFA accidents. The fuel handling accident and fuel defect accidents are modeled with the same power history and plate location as the MHA.

First, the occupational dose from a fuel mishandling accident that results in a breach of the fuel cladding was calculated. The quickest time a fuel element can be brought to the reactor pool is about an hour (60 minutes) after reactor shutdown. In order to be conservative, a 30-minute cooling time before element handling was assumed for the FHA. For an element that has cooled for 30 minutes prior to handling, the occupational dose was calculated to be 349 mrem, which is encompassed by the MHA.

The FHA was also analyzed for not requiring containment integrity for handling fuel with decay greater than 60 days. This analysis was not part of the MUR SAR, but was performed by MURR in response to a “request for additional information” in the NRC review of the previous SAR [23]. The occupational dose to a worker present in the containment for one hour with a breached fuel element with 60 days of decay was calculated to be 0.79 mrem. For the LEU fuel, the dose is calculated to be 0.90 mrem.

The last two accidents analyzed were for a fuel element malfunction due to either cladding deformation/corrosion or overheating of the fuel that leads to cladding failure. Pitting corrosion is not catastrophic in nature and can be detected by conventional monitoring techniques in place at the MURR. Based upon conservative assumptions for the size of a pit forming on the plate surface, the release was computed to be well encompassed by the MHA. Overheating of the MURR fuel with subsequent potential cladding failure was analyzed in detail in Chapters 4 through 6 for RIAs, LOCAs, and LOFAs, respectively. All of these analyses concluded that the operational and environmental impacts of these accidents did not include any fuel melting or fission product release and are easily bounded by the MHA.

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Appendix A – Primary Pump Characteristic Curves

For all of the LOCA and LOFA transients simulated in Chapters 5 and 6, although the result could be sensitive to the shape of the pump head curve, they would not be sensitive to variations in the shape of the flow coastdown. This insensitivity to flow coastdown is certainly true for the locked-rotor LOFA because there the pump speed is prescribed to go from full speed to 50 or 60% of full speed over 0.001 s. It is also true for the LOCAs simulated in Chapter 5 and the other two LOFAs simulated in Chapter 6 because for these upsets the peak fuel temperature is reached by 0.35 s, which is 0.65 s before the pump is tripped, or it occurs after 10 s, which is after the two main isolation valves have closed. Figure 5.19, Figure 5.22, and Figure 5.23, which provide the flows for the doubled-ended cold-leg break and 8-inch cold-leg break LOCAs, show that for these two transients there is significant variation in the primary pump and reactor vessel flows during the first second of these transients while the primary pumps are running at constant speed. Thus, the focus of this appendix is to represent the pump head curve properly. Although the pump torque characteristics may not be of importance here, it too is included below. Homologous pump curves include dimensionless curves for both the head and the torque. Producing both at the same time does not require significantly more effort. The torque curves may be needed at some future date. If they are, then other pump parameters, such as pump inertia, and those parameter that effect torque friction should also be investigated.

Figure A-1 shows the measured primary pump characteristics as provided by the pump manufacturer. The first three columns of Table A-1 were read from Figure A-1. By definition, the pump efficiency is the ratio of the power output of the pump divided by the power input to the pump. The power output is the product of pump head and the flow rate. The power input is the product of the torque and the pump speed. Thus, efficiency, η , is given by $\eta = \rho Q g H / (\omega \tau)$, where these symbols in order are density, volumetric flow rate, gravitational acceleration (32.17 ft/s^2), head, speed, and torque. Thus, torque is given by $\tau = \rho Q g H / (\omega \eta)$. This relationship was used to obtain the values of torque shown in the table.

There is a mark in Figure A-1 at 1800 gpm and 170 feet. These were taken to be the reference conditions. As indicated in Figure A-1, the rated speed is 1750 RPM. This is taken to be the reference speed. The rated density, ρ_R , is taken to be 60.674 lbm/ft^3 . This is what was used in the MURR SAR RELAP5 model. It corresponds to about 175 F. Figure A-1 shows a 0.972 specific gravity which also agrees with this water density. These reference quantities were used to calculate the reference torque of 322.5 ft-lbf. v , α , h , and β are dimensionless, flow, speed, head, and torque, respectively, as defined in Table A-1, where these values are provided.

Methods for producing homologous pump curves needed for RELAP5 input are described in Volume II of the RELAP5/MOD3.3 manual. (It was also very helpful to have recently attended an advance class on RELAP5-3D that was provided in Idaho by some of the RELAP5-3D developers.) These methods are used to produce the current of curves. The definitions of symbols are the same as in the manual. Table A-1 was used to produce Table A-2. The data in

Figure A-1. Measured Primary Pump Characteristics.

