JM-1 SLOWPOKE-II Research Reactor Supporting Analysis Report for the Conversion from HEU to LEU

Nuclear Engineering Division

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Executive Summary

The JM-1 research reactor (SLOWPOKE-2 type) at the International Centre for Nuclear and Environmental Sciences (ICENS) of the University of West Indies, in Kingston (Jamaica), was successfully converted from HEU to LEU fuel in September of 2015. As part of the work to produce the corresponding Safety Analysis Report (SAR), which was to be reviewed and approved by the Jamaican regulator in order to provide authorization for the reactor commissioning, a detailed safety analysis was carried out. This analysis, involving a variety of calculations, is summarized in the present report; describing the basic methodology and the obtained results.

The report covers both the neutronic and thermal hydraulic aspects of the analysis, for which MCNP and PLTEMP/ANL codes, respectively, were used. Initially, the HEU core was modeled and analyzed, subsequently comparing the obtained results with the available experimental data. Following the HEU analysis, the modifications involved with the change to an LEU core were introduced into the models and the results examined to ensure that the differences in the expected behavior did not negatively affect the reactor safety or performance. In fact, the LEU core was found to present significant advantages in certain areas. This report presents a comparison of the HEU and LEU cores that provides a basis for the safety analysis report for the conversion of the JM-1 reactor to LEU.

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Table of Contents

Executive Summary.....	i
Table of Contents.....	iii
List of Figures	v
List of Tables	vii
1 INTRODUCTION	1
1.1 REACTOR DESCRIPTION AND CONTEXT	1
1.2 SCOPE AND PURPOSE.....	2
2 HEU CORE SAFETY ANALYSIS	4
2.1 NEUTRONIC ANALYSIS.....	4
2.1.1 Core reactivity.....	5
2.1.2 Control rod worth	6
2.1.3 Reactivity worth of top reflector	6
2.1.4 Flux distribution in irradiation channels.....	7
2.1.5 Axial flux distribution	7
2.1.6 Moderator void reactivity coefficient (MVRC)	9
2.1.7 Fuel temperature coefficient of reactivity.....	9
2.1.8 Combined temperature reactivity response	10
2.1.9 Reactor coolant temperature coefficient of reactivity.....	13
2.2 THERMAL HYDRAULIC ANALYSIS.....	15
2.2.1 HEU core thermal hydraulic model.....	17
2.2.2 Modeling process and results.....	22
3 LEU CORE SAFETY ANALYSIS	27
3.1 NEUTRONIC ANALYSIS.....	28
3.1.1 Core reactivity.....	28
3.1.2 Control rod worth	28
3.1.3 Reactivity worth of top reflector	29
3.1.4 Flux distribution in irradiation channels.....	29
3.1.5 Axial flux distribution	30
3.1.6 Moderator void reactivity coefficient (MVRC)	30
3.1.7 Fuel temperature coefficient of reactivity.....	32
3.1.8 Combined temperature reactivity response	32
3.1.9 Reactor coolant temperature coefficient of reactivity.....	34
3.1.10 Predicted core configuration at commissioning.....	36

3.2	THERMAL HYDRAULIC ANALYSIS.....	37
3.2.1	LEU core reference thermal hydraulic model.....	37
3.2.2	Modeling process and results	39
3.2.3	Predicted core configuration at commissioning.....	42
3.3	SELF-LIMITING POWER EXCURSION BEHAVIOUR	43
3.3.1	Reactor kinetics parameters	44
3.3.2	Critical heat flux	45
3.3.3	Experimental transients reactivity analysis	46
	ACKNOWLEDGEMENT	52
	REFERENCES	53
	Appendix A: Sample PLTEMP/ANL input data calculations for the JM-1 HEU core	55
	Appendix B: Main parameters of the MCNP models.....	58

List of Figures

Figure 1: Vertical cross section of the JM-1 critical assembly	2
Figure 2: Vertical cross-section of the JM-1 reactor model.....	4
Figure 3: Horizontal mid-core cross-section of the JM-1 HEU reactor core (at commissioning)	5
Figure 4: MCNP5 estimated differential and cumulative reactivity worth of Be shims in HEU core	7
Figure 5: Average thermal flux distribution in the inner and outer irradiation channels	8
Figure 6: Axial thermal flux distribution in the HEU core at 19.08 kW.....	8
Figure 7: Calculated excess reactivity as a function of void volume in the HEU core	9
Figure 8: Calculated excess reactivity as a function of average fuel temperature	10
Figure 9: Calculated JM-1 reactivity versus moderator temperature and experimental data comparison	12
Figure 10: HEU radial neutron flux at mid-core height.....	14
Figure 11: Total excess reactivity as a function of coolant temperature for the HEU core	15
Figure 12: PLTEMP/ANL single fuel pin model of the MNSR reactor core	17
Figure 13: Central, corner, and wall channels of a rod bundle in a hexagonal tube.....	18
Figure 14: Calculated and experimental coolant outlet temperatures in the JM-1 HEU reactor.....	20
Figure 15: JM-1 HEU core power distribution and individual coolant channels segmentation...	21
Figure 16: Horizontal mid-core cross-section of the proposed JM-1 LEU reactor core	28
Figure 17: Calculated differential and cumulative reactivity worth of Be shims in LEU core	29
Figure 18: Calculated thermal flux distribution in the irradiation channels at nominal power...	30
Figure 19: Axial thermal flux comparison between LEU core (21.23 kW) and HEU (19.08 kW) ..	31
Figure 20: Calculated excess reactivity change with void volume in the LEU core	31
Figure 21: Calculated excess reactivity as a function of average fuel temperature in the LEU core	32
Figure 22: Calculated JM-1 reactivity versus moderator temperature compared to experimental data.....	33
Figure 23: LEU radial neutron flux at mid-core height	34

Figure 24: Total excess reactivity as a function of coolant temperature for the LEU core	35
Figure 25: Calculated and experimental coolant outlet temperatures in the RMC LEU reactor	38
Figure 26: Reference JM-1 LEU core power distribution and individual coolant channels segmentation	38
Figure 27: HEU SLOWPOKE-2 power and reactivity evolution after a 5.88 mk reactivity insertion	47
Figure 28: HEU SLOWPOKE-2 core coolant temperature evolution after a 5.88 mk reactivity insertion	49
Figure 29: SLOWPOKE-2 transient power peaks measurements after sudden reactivity insertions.....	50

List of Tables

Table 1: JM-1 reactor core heating experimental data	19
Table 2: Uncertainties included in the six hot channel factors for the HEU core	23
Table 3: Calculated operating parameters of the JM-1 HEU reactor	26
Table 4: Uncertainties included in the six hot channel factors for the LEU core	40
Table 5: Calculated operating parameters of the JM-1 LEU reactor	41
Table 6: Operating parameters comparison between JM-1 LEU reactor core configurations	43
Table 7: HEU power transient peaks key parameters (5.88 mk insertion).....	51
Table 8: LEU power transient peaks key parameters (5.88 mk insertion)	52

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1 INTRODUCTION

1.1 REACTOR DESCRIPTION AND CONTEXT

The Jamaican SLOWPOKE-2 research reactor (JM-1) is a pool type reactor, designed by the Atomic Energy of Canada Ltd (AECL). It is fueled by approximately 1 kg of uranium enriched to 93% ^{235}U and has a thermal rating of 20 kW when operating at full power. JM-1 is owned and operated by the University of the West Indies, Mona Campus, and was commissioned in March 1984. During the last 30 years the reactor has been primarily used for neutron activation analysis of trace elements in studies related to health, the environment and agriculture. The current core configuration could see the Jamaican SLOWPOKE-2 reactor operating for another 14-18 years, at which time the addition of a large beryllium annulus as supplementary reflector could extend the life of the core by another 15 years. However, in keeping with the spirit of the growing international consensus to eliminate civil uses of HEU, a formal request was made in 2009 via the IAEA to the U. S. Department of Energy Material Management and Minimization (M^3) Conversion Program [current successor to the Global Threat Reduction Initiative (GTRI) established in 2004, which in turn followed the Reduced Enrichment for Research and Test Reactors (RERTR) program established in 1978] to convert the reactor in Jamaica to LEU fuel and to ship the spent HEU fuel to the U.S., the source of the fuel [1].

The core of the JM-1 reactor is located under approximately 4.4 m of water, close to the bottom of a watertight reactor container. The $\sim 1.4 \text{ m}^3$ of water within this container acts as radiation shielding and as the moderator and primary heat transfer medium within the core. This water-filled container is in turn immersed in a pool of $\sim 27 \text{ m}^3$ of water from which heat is extracted by means of a water-cooled coil located near the top of the pool. The thermal capacity of the water in the reactor container and the pool is very large (temperature rise of $0.03^\circ\text{C}/\text{kWh}$), and the heat removal capacity of the cooling coil is a maximum of $\sim 4 \text{ kW}$. Consequently, with the reactor operating at power levels up to 20 kW for several hours, the gradually increasing water temperatures, combined with the system's negative reactivity feedback characteristics, act to automatically limit operating levels to less than 50% of maximum power for extended runs.

The basic configuration of the SLOWPOKE-2 reactor is illustrated in Figure 1. Cold water is drawn through the inlet orifice by natural circulation, and after flowing past the hot fuel pins, exits via the outlet orifice. This hot water rises and mixes with the bulk of the water in the reactor container which transfers the heat through the reactor container wall to the reactor pool water, and ultimately to the cooling coil. The core inlet orifice restricts the natural circulation of water through the core. The inlet area is fixed and is deliberately chosen such that the highest power achieved during the maximum credible self-limiting power excursion can cause no damage to the core, nor present any safety hazard. That is accomplished as a result of the negative temperature reactivity coefficients (intrinsic to the design), which contribute to decrease the reactivity (and correspondingly the reactor power) as the temperature of the core increases.

Because the reactor is unpressurized and relies upon natural convection, the potential problems of depressurization or coolant flow pump failure are avoided. The thermal energy

Figure 1: Vertical cross section of the JM-1 critical assembly

generated in the core is eventually removed from the pool by a water-cooled coil of limited heat removal rate capability. Because of the reactor's limited excess reactivity and strong negative temperature reactivity feedback characteristics, any significant deterioration in heat

removal capacity (e.g. loss of cooling water flow) will eventually result in an automatic decrease in reactor power to match the reduced heat removal capacity.

1.2 SCOPE AND PURPOSE

This report presents the neutronic and thermal hydraulic analyses performed for the current JM-1 research reactor HEU core configuration and for the reference LEU 198 fuel pin configuration found in the SLOWPOKE-2 LEU reactors at the École Polytechnique de Montreal (EPM) and the Royal Military College of Canada (RMC). Neutronic calculations were carried out using the Monte Carlo N-Particle transport code (MCNP5) while the Argonne-developed code, PLTMEP/ANL V4.1, was used for the thermal hydraulic analyses. The analyses of the HEU and LEU cores are discussed in chapters 2 and 3 respectively. A brief analysis of the predicted core configuration using the developed reactor model according to actual fuel manufacturing data is also presented (sections 3.1.10 and 3.2.3), along with analysis on the possible effects the different configuration may have on the most relevant reactor parameters (section 3.3).

The purpose of this document is to serve as a basic reference to the Safety Analysis Report that supports the conversion process, while its scope is limited to the mentioned neutronic and thermal hydraulic aspects related to reactor performance and safety.

2 HEU CORE SAFETY ANALYSIS

The HEU core of the JM-1 reactor consists of 296 fuel pins which are 228 mm long and 5.23 mm in diameter. The fuel is a coextruded uranium-aluminum alloy that is 28% by weight uranium, enriched to ~93%, giving a total core mass of approximately 817 g of ^{235}U . The fuel cladding and fuel cage are constructed from aluminum. The core is encased in a 102 mm-thick, 228 mm-high annular beryllium reflector and sits on a bottom 102 mm-thick beryllium disc 322 mm in diameter. The top reflector consists of semicircular beryllium plates called shims, each only a few millimeters thick, which are added as necessary to compensate for burn-up, by reducing neutron leakage as the Be reflector grows thicker. There are five inner irradiation sites located in the beryllium annular reflector and one outer site just outside the annulus.

2.1 NEUTRONIC ANALYSIS

A detailed model of the JM-1 HEU reactor was created, with MCNP5 [2], in a three-dimensional Cartesian coordinate system, using data from engineering drawings of the reactor components. This model was based on the HEU core configuration at commissioning. During its development a significant effort was devoted to accurately capture all relevant features of the reactor. This was not entirely possible as there is no reliable data available on the precise uranium vector and the impurities present in the original HEU fuel. The fuel composition used in the HEU MCNP5 model is taken from the Research Reactor Core Conversion Guidebook (RRCGB) [3], which contains information on the typical composition of U-Al fuels enriched to 93.19% ^{235}U , which is almost identical to that of the 93.18% enriched fuel used in the JM-1 research reactor. The reactor, as modeled in MCNP5, is illustrated in Figures 2 and 3.

Figure 2: Vertical cross-section of the JM-1 reactor model

Figure 3: Horizontal mid-core cross-section of the JM-1 HEU reactor core (at commissioning)

2.1.1 Core reactivity

The MCNP5 estimated HEU core excess reactivity was calculated to be 5.01 ± 0.06 mk (two standard deviations are used as reference for statistical margin of error throughout the report, including the plots, resulting in a 95% confidence interval). This reactivity estimate is higher than the 3.4 mk (340 pcm) of excess reactivity that was measured at commissioning. The difference in the calculated and measured excess reactivity is likely to be partially due to the uncertainty in the actual fuel composition. A significant reduction in the calculated excess reactivity, approximately 2 mk, was observed when the uranium vector in the MCNP5 calculations was adjusted from the 93% ^{235}U and 7% ^{238}U used initially, to the typical 93.18% ^{235}U , 5.38% ^{238}U , 0.44% ^{236}U and 1% ^{234}U along with other minor impurities normally found in uranium-aluminum alloy fuels, as reported in the RRCGB [3]. Based on the typical impurity ranges expressed as equivalent boron content (EBC) also found in RRCGB and the stated purity of the aluminum alloy used to manufacture the fuel, the impurities value chosen as reference is 2.4 ppm EBC, though values up to 6 ppm are considered typical for this type of fuel. If a value of 5 ppm was selected instead for the EBC, the total excess reactivity would be reduced by an approximate additional 0.3 mk. The uncertainty in some beryllium impurities concentrations may also be a contributing factor to the excess reactivity overestimation, as these impurities were reported in the manufacturer specifications as being below specific values, assumed to be the respective detection limits of the techniques used to determine them.

Assigning a specific value to these concentrations is somewhat arbitrary in any case. A value of half the detection threshold might be selected, for instance. However, if we take into account that the concentration of these impurities shouldn't be zero, and that the detection threshold is likely to be roughly in the range of the actual impurities concentration, we may conclude that the actual figures have a higher probability to be closer to the detection threshold than to zero. The resulting situation is that of a truncated probability distribution, where the highest

probability density is not centered within the available range of values. In this case a value of 66% of the reported respective detection limits was assigned. This same approach was also followed, for instance, by Y-12 National Security Complex in the United States (manufacturers of the JM-1 HEU fuel and LEU UO_2 powder) when calculating the EBC estimates of the UO_2 provided to AECL for the LEU fuel manufacturing. This 66% of the reported detection limits returned the 5.01 mk excess reactivity estimation. The maximum possible combined reactivity worth of these impurities was determined to be approximately -2.5 mk. Therefore, the resulting theoretical excess reactivity allowing for the uncertainty of beryllium impurities would range from about 4.3 mk to 6.8 mk; while not all the values within this range should be expected to be equally likely. Our best estimate results in this overestimation of 1.61 mk, which is very small, denoting a deviation of just 0.160% of total reactivity, as well as a significant improvement compared to previous modeling attempts, which overestimated the excess reactivity of the SLOWPOKE HEU configuration by 119 mk, 13 mk and 3.3 mk respectively [4,5].

It is important to stress that even though accurate excess reactivity estimates contribute to the overall confidence in the model, the most important role of the neutronics model lies in its capability to accurately replicate the reactor's dynamic behavior. The behavior is determined by reactivity changes, which, in absolute terms, are less influenced by slight composition differences and other possible small inaccuracies than the total excess reactivity is. The estimates of the latter, in turn, can easily be adjusted to fit experimental data.

2.1.2 Control rod worth

The control rod worth was determined by calculating the difference between the MCNP5 estimated reactivities with the control rod completely extracted and inserted respectively. The worth of the control rod was calculated to be 5.63 ± 0.12 mk for the HEU core which compares very well with the measured value of 5.4 mk [6].

2.1.3 Reactivity worth of top reflector

The system reactivity decreases in the long term as ^{235}U is consumed and poisons (in particular ^{149}Sm) build up in the reactor fuel. This loss in reactivity is compensated for by means of a periodic shim adjustment, where the thickness of the top beryllium reflector is increased by the addition of beryllium shim plates, each having a small positive reactivity worth. Experience at the SLOWPOKE-1 (Toronto) has demonstrated that such an adjustment is required when the installed excess reactivity has decreased by approximately 0.7 mk [7].

As fuel burn-up proceeds and the thickness of the beryllium top reflector is correspondingly increased, the marginal reactivity worth of additional beryllium shims decreases. A typical addition of one 3.2 mm thick semicircular beryllium plate is worth 0.7 mk on a fresh core but much less in a core with ~ 16 kW_y burn-up (having a thick top reflector already in place). In that case, six semicircular beryllium plates, 3.2 mm-thick each, would be needed to obtain the same reactivity gain, increasing the reflector thickness by 9.6 mm [7]. It should be taken into account that the way the semicircular beryllium plates are arranged on the tray also affects reactivity, being most effective as reflector for a given thickness when both halves of the tray are leveled. The JM-1 reactor can accommodate a maximum shim thickness of 102 mm. Top beryllium

reflector cumulative and differential reactivity worth as a function of thickness as calculated by MCNP5 is illustrated in Figure 4 for the HEU core.

Figure 4: MCNP5 estimated differential and cumulative reactivity worth of Be shims in HEU core

2.1.4 Flux distribution in irradiation channels

Figure 5 shows the estimated averaged thermal neutron flux distributions (up to 0.625 eV) in the inner and outer irradiation channels (only one outer channel). Measured thermal neutron flux at full power in the lowest section of the inner irradiation sites ($10^{12} \text{ n}\cdot\text{cm}^{-2} \cdot \text{s}^{-1}$) is used as reference level to properly scale MCNP5 flux results. The corresponding estimates for the outer irradiation tube are in very good agreement with the measured thermal flux of $0.54 \times 10^{12} \text{ n}\cdot\text{cm}^{-2} \cdot \text{s}^{-1}$ [6].

2.1.5 Axial flux distribution

Figure 6 shows the calculated axial thermal flux distribution in the JM-1 HEU reactor at nominal full power at a radius of 4.6 cm. Given that approximately 92.7% of fissions in the HEU core are induced by thermal neutrons, the thermal flux distribution in the core region is essentially proportional to power, thus providing useful information. The precisely calculated axial form factor (or axial offset), which is the power ratio between the horizontal reactor section having the highest power and the average power of all these horizontal sections, is 1.09. The calculated radial power form factor (or radial offset), which in this case is the power ratio between the highest power fuel pin and the average power of the pins in the core, is 1.25; compared to a previously calculated value of 1.20 [7]. The total form factor (or reactor offset), corresponding to the power ratio between the highest fuel pin section power and the core average power is 1.37.

Figure 5: Average thermal flux distribution in the inner and outer irradiation channels

Figure 6: Axial thermal flux distribution in the HEU core at 19.08 kW

2.1.6 Moderator void reactivity coefficient (MVRC)

The production of voids within the reactor core as a result of the coolant-moderator boiling would displace moderator from the core, decreasing moderator density and its moderation capability as well as increasing leakage, thus reducing the system reactivity. The MCNP5 estimated core average MVRC was determined by adjusting the density of the moderator within the core volume without changing the temperature (exploring a void fraction range from 0 to 2%) to simulate the effect of bubble formation associated with incipient boiling. The average HEU-core moderator void coefficient for JM-1 calculated with MCNP5 is -3.132 mk/% of voids. The obtained value is equivalent to -0.041 mk/cm³ (core water volume of ~ 7.6 L). This compares very well with the previously calculated value of -0.046 mk/cm³ and the measured value of -0.042 mk/cm³ at the center of a SLOWPOKE-1 reactor core using a 7 cm³ void [7]. Excess reactivity as a function of void volume is illustrated in Figure 7.