Table A-2 are plotted in Figure A-2 and Figure A-3. The ordinate, “head” in Figure A-2 is h/α^2 or h/v^2 , as appropriate. The ordinate, “torque” in Figure A-2 is β/α^2 or β/v^2 , as appropriate. In the legends, 1 represents regime 1 and 2 represents regime 2. These two figures represent the first quadrant of the homologous head and torque curve, respectively. This quadrant is the one in which both pump flow and the pump head are positive and is the one needed for the current LOCA and LOFA analyses.

Table A-1. Primary Pump Data.

Flow (Q), gpm	Head (H), feet	Efficiency (η)	Torque , ft-lbf	v (flow)*	α (speed)*	h (head)*	β (torque) *
0	245	0		0.000	1.000	1.441	
200	245	0.24	151	0.111	1.000	1.441	0.467
400	245	0.42	172	0.222	1.000	1.441	0.534
600	244	0.56	193	0.333	1.000	1.435	0.598
800	242	0.66	216	0.444	1.000	1.424	0.671
1000	235	0.72	241	0.556	1.000	1.382	0.747
1200	225	0.75	266	0.667	1.000	1.324	0.824
1400	214	0.77	287	0.778	1.000	1.259	0.890
1540	202	0.775	296	0.856	1.000	1.188	0.918
1600	196	0.76	304	0.889	1.000	1.153	0.944
1700	186	0.74	315	0.944	1.000	1.094	0.977
1800	170	0.70	322.5	1.000	1.000	1.000	1.000
1900	155	0.65	334	1.056	1.000	0.912	1.036
2000	126	0.55	338	1.111	1.000	0.741	1.048

*These quantities are dimensionless because they are the ratio of the dimensional quantity to the corresponding reference quantity, i.e., 1800 gpm, 1750 RPM, 170 feet, or 322.5 ft-lbf.

Table A-2. Data for the First Quadrant of the MURR Primary Pump Homologous Curves.

Regime 1			Regime 2		
v/α	h/α^2	β/α^2	α/v	h/v^2	β/v^2
0.000	1.441	0.000	1.000	1.000	1.000
0.111	1.441	0.467	0.912	0.818	0.930
0.222	1.441	0.534	0.741	0.600	0.849
0.333	1.435	0.598			
0.444	1.424	0.671			
0.556	1.382	0.747			
0.667	1.324	0.824			
0.778	1.259	0.890			
0.856	1.188	0.918			
0.889	1.153	0.944			
0.944	1.094	0.977			
1.000	1.000	1.000

Figure A-2. Homologous Head Curves for the First Quadrant of the MURR Primary Pumps.

Figure A-3. Homologous Torques Curves for the First Quadrant of the MURR Primary Pumps.

Figure A-4 and Figure A-5 show the complete set of homologous head and torque curves used in the MURR SAR analysis. The digital values used to plot these curves were copied from a RELAP5 model input file. The head curve has eight regimes, as does the torque curve. Regimes 1, 2, 3, and 4 are the solid red, blue, green, and purple lines, respectively. Regimes 5, 6, 7, and 8 are the dashed red, blue, green, and purple lines, respectively. Note that there should be no discontinuity where one regime line ends and another begins.

Figure A-4. Homologous Pump Head Curve Used in the MURR SAR.

Figure A-5. Homologous Pump Torque Curve Used in the MURR SAR.

For the new homologous pump curve, regimes 1 and 2 of Figure A-4 and Figure A-5 are replaced with those of Figure A-2 and Figure A-3. Region 2 was extended to join the end of regime 8. Part of regime 3 was altered to join the end of the new regime 1. The results are shown in Figure A-6 and Figure A-7.

Figure A-6. New Homologous Pump Head Curve.

Figure A-7. New Homologous Pump Torque Curve.

Since in RELAP5 there are two assumed identical pumps in parallel that are being modeled as a single pump, the total flow, Q , should be doubled in the model and the rated pump flow in the model should be doubled. When this is done, the homologous curves are unaffected because the ratio of flow to rated flow is used in calculating these curves. However, in the RELAP5 input the rate flow was doubled from 1800 gpm to 3600 gpm. The torque and efficiency are unaffected. There is twice as much flow at the same torque and efficiency producing twice as much power.

Because the rated combined flow for the two pumps is 3600 gpm and the initial steady-state condition in the RELAP5 analysis is at 3700 gpm, the pump speed was increased by a factor of $3700/3600 = 1.028$. This factor is the ratio of initial pump velocity to rated pump velocity in the RELAP5 code input.

The new pump model was used only for the two test cases identified in Section 5.8.9. The test cases repeated the double-ended cold-leg LOCA for the HEU and the LEU core with the new pump model. All of the other MURR RELAP5 results quoted in this report are based on the MURR SAR pump model.

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