Figure 7: Calculated excess reactivity as a function of void volume in the HEU core

2.1.7 Fuel temperature coefficient of reactivity

The negative fuel temperature coefficient of reactivity is associated to the increase in parasitic neutron capture by ²³⁸U due to Doppler broadening of the neutron absorption resonances, which results in a decrease in the number of neutrons available to cause fission. As it can be expected in a reactor fueled with HEU, where the isotopic fraction of ²³⁸U is small, the corresponding increase in neutron absorption with increasing temperature is also small, resulting in a negative but essentially negligible reactivity coefficient. Heating of the HEU fuel also causes expansion and the consequent expulsion of water (moderator) from the core, an effect that is essentially instantaneous with reactor power change. Though this effect will contribute to decrease system reactivity, it is compensated to some extent by an increase in reactivity as the fuel cage expands, increasing the effectiveness of the beryllium reflectors,

reducing neutron leakage from the core. However, given that the fuel pins and fuel cage flanges are not welded together, the heat transfer between them will likely be poor, consequently the fuel cage temperature would be more closely related to the core water temperature than to the temperature of the fuel pins, thus the fuel cage expansion would be minimal and delayed to some extent. The variation of fuel pin dimensions as a result of heating is not accounted for in the MCNP5 simulations reported in this document. Nonetheless, it is possible to estimate the reactivity effects of fuel expansion by estimating the moderator fraction displaced and using the void reactivity coefficient as reference. Using a linear thermal expansion coefficient for the aluminum alloy of $20 \times 10^{-6} \text{ K}^{-1}$, the total fuel pin volume would expand by about $0.087 \text{ cm}^3/\text{°C}$. Combining that with the void coefficient previously determined results in a reactivity effect associated to the fuel expansion of $-0.0035 \text{ mk}/\text{°C}$. This value is small but still more significant than the one purely related to the Doppler broadening of neutron absorption resonances, of just $-0.0009 \text{ mk}/\text{°C}$ according to MCNP5 results illustrated in Figure 8. The combined effect would then be of $-0.0044 \text{ mk}/\text{°C}$, though it may be partly compensated by the change in fuel and core volume, which would be difficult to determine accurately.

Figure 8: Calculated excess reactivity as a function of average fuel temperature

2.1.8 Combined temperature reactivity response

The high degree of inherent safety of the JM-1 research reactor is primarily based on its negative temperature coefficient of reactivity due to under-moderated operating conditions, resulting in a strong and rapid negative temperature feedback, which is coupled with very limited maximum available excess reactivity. As the reactor power increases, the temperature of the water, which acts as moderator and coolant, also increases, causing it to expand. The resulting displacement of water from the core and the associated density reduction decreases the moderator to fuel ratio in the core. In an under-moderated reactor such as the

SLOWPOKE-2, this reduces moderation further and increases fast neutron leakage, reducing the reactivity of the system, leading to a power drop.

The temperature coefficient of reactivity analyzed in this section is not only that associated to the reactor water. Given that, in reality it is virtually impossible to thermally isolate the different reactor components and determine the individual temperature effects on reactivity, the analysis performed is meant to replicate experimental conditions. The experiments performed in 1987 at EPM with the HEU core [8] involved either adding ice to the pool to cool it or direct steam injection below the reactor to rise the pool temperature. The reactor was operated at 100 W and the corresponding measurements were made after the inlet and outlet temperature thermocouples indicated steady core water temperature. The excess reactivity values presented in Figure 9 were determined by control rod balance at each temperature.

Since reactor operation involves simultaneous temperature changes through its different components, though not in a uniform way, testing the combined temperature effects may not only be easier but more meaningful with regard to the effects expected during operation. In this case the reactivity coefficients that are involved are the moderator temperature coefficient, the fuel temperature coefficient and the reflector temperature coefficient.

The same experiments at EPM were reported to result in an excess reactivity maximum at 13.1°C [9], though it is difficult to accurately locate the peak, since reactivity measurements remain essentially flat below about 18°C. A reactivity peak at about 19.5°C was also reported in the commissioning report of the very first SLOWPOKE-2 [10], though it presented significant differences in core configuration and beryllium reflector dimensions with later version, so those results are not a valid accurate reference for the JM-1.

In any case, the reactivity coefficient is negative for most usual inlet temperatures (above ~15°C). In the event of colder conditions, any sustained reactor operation, even at low power (<2 kW), will readily increase moderator temperature above the value where combined reactivity contribution peaks, returning to the reactor operating regime with negative temperature reactivity feedback. Any marginally positive reactivity coefficient as a result of low coolant temperature could only possibly contribute to briefly increase the reactor power until a higher temperature stable regime is reached, having no safety significance.

The MCNP5 analysis of the reactor model reactivity as a function of temperature involved adjusting the overall reactor temperature, and corresponding water density, from 20.45°C to 45°C in MCNP5. Although it might be worth simulating the reactor conditions at somewhat lower temperatures to match available experimental data, the temperature range lower boundary becomes limited by the lowest temperature for which neutron cross section libraries are available for the involved isotopes. While some methods and tools do exist that allow generating cross section libraries at higher temperatures by means of Doppler broadening, the same is not possible in the opposite direction, so the analysis becomes limited by the 20.45°C threshold. Nonetheless, the simulation was still useful for the HEU fueled core, as it shows that from 20.45°C on there is a continuous decrease in reactivity with increasing core temperature, as illustrated in Figure 9. It is also possible to notice that the results trend tends to flatten as the temperature decreases, making it perfectly compatible with the experimentally observed

Figure 9: Calculated JM-1 reactivity versus moderator temperature and experimental data comparison

flattening at lower temperatures. The MCNP5 results are rescaled to properly compare the results with EPM measurements, which apparently were not performed soon after a shim adjustment, hence maximum excess reactivity was far from the 4 mk limit. On the other hand, the magnitude of the curve's slope also increases with increasing temperature, resulting in a larger negative coefficient, resulting in a more stable behavior at higher power and temperatures. Conversely, the excess reactivity peak at low temperatures may be explained by a combination of factors. The most important one is the change in water density as a function of temperature. The highest density of water is reached at 4°C and the density versus temperature gradient decreases as temperature approaches that value, becoming a less significant factor. In addition to that effect, hydrogen neutron absorption cross section in water increases as temperature decreases; along a decreasing temperature evolution, the neutron absorption growth would overcome the reactivity benefit from a progressively marginally denser moderator. This positive reactivity contribution is driven by the water surrounding the reactor core and not by the water inside it, as explained in greater detail in Section 2.1.9.

The fuel temperature coefficient remains essentially constant within this range. Reflector temperature coefficient is also expected to be roughly constant, though positive, given that the cross section of the reaction ${}^9\text{Be}(n,2n){}^8\text{Be}$ increases with temperature in this range while the absorption cross section of the impurities present in the beryllium decreases with temperature, but it is a minor effect. The hardening of the neutron spectrum, as a result of the water temperature increase and density decrease also contributes to the positive beryllium reflector coefficient. These contributions are relatively small, so the overall behavior observed is dominated by the moderator temperature coefficient. The accurate determination of the

beryllium coefficient is actually far more challenging, given the previously mentioned uncertainty in the impurities concentration present, which would result in a significant uncertainty margin for the coefficient. Its actual value would also be affected by the fuel cage expansion and the mentioned core water parameters. Under realistic operating conditions, however, the annular reflector average temperature remains close to the core inlet temperature, as only one of its surfaces faces the higher temperature core. The effective marginal effect the reflector has on reactivity during operation is much smaller than that obtained under uniform temperature conditions. Since it has been determined, both experimentally and according to the presented simulation results (Figure 9), that the combined effect, even under such conditions, results in a strong negative reactivity feedback, the precise value associated to the beryllium reflector coefficient of reactivity has no impact on the safety.

2.1.9 Reactor coolant temperature coefficient of reactivity

It is difficult to experimentally isolate the reactivity effects of water temperature alone and to obtain reference measurements of this effect to validate the simulation results appropriately. However, given that model predictions have already been shown to be reliable with regard to other phenomena, where experimental data for validation was available, the MCNP model results regarding the coolant temperature coefficient can provide valuable information. The results shown here are particularly useful later on in Section 3.3 analysis.

When considering the reactivity effects of coolant temperature in the SLOWPOKE-2 reactors it is important to make a distinction between the water present within the core itself and the remainder of the water inside the reactor container. From here on, this water volume around the core, which basically acts as additional neutron reflector beyond the beryllium reflector itself, will be referred as the ‘water reflector’. The water reflector contributes to the overall reactivity value as a function of temperature in a different way than the water inside the core (or ‘core coolant’) does. One of the main reasons of this difference is that the energy spectrum of the neutron flux within the core is very different than the spectrum in the water reflector. On the outer side of the beryllium reflectors the neutron flux is essentially thermalized (Figure 10), hence water there has a very minor role in terms of moderation, which leads to different reactivity effects as a result of density changes. As reference, while a 1% drop in core coolant density (at constant temperature) would result in a reactivity loss of about 3 mk, as seen in Section 2.1.6; the same density drop in the water reflector would result in a reactivity change of just -0.12 ± 0.04 mk. Notwithstanding, the potential influence on reactivity of the water reflector is not always negligible. The effects of temperature on water neutron cross sections in this region can have a more significant impact on reactivity. Overall, it is particularly relevant in this analysis to make the distinction between core coolant and water reflector, since, for most reactor operation conditions, the water temperature inside the core is significantly higher than in the reactor container, and specifically the water reflector.

Figure 11 shows the HEU core total reactivity as a function of coolant temperature, comparing the case of uniform water temperature throughout the reactor container and the case where only the core coolant temperature is allowed to change, while keeping the water reflector at 20.45°C. As it can be seen, the reactivity gradient as a function of temperature is larger when the container water remains cold and only the core coolant heats up. Water temperature and

Figure 10: HEU radial neutron flux at mid-core height

density are linked together in the model in this analysis, just as it would happen in reality. The resulting relationships between reactivity and temperature are not linear, so temperature coefficients in this temperature range are not constant. It is easy to notice, both in the equations below and in Figure 11, that the slope of the reactivity curve is greater from the beginning for the core coolant coefficient, and that the slope magnitude also increases more rapidly with temperature in that case.

$$\alpha = -0.00037 - 0.00127 \cdot T$$

$$\alpha = -0.04531 - 0.00137 \cdot T$$

Uniform container temperature variation

Core temperature variation only

It becomes apparent from these results that while the container water temperature has very little effect on reactivity as a result of water density change, as previously stated, it does have a significant effect due to the thermally induced vibration of the water molecules, which impacts neutron cross sections. Colder reactor container water clearly results in lower total reactivity values for a given core temperature. However, a hypothetical heating of the reactor container water would not result in a reactivity increase, since the core water would also heat up, driving reactivity downwards.

Figure 11: Total excess reactivity as a function of coolant temperature for the HEU core

2.2 THERMAL HYDRAULIC ANALYSIS

The purpose of the thermal hydraulic analysis is to determine the safety margins and maximum allowed operating power of the reactor. Given that this reactor is cooled by natural circulation, the maximum allowed power is conservatively assigned to the power at which the Onset of Nucleate Boiling (ONB) occurs. This point sets the beginning of the nucleate boiling region, where small steam bubbles are generated on the surface of a material that is hotter than the liquid coolant saturation temperature and are immediately dispersed, sometimes eventually condensing again within the liquid. Nucleate boiling actually improves heat transfer by removing the heat of vaporization from the heated surface without reducing the hot surface contact area with the coolant. At higher surface temperatures and heat fluxes (heat transfer rates), the amount of steam produced on the heated surface is large enough to start forming a steam film over the surface, acting partially as an insulator and preventing the liquid from cooling the surface directly. When the steam insulation effect compensates the heat removal improvement provided by the heat of vaporization, a maximum heat transfer rate is reached (Critical Heat Flux or CHF) and the Departure from Nucleate Boiling (DNB) is defined. It is this DNB that establishes the maximum allowed power level in commercial reactors (with forced circulation). Beyond this point heat transfer starts to deteriorate and the cladding and fuel temperatures can increase rapidly to unsafe levels. However, in natural circulation systems the generation of steam in the core may involve more complex flow phenomena than in the case of forced circulation systems and it may be difficult to model or estimate accurately. Therefore, even though it is to be expected that heat transfer may continue improving significantly beyond ONB under natural circulation conditions, as experimental evidence shows, it becomes very

difficult to make any reasonably accurate predictions under that regime, so the conservative ONB power is chosen as the effective safety limit.

The general procedure for the analysis involves developing a full core computer model for the overall reactor behavior, which serves as a basis and is used for calibration and validation purposes. Additionally, another model of the hottest fuel pin only, along with its corresponding coolant channel, is used to determine the limiting operating conditions.

The full core model is based on the actual reactor geometry, the axial power profile, the Darcy friction factor (f) and the coolant minor pressure losses along the core region, among other parameters. The friction coefficient is considered to be a product of the Darcy friction factor and the Reynolds number (Re) by PLTEMP/ANL [12] and is also derived from the geometry and resulting flow conditions, which have been determined to correspond to laminar flow for the reactor operating range up to ONB.

This initial model needs to be calibrated with experimental data, associating reactor power with coolant temperature increase from the core inlet to outlet. These parameters make it possible to accurately calculate the resulting mass flow and flow velocity through the core, and eventually to obtain the coefficients that determine pressure drop through the core under a wide range of operating conditions. Given the complex geometry of the coolant path at the core inlet and outlet and the even more complex behavior of coolant flow along such a path, the pressure drop based on calibration using actual experimental data is expected to be much more accurate and reliable than any theoretical estimate that could be obtained otherwise. The ability of the resulting model to accurately match the collection of experimental coolant temperature increments at different power levels serves as validation and provides confidence in the results beyond those conditions.

The model for the hottest fuel pin is a variation of the calibrated model for the whole core, in which the chosen power density is that of the hottest pin instead of the core average power density. Hot channel factors (HCFs), accounting for the uncertainty margins of the relevant simulation parameters are also incorporated into this model to ensure that no foreseeable measurement error results in larger safety margins estimates than those actually corresponding to the real reactor. In other words, all relevant safety parameters are multiplied by their corresponding hot channel factor to obtain their most conservative value according to the known possible error margins for that parameter. The results obtained when all HCFs are considered, correspond to the conditions where all those variables simultaneously have their most adverse values within their foreseeable error range.

It is also important to note that, in the thermal hydraulic models created, all power is assumed to be generated within the fuel pellets, thus neglecting the direct heating of the coolant or other reactor components by gamma radiation or neutron scattering. Accurately calculating the power fraction deposited in each component of a small reactor can be complex, and a small fraction of the energy may even be deposited outside the core. This assumption is a conservative simplification from the point of view of the thermal hydraulic analysis, as it will result in slightly higher pin temperatures. It is also meant to offset the possible inaccuracy in the experimental determination of the nominal reactor power.

2.2.1 HEU core thermal hydraulic model

The actual process followed to obtain the fuel and cladding temperature safety margins and maximum allowed power for the HEU core of the JM-1 research reactor is described in this section. The resulting estimated values are also presented.

The thermal hydraulic safety analysis of the MNSR reactor, a very similar reactor to the SLOWPOKE-2, outlined in [11] was used as a general reference for the analysis of JM-1. The MNSR analysis was performed with PLTEMP/ANL V4.1 code, using a one-pin (average or peak power pin) equivalent unit cell model to simulate the reactor core configurations. The model geometry is shown in Figure 12. Even though only one representative fuel pin and flow channel are modeled, the code has been validated and proven to give accurate results. The validation report is included in the PLTEMP/ANL user's guide [12].

Figure 12: PLTEMP/ANL single fuel pin model of the MNSR reactor core

Some geometric differences between the MNSR core and that of the SLOWPOKE-2 should be taken into account. The MNSR core (containing 348 fuel pins arranged in concentric circular rings instead of a regular lattice) can be considered to be essentially uniform and regular from a thermal hydraulic point of view. The only aspects that challenge that judgment are a few dummy fuel pins, four tie rods, the core boundary and slight geometrical differences between the virtual flow channels cooling each pin (given the imperfect pins lattice). Overall these are deemed to be negligible for the intended purposes. On the other hand, the SLOWPOKE-2 core configuration is more heterogeneous. Despite the fact that the SLOWPOKE-2 fuel pin lattice is perfectly regular, unlike the one of the MNSR, many pin positions are vacant, being occupied by coolant instead of fuel pins. The HEU core configuration contains 296 fuel pins distributed among the 342 possible positions in the core lattice.

In the MNSR, it becomes possible to average all fuel pins to obtain an equivalent unit cell (comprising a single fuel pin and flow channel) without introducing a significant bias between the model and the real reactor behavior, as all pins can be considered roughly identical from a thermal hydraulic point of view. The above SLOWPOKE-2 core description evidences the more heterogeneous core configuration compared to the MNSR. The lack of homogeneity involves additional difficulties in the process of averaging the core geometry to obtain an equivalent unit cell model that behaves like the full core, given the highly non-linear behavior of many thermal hydraulic phenomena. As an example of the kind of difficulties involved, the combined flow through two different unit cells may deviate significantly from that through two cells with flow area and wetted perimeters equal to the average of those of the two former cells. The way the flow friction coefficients are tabulated in available literature for specific cell geometries may also contribute to larger calculation errors in case the model is oversimplified by directly averaging the main geometry parameters of differently shaped channels over the full core.

All the previous reasons led to the development of an alternative modeling approach. The method chosen is based on an available laminar flow analysis for rod bundles. Specifically, the theoretical reference selected corresponds to a triangular array of 7 rods within a hexagonal tube [13]. The reference analysis provides the required friction coefficient values for the central flow channels (defined by any three contiguous rods) and for the wall channels (defined between the perimeter rods and the tube walls); all of them provided as a function of the bundle dimensions (Figure 13). It also includes the equations to determine the friction coefficient of the whole bundle as a weighted average of the friction coefficient of each of the bundle channels. There are only two assumptions made in this case when applying this bundle approach to the SLOWPOKE-2 core. First of all, it is assumed that all the central channels' friction coefficients remain essentially constant when the number of rods in the bundle is increased, which is reasonable as long as the hydraulic diameter is relatively small (otherwise it could result in significantly higher flow in the central bundle channels). This assumption essentially implies that the flow drag induced in the inner channels by neighboring channels in contact with the walls of the bundle/reactor is negligible compared to the effect of the friction with the rods or fuel pins. Secondly, it is assumed that the wall channels' friction coefficients

Figure 13: Central, corner, and wall channels of a rod bundle in a hexagonal tube

are not significantly affected by the curvature of the tube walls; in this case the circular core boundary defined by the annular reflector, as opposed to the shapes of the hexagonal outer boundary in the reference tube bundle. This is a reasonable assumption as long as the distance between the rods/pins and the outer wall is not very small, in which case their relative distance may vary significantly depending on the specific wall geometry.

Once this calculation method was selected, the reactor core was divided into central and wall channels of different sizes as appropriate, according to the HEU core specific geometry (see Figure 15). The friction coefficients were determined for each of the channels according to existing tabulated values, using the minimum channel geometry approximations necessary, and were subsequently averaged according to their flow areas and wetted perimeters by means of Equation 1;

$$\frac{1}{f Re} = \sum_{i=1}^m \frac{1}{(f Re)_i} \left(\frac{D_{h,i}}{D_h} \right)^2 \left(\frac{A_{c,i}}{A_c} \right) \quad (\text{Eq. 1})$$

where f is the Darcy friction factor; Re , the Reynolds number; D_h , the hydraulic diameter; A_c , the flow area and m is the total number of flow channels [13].

The averaged core friction coefficient (224.2) of the HEU model was then used in the single-channel PLTEMP/ANL natural circulation model that was created for the SLOWPOKE-2. Its hydraulic resistance coefficient (K) was subsequently obtained by calibrating the model according to experimental JM-1 commissioning core heating data for coolant temperature rise as a function of power [14]. The data is presented in Table 1.

Table 1: JM-1 reactor core heating experimental data

Reactor Power (kW)	Tinlet (°C)	Toutlet (°C)
0.5	19.9	22.1
2	20.1	26.1
5	20.8	31
10.3	22	37.5
20.1	24.3	46.7

The data point used for calibration was that of the highest power (20.1 kW) with the intention of minimizing the error due to instrumentation precision and the distance between experimental reference data and ONB power. Calibration involves running the PLTEMP/ANL simulation with an inlet temperature of 24.3°C, at 20.1 kW, and adjusting the hydraulic resistance coefficient (K) to achieve the experimentally determined outlet temperature of 46.7°C. The obtained hydraulic resistance coefficient was 169.0. For verification, this loss coefficient was then used to simulate the other operating conditions, only changing the reactor power and the inlet temperature, allowing the PLTEMP/ANL code to calculate the outlet temperature. The resulting calibrated model takes into account the pressure drop caused by the coolant flow through the limited inlet and outlet flow areas, as well as the friction of the coolant flow within the core. As illustrated by Figure 14, the results obtained with the model agree very well with the experimental results.

Figure 14: Calculated and experimental coolant outlet temperatures in the JM-1 HEU reactor

The procedure to determine the reactor power at which ONB occurs for the JM-1 HEU reactor is also affected by the core heterogeneity. The equivalent unit cell used for the full core model is not necessarily representative of the corresponding equivalent unit cell for the hottest pin. In particular, the flow area that can be considered to contribute to the cooling of the hottest pin is not necessarily equal to the core average flow area per pin. The relevant geometric parameters can be easily modified on the existing full core model previously described to adapt to the hottest pin equivalent unit cell. However, the hydraulic resistance calibration can only be performed for the whole core. Consequently, some degree of uncertainty may exist about the error margin of predictions obtained with such a model if the core average and hot pin equivalent cells differ significantly.

The chosen solution to the above issue involved the creation of a dual channel model of the reactor, where one channel represents the hottest pin equivalent unit cell and the other one represents the remainder of the core, with both channels sharing the same inlet and outlet. Given that the combined model comprises the full core as well, the same calibration process can be used and the overall behavior was checked to ensure it matches that of the initial single channel model. This approach was deemed to provide more reliable results.

Another complication with the heterogeneous SLOWPOKE-2 core is selecting the hottest pin itself. Given that the coolant flow corresponding to each fuel pin is also variable, the hottest pin is not necessarily the one with the highest power. To determine the hottest pin, both the power and the corresponding available coolant volume should be examined simultaneously to ascertain the less favorable conditions or highest power per flow area ratio. Different approaches were considered and tested to ensure the selection is the most limiting in terms of

available safety margins, while keeping necessary assumptions and approximations to a minimum.

The highest power pins in the HEU core are located in the innermost “ring” or hexagon strictly speaking, next to the central spindle illustrated in Figure 15. However, the corresponding flow area that could be assigned to them is significantly larger than that of a regular unit cell due to the gap between them and the central spindle; where a regular unit cell is a hexagon centered on a fuel pin position and with a distance between parallel sides equal to the fuel pin pitch (1.104 cm). Some of the pins of the second innermost “ring” have the flow area of a regular lattice unit cell with a power greater than 85% of the highest power pin, being essentially hotter as a result than the innermost pins. Still, it was deemed that this was not the most conservative assumption possible, and that the coolant flow channel that would heat up the most and result in higher coolant and cladding temperatures is the one between the first and second innermost “rings” (hexagons) of pins, encompassed by the red triangle in Figure 15. This mainly assumes no water mixing or heat transfer between adjacent flow channels, which is a necessary conservative assumption if more complex CFD methods are to be avoided. The resulting reference cell is triangular in shape, with vertices on the centers of one second “ring” pin and two of the innermost “ring” pins. Consequently it has a section area half that of a regular hexagonal unit cell, involving half the flow area, as well as half the fuel pin cross-sectional area and pin surface. Therefore, the power used is half the average power of the 3 fuel pins involved

Figure 15: JM-1 HEU core power distribution and individual coolant channels segmentation

(94.72% of the highest power pin). These power values are not absolute values but fractions of the total core power, which is an input parameter of the simulations, allowing to explore all possible reactor power levels while keeping power distribution unchanged. The constant power distribution assumption has been verified using MCNP for a wide range of reactor power levels, resulting in a maximum axial offset difference of less than 1% and an even lower radial offset, which is the one relevant in the current analysis.

The modeling of the hottest fuel pin or flow channel as described is also somewhat conservative given that it neglects heat transfer within the fuel meat and cladding, though it averages the power of the 3 pins involved. The resulting pin power is approximately 18% higher than the core average, which is relatively uniform for a reactor.

2.2.2 Modeling process and results

Five input data files were used in the PLTEMP/ANL V4.1 code to calculate the safety margins in the steady-state operation of JM-1 reactor with the HEU core at 19.08 kW. Sample calculations of the values used in the input files are given in Appendix A.

In the HEU core analysis, as described earlier, one input data file models the one-pin equivalent of the 296-pin core with a reactor power of 20.1 kW and a coolant inlet temperature of 24.3°C, replicating experimental conditions. The pin is modeled as a solid rod of U-Al alloy fuel meat of radius 2.108 mm in a 0.508 mm thick Al-alloy cladding, without any gap thermal resistance in-between, producing a total of $20.1/296 = 0.06791$ kW. This input data file, accompanied by the average axial power profile obtained with MCNP5, was used to calibrate the hydraulic resistance (minor pressure losses) in the PLTEMP/ANL model to reproduce an experimentally measured coolant temperature rise through the core of 22.4°C (from 24.3°C to 46.7°C) at that power level. The calibrated model was then used to simulate the other experimental conditions, changing only the reactor power and coolant inlet temperature to match those at the time of the experiments. The PLTEMP/ANL coolant outlet temperatures comparison with the experimental values was already presented in Figure 14.

Another input data file was used to determine the most limiting initial reactor operating conditions possible, so that the safety margin calculations are the most conservative as well. Given that the reactor power at which ONB is reached depends on inlet water temperature (the colder the inlet coolant, the higher the reactor power should be to reach nucleate boiling conditions), the highest possible coolant inlet temperature at nominal power operating conditions is chosen as reference. Taking into account that the JM-1 reactor instrumentation doesn't provide readings of the inlet temperature, the outlet temperature is used instead to infer the former using PLTEMP/ANL HEU calibrated model. According to the JM-1 reactor operation history, the highest outlet temperature ever recorded is 60°C. The corresponding inlet temperature was estimated using an equivalent HEU one-fuel-pin core model, with a power per fuel pin corresponding to the full nominal power of 19.08 kW ($19.08 \text{ kW} / 296 \text{ pins} = 64.46 \text{ W/pin}$) and with hydraulic resistance coefficients from previous calibration, until the resulting outlet temperature is 60°C. The resulting estimated maximum inlet temperature during regular operation at full power is 40.5°C.

The next input data file uses the above adjusted values of the hydraulic resistance coefficient and the channel inlet temperature, and models the peak power pin of the HEU core, with all six Hot Channel Factors (HCF) applied, given in Table 2. The purpose of this input data file is to determine the maximum allowed operating reactor power with all hot channel factors taken into account.

As mentioned above, since the geometry of the equivalent hot fuel pin cell or flow channel differs from the core average one, the same one-pin model approach is not suitable. As a result, a dual channel model comprising the hottest pin and the remainder of the core was used instead. To maintain the appropriate flow rate ratios between channels, the core average channel could not be modeled the same way it was in the two previous input files. That is, the average power pin channel should comprise 295.5 pins and the corresponding core flow section, instead of just 1/296 as before, while the hottest pin channel accounts for the other half of a pin, as outlined previously in Section 2.2.1.

Table 2: Uncertainties included in the six hot channel factors for the HEU core

(X implies that an uncertainty affects a hot channel factor)

	Uncertainty Type	Fractional Value	FPOWER	FFLOW	FNUSLT	FBULK	FFILM	FFLUX
LOCAL OR RANDOM UNCERTAINTIES								
1	Neutronics calculation of power density in a pin, u_1	0.10 ^a				X	X	X
2	²³⁵ U mass per pin, u_2	0.03 ^b				X	X	X
3	UO ₂ pellet radius, u_3	0.003 ^c					X	X
4	U enrichment in a pellet, u_{10}	0.016 ^c					X	X
5	Fuel pellet density, u_{11}	0.044 ^c						
6	Fuel pin radius, u_{12}	0.003 ^c				X	X	X
7	Fuel pin pitch, u_{13}	0.003 ^d				X	X	
8	Flow redistribution among channels, u_6	0.153 ^e				X		
SYSTEM-WIDE UNCERTAINTIES								
9	Reactor power determination uncertainty, u_7	0.003 ^f	X					
10	Flow uncertainty due to uncertainty in friction factor, u_8	0.0223 ^g		X				
11	Heat transfer coefficient uncertainty due to uncertainty in Nu number correlation, u_9	0.078 ^h			X			
	TOTAL VALUE		1.003	1.0223	1.078	1.1607	1.1151	1.1146

- a. This is a reference value that has been used in past conversion analyses, and is reported in the User's Guide to the PLTEMP/ANL Code [12].
- b. Typical value given in Appendix V of [12]. Typical fuel fabrication tolerance is used because specific tolerances for SLOWPOKE HEU fuel rod fabrication are not available; however, potential differences are not expected to be significant, thus not producing significantly different results.
- c. The typical value for early light water reactors given in [15].
- d. It is assumed to be equal to the uncertainty in pin radius.
- e. In natural circulation, each channel's flow is induced by the power of the single fuel pin in the channel. However, the hot channel's flow is dragged by the neighboring channel's flow. The conservative approach used to estimate this flow correction involved considering a weighted average of the flow velocity for the hot channel and its neighbors according to the shared perimeter between them and the wetted perimeter of the hot channel as weights. The estimate of the flow velocity of each neighboring channel is obtained based on eq. 13 of ref. [20]. Therefore, the coolant flow velocity in each of the channels varies approximately as $[\text{Power}]^{0.53} \times [\text{Flow Area}]^{0.47} \times [\text{Hydraulic Diameter}]^{0.94}$. In this case, the resulting averaged flow velocity is 84.7% of the original for the hot channel. The flow reduction coefficient is therefore of 15.3% ($1 - 0.847 = 0.153$).
- f. $u_7 = (\text{Peak pin-to-average pin power ratio}) \times 1.003 - 1.0$ where 1.003 represents the 3-sigma uncertainty, 0.3 %, in MCNP statistics for the calculated peak pin power.
- g. $u_8 = 0.5/22.4 = 0.0223$. This uncertainty in flow rate is due to the uncertainties in the coolant channel friction factor and hydraulic loss factors. The flow rate uncertainty equals the uncertainty in the measured coolant temperature rise of 22.4 °C in the thermal-hydraulic test at 20.1 kW because the PLTEMP/ANL hydraulic model is calibrated to this test. An uncertainty of 0.5 °C is assumed in the measured coolant temperature rise of 22.4 °C in the reactor. A fractional uncertainty of 0.5/22.4 in ΔT_c implies an equal uncertainty in the flow rate.
- h. The Churchill-Chu Nusselt number (Nu) correlation developed for natural circulation on a vertical flat plate and implemented in the code is used in this thermal hydraulic analysis for a vertical circular rod. The natural circulation Nu on a rod's vertical surface (convex surface) is greater than that on a flat vertical surface which is in turn greater than that on the inner surface of a tube (concave surface). The rod-to-plate Nu ratio was estimated using Eq. (4-44) of [16] to be about $\zeta / \ln(1 + \zeta) = 0.234 / \ln(1.234) = 1.113$ where $\zeta = 1.8 (L/D) / (\text{Nu}_{\text{plate}}) = 1.8(0.22/0.00523) / (\approx 324) = 0.234$. Hence, this uncertainty is lowered from the typical 20% to 7.8% in this analysis ($1.2/1.113 = 1.078$) to compensate for the extra conservatism.

System-wide or Global Hot Channel Factors:

- FFLOW = a factor to account for the uncertainty in total reactor flow
 FPOWER = a factor to account for the uncertainty in total reactor power
 FNUSLT = a factor to account for the uncertainty in Nusselt number correlation

Local Hot Channel Factors:

- FBULK = a hot channel factor for local bulk coolant temperature rise
 FFILM = a hot channel factor for local temperature rise across the coolant film
 FFLUX = a hot channel factor for local heat flux from cladding surface

As a first step, the previous one-pin equivalent model was modified to represent the total core dimensions. This basically involved multiplying by 296 all the required parameters, such as flow area, wetted perimeter, fuel plate (or pin) width, pin circumference and power to obtain the total original value. The resulting input was tested to ensure it provided exactly the same results as the previous average pin model. Once that was verified (validating both this model

version and the code's internal consistency), the hot pin cell (or channel) was modeled, as if both parts of the model were two different assemblies in the core. As explained in Section 2.2.1, the hottest pin model is chosen to represent just half of a pin ($3 \times 1/6$) and half of a regular core lattice cell. Its related geometrical parameters are calculated and introduced into the model, along with the friction coefficient ($f \cdot Re$) that corresponds to this channel geometry (165.2) instead of the core average (224.2). A power factor 18% higher than the core average is assigned as well to this assembly according to previous MCNP calculation results. These parameters are subsequently subtracted from the full core average model tested, to obtain the average model of the remaining 295.5 pins and flow area that constitutes the other fuel assembly of the core. The hydraulic resistance coefficient at the inlet and outlet and other parameters such as chimney height are identical for both assemblies/pins. The HCFs are also introduced into this model. Three of the HCFs are common for the whole core and therefore common for both assemblies. These correspond to the uncertainties in reactor power, the coolant flow in the channel and in the Nusselt number. The other three, related to the ONB, are specific to the hot pin assembly, and correspond to the bulk coolant temperature in the channel, the temperature drop across the coolant film on the cladding surface and the heat flux. The thermal hydraulic input information with the axial power distribution now contains two different profiles, one for the hottest pin and one for the core average. The resulting model is tested again with the same input temperature and power to provide the same overall output and 60°C outlet temperature in particular. Different conditions were tested as well to ensure consistent behavior. The output results used to compare between different conditions are those without the HCFs applied. These results are provided independently in the same output file.

Afterwards, the power at which ONB occurs was determined. The PLTEMP/ANL code keeps track of the onset of nucleate boiling ratio (ONBR), which is the ratio between the ONB heat flux and the current flux, and minimum ONBR. ONB is reached when the power is such that the minimum ONBR is equal to 1.0 for the hottest pin with all HCFs applied. The value of the power obtained is the true allowed reactor power, in the sense that it doesn't take into account possible instrumentation measurement errors.

The maximum allowed reactor operating power was found to be 46.30 kW for the JM-1 HEU core without the hot channel factors applied. With all six hot channel factors, the maximum allowed reactor steady state operating power (corresponding to the onset of boiling, i.e., $ONBR = 1.0$) is found to be 35.35 kW. These maximum allowed power levels correspond to actual reactor powers and not to measured powers, which could be subject to instrument measurement errors. These results illustrate that the JM-1 HEU research reactor, operating at full nominal power, has a reactor power safety margin of 1.85. The PLTEMP/ANL code predicts the coolant saturation temperature (the temperature at which boiling will begin in the coolant) to be 112°C which compares very well with the value of 111°C stated in [7]. Under normal operating conditions at maximum power, calculated parameters of the JM-1 reactor are given in Table 3.

In this part of the analysis it was assumed that, even at full power with an inlet temperature of 40.5°C, enough excess reactivity would be available to increase the power up to ONB levels. That is probably unrealistic, since according to the results previously shown in Figure 9, a

uniform reactor container water temperature rise up to 40.5°C would already decrease available excess reactivity by about 1.15 mk. Even immediately after a shim addition, and without taking into account the negative reactivity from neutron poison buildup during the process to reach those 40.5°C at the core inlet, the available excess reactivity would be no greater than 2.85 mk.

Table 3: Calculated operating parameters of the JM-1 HEU reactor

Parameter	Inlet at 20.0°C	Inlet at 40.5°C
Nominal Power (kW)	19.08	19.08
Average Fuel Pin Power (W)	64.5	64.5
Peak Fuel Pin Power (W)	80.3	80.3
Core Flow Rate (kg s ⁻¹)	0.201	0.239
Peak Clad Temp.(°C)	66.8	81.7
Average Fuel Temp. (°C)	52.8	68.9
Peak Fuel Temp. (°C)	67.0	81.9

3 LEU CORE SAFETY ANALYSIS

With the exception of the fuel pins and fuel cage, all the components of the HEU core assembly will be reused, without modification, in the new LEU core. The reference LEU core model for the JM-1 reactor has 198 fuel pins, which are 236.5 mm long and 5.26 mm in diameter, to match the existing and almost identical cores at École Polytechnique de Montreal (EPM) and the Royal Military College of Canada (RMC), from which experimental data is available. The LEU fuel will consist of sintered UO_2 pellets with an enrichment of $\sim 19.88\%$, while the cladding and the fuel cage will be manufactured from Zircaloy-4. The impact of other differences between JM-1 and reference cores aside from enrichment will be evaluated later on sections 3.1.10 and 3.2.3, along with the possibly required changes and their influence on the reference scenario. The total predicted uranium mass will be ~ 5660 g with ~ 1125 g being ^{235}U .

The reactor parameters estimated with the reference LEU core configuration model are compared to experimental data from the LEU-fueled SLOWPOKE-2 reactors at EPM and RMC. The proposed 198-pin LEU configuration is identical to that used at both EPM and RMC. The neutronics estimations were compared with those from EPM since the differences in reactor geometry between the JM-1 reactor and that at EPM were significantly less than those between JM-1 and the RMC SLOWPOKE-2. Structures at the RMC reactor that are not present in the JM-1 reactor include flooded irradiation sites, a thermal column (filled with D_2O) and a neutron beam port among others, all of which affects system reactivity to some extent. The only major difference between the JM-1 reactor and the EPM reactor is that the JM-1 reactor has only one large outer irradiation site while the EPM reactor has 2 large and 3 small outer irradiation sites. This was observed to have a small but significant effect on the system reactivity, which is discussed in detail in Section 3.1.1. Other slight differences are likely to exist between the JM-1 and EPM reactors with regard to fuel composition and other aspects which are not known, due to a lack of such fine detailed information about the EPM SLOWPOKE-2, and therefore are not included in the model. The comparison presented here with the EPM reactor is meant as general reference but it is not expected nor warranted to be 100% accurate, nor intended as rigorous validation of the model.

The experimental thermal hydraulic data from the EPM reactor differ substantially from that of RMC, even though both reactor cores are expected to be identical with regards to coolant flow through the core. The two sets of data are difficult to compare directly, as the inlet temperature ranges of both experiments are significantly different and all data points are at different power levels. Given that temperature greatly affects water's viscosity, and hence its flow, the resulting temperature differences between inlet and outlet cannot be compared directly. After data adjustments to account for this effect using PLTEMP/ANL, the estimated temperature rise differences between the EPM and RMC reactors at identical power levels reached values of more than 3°C , which cannot be explained by any reasonable measurement error margin. It also wasn't possible to replicate the experimental results from EPM reactor using PLTEMP/ANL regardless of the calibration data point used, further suggesting some issues with that data set. For this reason the RMC reactor experimental results were used as reference to calibrate the PLTEMP/ANL LEU core model and for subsequent validation purposes.

3.1 NEUTRONIC ANALYSIS

The JM-1 LEU model incorporated data obtained from the final fuel composition assay and is based on what the configuration will be at the time of the LEU core commissioning. As such, this model includes two self-powered cadmium flux detectors; one operational flux detector in irradiation site #2 and a non-operational detector that got stuck inside the annular beryllium reflector. The LEU core reactor version, as modeled in MCNP5, is illustrated in Figure 16, showing the proposed 198 fuel pin configuration.

Figure 16: Horizontal mid-core cross-section of the proposed JM-1 LEU reactor core

3.1.1 Core reactivity

The MCNP5 estimated excess reactivity of the LEU core was 4.89 ± 0.08 mk with the impurities concentration in the beryllium reflectors set to 66% of the detection limits. The difference in the number of outer irradiation sites between the EPM LEU reactor and JM-1 (5 versus 1, respectively) has an effect on reactivity, as does the second neutron flux detector in the JM-1 reactor. If the additional four outer irradiation sites, 2 large and 2 small, are incorporated into the model, while removing the second flux probe, the estimated excess reactivity is reduced by about 0.6 mk. The resulting excess reactivity discrepancy with the reference value of 3.69 mk drops to just about 0.6 mk, which is a good agreement, especially when compared to the overestimations of 24 mk and 6 mk previously reported [5,17].

3.1.2 Control rod worth

The worth of the control rod was calculated to be 5.44 ± 0.12 mk. This $\sim 3.5\%$ reduction in the control rod worth compared to the calculated value for the HEU core (5.63 ± 0.12 mk) is expected based on experimental evidence where a similar 4% reduction was observed with the HEU to LEU conversion of the EPM SLOWPOKE-2 reactor. This reduction is basically due to the fact that the LEU core is less transparent to neutrons, given the higher amount of ^{238}U present,

which significantly contributes to neutron absorption. Hence, the control rod has a somewhat more local effect on neutron flux, slightly decreasing its worth.

3.1.3 Reactivity worth of top reflector

As already mentioned in Section 2.1.3, the JM-1 reactor can accommodate a maximum shim thickness of 10.2 cm and the estimated top beryllium reflector incremental reactivity worth in LEU core is illustrated in Figure 17.

Figure 17: Calculated differential and cumulative reactivity worth of Be shims in LEU core

3.1.4 Flux distribution in irradiation channels

In order for the $10^{12} \text{ n} \cdot \text{cm}^{-2} \text{ s}^{-1}$ flux in the inner irradiation channels to be maintained with the HEU to LEU conversion, a $\sim 11\%$ increase in power will be required, resulting in a full nominal power of 21.23 kW with the LEU core. A similar increase was also observed to be true for the EPM reactor whose LEU nominal thermal power at a flux of $10^{12} \text{ n} \cdot \text{cm}^{-2} \text{ s}^{-1}$ was 19.84 kW, up from 17.2 kW with the HEU core, a $\sim 15\%$ increase [18,19]. The new reactor power estimate assumes the same amount of energy released per fission and the same contribution from decay heat and gammas generated by neutron capture. Given the different composition of the fuel, fuel cladding and fuel cage, which also contribute to modify the neutron energy spectrum to some extent, the previous energy parameters cannot be considered to be perfectly constant. However, the actual differences are considered to be of no significance for the precision level required in these calculations. A temperature of 20.45°C (matching the available cross section

libraries) is assumed for the neutron flux and power distributions of both HEU and LEU cores; having verified that the relative changes as a function of temperature are also negligible. Therefore, the LEU core nominal power value remains valid at higher temperatures.

The flux distributions at 21.23 kW in the LEU core and 19.08 kW in the HEU core in the inner and outer irradiation channels are compared in Figure 18.

Figure 18: Calculated thermal flux distribution in the irradiation channels at nominal power

3.1.5 Axial flux distribution

Figure 19 shows the calculated axial thermal flux distribution in the reference JM-1 LEU core configuration at the predicted nominal full power of 21.23 kW at a radial distance from core axis of 4.6 cm. The calculated axial power form factor is 1.13, while the radial offset is 1.53. The total power form factor is 1.76 for the LEU configuration.

3.1.6 Moderator void reactivity coefficient (MVRC)

The average LEU-core moderator void coefficient for JM-1, calculated with MCNP5, is -2.65 mk/% of void fraction. The obtained value is equivalent to -0.035 mk/cm³ (core water volume of ~ 8.0 L) and the reactivity change with void volume is illustrated in Figure 20. This compares very well with the calculated value of -0.033 mk/cm³ and a measured value of -0.036 mk/cm³ in the EPM LEU Safety Analysis Report [17].

Figure 19: Axial thermal flux comparison between LEU core (21.23 kW) and HEU (19.08 kW)

Figure 20: Calculated excess reactivity change with void volume in the LEU core

3.1.7 Fuel temperature coefficient of reactivity

Heating of LEU fuel will cause Doppler broadening of the uranium nuclides' neutron absorption resonances increasing their absorption of thermal neutrons. The increase in thermal neutron absorption by the predominant ^{238}U nuclide in the LEU fuel results in a negative reactivity feedback as ^{238}U removes thermal neutrons from the system without fissioning. The reactivity change as a function of temperature was estimated with MCNP5 by varying the fuel temperature and using temperature dependent fuel cross-section libraries to estimate the excess reactivity values, which were subsequently plotted as shown in Figure 21. The calculated fuel temperature coefficient for the JM-1 LEU core is $-0.008 \text{ mk}/^\circ\text{C}$ compared to a calculated value based on experimental data of $-0.009 \text{ mk}/^\circ\text{C}$ for the EPM reactor [17].

Figure 21: Calculated excess reactivity as a function of average fuel temperature in the LEU core

3.1.8 Combined temperature reactivity response

The combined or overall temperature coefficient of reactivity for the LEU core was estimated by adjusting the temperature (and density) of the pool and reactor water, as well as the temperature of all other submerged components (fuel, beryllium reflector...), from 20.5°C to 45°C. These specific conditions and reactivity coefficient are analyzed here instead of the moderator temperature coefficient mainly because of the availability of experimental measurements that do allow proper validation of the results. All the experimental data as well as the MCNP5 simulation results were rescaled to the same maximum excess reactivity value to allow an easier and more direct comparison of the reactor behavior under these conditions without the interference of small possible inaccuracies in the total excess reactivity calculations or different initial conditions at the start of the experiments. This does not affect the precision of the analysis since what is relevant, in terms of reactivity coefficients, are the relative variations of the reactivity as a function of temperature. The resulting comparison, illustrated in Figure 22, shows that the calculated reactor behavior fits the experimental data very well.

Figure 22: Calculated JM-1 reactivity versus moderator temperature compared to experimental data

The procedure to obtain the LEU experimental data at EMP involved heating the reactor pool water to the desired levels by operating the reactor for some time and waiting several hours (~64 hrs) for temperature equilibrium to be reached throughout the reactor. Afterwards, the reactor period was determined by measuring the time required, after startup, for the reactor power to increase from 40% to 90% of preset reactor power levels. The reactor period obtained was then used to estimate the total excess reactivity as a function of moderator temperature. Two such experiment series were performed at the EPM reactor, from 0.8 kW to 1.8 kW and from 8 kW to 18 kW, to determine whether the quicker fuel temperature change at the higher reactor power would significantly affect the measured excess reactivity behavior as a function of temperature, concluding it was not so. The earlier set of results from RMC (used as reference by AECL) were obtained by first running cold or hot water through the heat exchanger to adjust the pool temperature to the desired levels, operating the reactors at 100 W, and determining reactivity by control rod balance once the core temperature had stabilized.

The reason the reactivity peak in the LEU core occurs at 33°C instead of ~15°C, as in the HEU core, is due to reactor undermoderation. Since there are more fuel pins and less water in the HEU core than in the LEU one, the HEU core is more undermoderated. That is evidenced by the magnitude of the void coefficient, being -3.13 mk/% of void in the HEU core versus -2.62 mk/% in the LEU core (Sections 2.1.6 and 3.1.6). Consequently, if it is assumed that the magnitude of the factors contributing to a negative temperature coefficient are smaller in the LEU core than in the HEU core, that these magnitudes decrease as temperature decreases and approaches 4°C (maximum water density), and that the magnitude of the factors contributing to a positive coefficient are approximately equal in both cores, the temperature at which positive and

negative factors balance each other is expected to be higher in the LEU core. The fuel temperature coefficient adds a small negative contribution to reactivity, of about -0.2 mk over the range from 20°C to 45°C , and about -0.1 mk at 33°C , which only partially compensates the smaller moderator temperature coefficient (Sections 2.1.9 and 3.1.9).

3.1.9 Reactor coolant temperature coefficient of reactivity

Based on the previously validated results for reactivity evolution as a function of reactor pool temperature, the reactor water temperature coefficient of reactivity is analyzed in this section. The temperature of all other reactor components, namely, the fuel and beryllium reflectors, remains unchanged through this analysis despite the change of coolant temperature. These conditions are obviously not possible to replicate experimentally, so no specific validation data is available for comparison. The simulation results reported here are valuable to understand the reactor behavior and related physical phenomena and are particularly useful for some aspects of the transients analysis presented later in Section 3.3.

The analysis performed for the HEU core in Section 2.1.9 showed that a distinction needs to be made between the water within the core and the remainder of the reactor container water, surrounding the beryllium reflectors. The same is true for the LEU. The neutron flux energy spectrum differs widely between the core and the regions around the reflector. Figure 23 shows the radial neutron flux profile for different energy groups, illustrating the difference in the neutron flux spectrum between the core and the water annulus surrounding the annular reflector.

Figure 23: LEU radial neutron flux at mid-core height

Figure 24 shows a comparison of the total excess reactivity as a function of temperature between the case of uniform reactor container water temperature and the case where only the coolant temperature within the core is allowed to change. As it can be seen, the effects of fuel and beryllium reflector temperature on reactivity are minor and the general reactivity trend as a function of temperature in the uniform case is very similar to the results previously shown in Figure 22. On the other hand, no reactivity peak is observed at low temperature when only the core coolant heats up. In that case, the reactivity drops significantly from the start as temperature rises. The gap between the two curves is essentially identical in magnitude to the HEU case (Figure 11) along the entire temperature range.

Figure 24: Total excess reactivity as a function of coolant temperature for the LEU core

The explanation for the observed behavior is also analogous to the one for the HEU core. The water surrounding the beryllium reflector has a minor moderation role, since the neutron flux is almost thermalized in that region. Its role as a reflector is not significantly affected by the density decrease that accompanies the temperature rise. The mean neutron free path may be increased slightly by the density drop but the effectiveness as a reflector is not modified significantly. The reactivity decreases at a very low rate of -0.19 ± 0.04 mk for each percent of void or density reduction of the water reflector, compared to the core coolant void coefficient of -2.62 mk/% (Section 3.1.6). Consequently, the effect of temperature on neutron cross sections becomes significantly more noticeable. The reason behind it is the same described in Section 3.1.8. The reactivity peak is observed at low temperature for the LEU core, when the water density gradient as a function of temperature becomes smaller, turning the temperature effects upon neutron cross sections dominant on reactivity evolution. Since density changes on the water around the beryllium reflector have very little impact on reactivity, regardless on the temperature, the dominant driver for reactivity evolution are the effects of temperature on cross sections. The reactivity would consequently increase as the water reflector temperature

risers. As seen in Section 3.1.8 the overall moderator temperature coefficient is negative above 33°C, because the previous effect is offset by the dominant reactivity influence of the water properties inside the core. During actual operation, particularly at high power, this effect is enhanced by the fact that the core coolant temperature is significantly higher than the water reflector temperature.

3.1.10 Predicted core configuration at commissioning

The neutronic analysis carried out so far has used the reference 198 fuel pins core configuration found at both EPM and RMC. The results agreement with experimental data is very good, especially if the slight known differences existing between EPM and JM-1 reactors are taken into account. However, as stated in Section 3.1.1, the predicted excess reactivity is still somewhat higher than the maximum excess reactivity allowed for the SLOWPOKE-2. Additionally, the reference diameter for fuel pellets (4.165 mm) and fuel pins (5.26 mm) have been used in the analysis but actual fuel manufacturing data by AECL became available, which takes into account fabrication tolerances, resulting in an average pellet diameter of 4.174 mm and average pin outer diameter of 5.32 mm (0.537 mm thick), as well as a pellet density of 10.59 g/cm³ (compared to the reference 10.6 g/cm³), for a total stack effective density of 10.55 g/cm³. Although the differences are not substantial, they have a small effect on reactivity estimates at the precision level we are considering in this study.

The most significant outcome of all the combined differences between JM-1 and the reference EPM and RMC reactors is that, according to our calculations, the excess reactivity will exceed the 4 mk limit established for this reactor design if 198 fuel pins are used, even without any beryllium shims. Our results show that to avoid exceeding the 4 mk threshold the maximum number of fuel pins allowed in the reactor will need to be 196. The effect of neutron fluence in the composition of the beryllium reflector has also been taken into account according to the operation history of the JM-1, amounting to 97 MWh over 30 years. The estimated effect is relatively small, about 0.99 ± 0.1 mk, but it has been considered pertinent to assess its possible impact, given previous existing evidence of significant neutron fluence effects in the composition of beryllium reflectors in other reactor designs [21].

The resulting estimated excess reactivity with 196 fuel pins, taking into account all previously mentioned considerations is 3.63 ± 0.06 mk at 20.45°C (3.89 mk at 33°C), with an associated control rod worth of 5.53 ± 0.1 mk. An additional fuel pin would bring the total excess reactivity to about 4.55 ± 0.08 mk, exceeding the 4.0 mk limit. Adding half a beryllium shim 1.6 mm thick (1/16") on the empty tray for a reactivity increase of about 0.4 mk would also exceed the 4.0 mk limit. Small uncertainties exist in many of the parameters used, making it unrealistic to warrant a degree of accuracy of less than 0.12 mk, and thus potentially bringing excess reactivity above 4.0 mk at 33°C. That would result in a final load of just 195 pins (3.13 ± 0.06 mk at 33°C), likely with an additional 1/16" beryllium shim. The commissioning process involves a step by step fuel loading approach precisely to avoid any issues related to inaccuracies in the reactivity estimations, preventing the reactivity limit from being exceeded, while getting as close as possible to the maximum. Nonetheless, a final value is provided as a best estimate in this report to allow a detailed comparison with the final commissioning configuration. Its accuracy can be later compared with previous equivalent estimation attempts [4,5].

3.2 THERMAL HYDRAULIC ANALYSIS

The PLTEMP/ANL model of the LEU core retains the same lattice but uses far fewer pins compared to the HEU core, which further exacerbates the lack of homogeneity issues highlighted in Section 2.2.1. The reference core model, as already mentioned (section 3), involves 198 fuel pins, according to previous experience on EPM and RMC reactors LEU configurations. A lower uranium enrichment of about 12.5% ^{235}U could be an alternative to obtain a uniform 342-pin core, but it was considered more important to take advantage of the available data and 45 reactor-years of combined successful operational experience with the existing LEU SLOWPOKE-2 reactors. Accordingly, it was decided to go ahead with a LEU reactor version that will essentially be a replica of the existing ones.

3.2.1 LEU core reference thermal hydraulic model

A similar procedure to that employed for the HEU and outlined in Section 2.2.1 was followed for the thermal hydraulic LEU model, which was calibrated, in terms of coolant temperature increase versus reactor power, using the experimental data from the RMC SLOWPOKE-2 reactor. The first of the five input data files is the one-pin equivalent of the 198-pin LEU core with a reactor power of 21.23 kW and a coolant inlet temperature of 21°C. The pin is modeled as a solid UO_2 rod of 2.0825 mm radius, surrounded by a 0.5091 mm thick Zircaloy-4 cladding of outer radius 2.63 mm, having a He-filled gap between fuel and cladding to improve heat transfer (thermal resistance of $0.000283 \text{ m}^2\cdot\text{K}/\text{W}$), and with a corresponding total power generation of $21.23 \text{ kW} / 198 \text{ pins} = 107.22 \text{ W/pin}$. This input data file, accompanied by the average axial power profile obtained with MCNP5, was used to calibrate the pressure loss coefficients in the PLTEMP/ANL model so that it could reproduce an experimentally measured coolant temperature rise of 22°C (from 21°C to 43°C) at 21 kW in the RMC reactor. The calibrated model was then used to simulate the other experimental conditions, changing only the reactor power and coolant inlet temperature to match those at the time of the experiments. The comparison between RMC experimental data and the PLTEMP/ANL LEU model is shown in Figure 25.

In the LEU core, the highest power pins are located in the innermost “ring”, shown in Figure 26. In this case however, the pins with the highest power to coolant flow ratio are also located in the innermost “ring”. Even though this ratio is useful as a first approximation to identify the hottest channel, if significant power differences exist between fuel pins it’s not that straightforward to identify the hottest pin or, more specifically, the one that would reach ONB at the lowest reactor power. In this case two candidates are considered, one corresponding to the fuel pin with the highest power (channel A) and the other corresponding to the highest power to flow area ratio (channel B), which would be the flow channel with the highest coolant temperature. In both cases, the corresponding channels flow areas are substantially larger than in the case of the hottest pin of the HEU core (3.24 and 1.59 times larger, respectively) due to the larger number of vacant pin positions in the lattice. However, the power per pin is also larger (76.1 W for the HEU versus 164.1 W and 135.4 W for channel A and B respectively). The power of the LEU core hottest pin candidates is 53.0% and 26.3% higher than the core average, indicating a significantly sharper radial power profile than in the HEU case.

Figure 25: Calculated and experimental coolant outlet temperatures in the RMC LEU reactor

Figure 26: Reference JM-1 LEU core power distribution and individual coolant channels segmentation

3.2.2 Modeling process and results

The methodology of the LEU core analysis is analogous to the HEU core one, with only a few additional changes to the model. The number of fuel pins in the reference model is 198, their dimensions are different, both in diameter and length, the materials and their thermal conductivities are different (and actually temperature dependent) and there is a gap between the fuel pellets and the cladding, resulting in an additional heat transfer resistance. Specific axial power profiles were also generated with MCNP5 for the LEU model.

The maximum inlet temperature at full power obtained for an outlet temperature at nominal power (21.23 kW) of 60°C is also 40.5°C. In the case of the LEU core, unlike in the HEU core, the hottest pin channel of the dual-channel (or dual-assembly) model to analyze the ONB comprises a full fuel pin and its corresponding coolant channel. The second assembly accounts for the remaining 197 fuel pins. The friction coefficient ($f \cdot Re$) used, based on the weighted average of its value within each of the flow channels shown on Figure 26 and following the procedure described in Section 2.2.1, is 218.3 for the whole core average, 145.4 for the channel A and 188.9 for the channel B. The precise $f \cdot Re$ values however have a minor influence on the results, since the flow is mostly constrained by the core inlet and outlet flow areas. The HCFs for the LEU core are given in Table 4.

The maximum allowed reactor operating power was found to be 39.75 kW for the JM-1 LEU core without the hot channel factors applied and assuming channel B fuel pin to be the hottest (Figure 26). The reactor power needed to achieve onset of boiling (ONBR = 1.0) in channel A is slightly higher (40.15 kW). With all six hot channel factors, the true maximum allowed reactor steady state operating power before ONB in channel B is found to be 31.05 kW. This is the true allowed power in the sense that there is no allowance in this value for any error in the power measuring instrument. Therefore, this maximum allowed power level corresponds to actual reactor power and not to the measured power, which could be subject to instrument measurement errors. When taking all HCFs into account, the results show the possibility of ONB occurring on the surface of channel A pin above 31.7 kW. Therefore, channel B remains the most limiting channel according to these calculations. This outcome is strongly related to the flow redistribution factors applied in the model, larger for hot channel B (see Table 4) due to the lower average flow velocity in the virtual flow channels surrounding it. The later conditions are therefore selected as the most conservative reference scenario. These results indicate that the JM-1 LEU research reactor, operating at full nominal power, has a predicted reactor power safety margin of 1.46. Calculated parameters of the JM-1 LEU reactor under normal operating conditions at maximum power are given in Table 5.

These calculated steady state power limits determined based on ONB may not be physically possible to achieve in sustained operation. This is due to insufficient excess reactivity available to compensate for the negative reactivity insertion resulting from the increase of operating temperatures and the combined effect of neutron poison buildup in the fuel.

Table 4: Uncertainties included in the six hot channel factors for the LEU core

(X implies that an uncertainty affects a hot channel factor)

	Uncertainty Type	Fractional Value	FPOWER	FFLOW	FNUSLT	FBULK	FFILM	FFLUX
LOCAL OR RANDOM UNCERTAINTIES								
1	Neutronics calculation of power density in a pin, u_1	0.10^a				X	X	X
2	^{235}U mass per pin, u_2	0.03^b				X	X	X
3	UO_2 pellet radius, u_3	0.003^c					X	X
4	U enrichment in a pellet, u_{10}	0.016^c					X	X
5	Fuel pellet density, u_{11}	0.044^c						
6	Fuel pin radius, u_{12}	0.003^c				X	X	X
7	Fuel pin pitch, u_{13}	0.003^d				X	X	
8	Flow redistribution among channels, u_6	$0.012 / 0.110^e$				X		
SYSTEM-WIDE UNCERTAINTIES								
9	Reactor power measurement uncertainty, u_7	0.003^f	X					
10	Flow uncertainty due to uncertainty in friction factor, u_8	0.0227^g		X				
11	Heat transfer coefficient uncertainty due to uncertainty in Nu number correlation, u_9	0.10^h			X			
	TOTAL VALUE							
	Hot Channel A		1.003	1.0227	1.100	1.0533	1.1148	1.1146
	Hot Channel B		1.003	1.0227	1.100	1.1206	1.1149	1.1146

- This is a reference value that has been used in past conversion analyses, and is reported in the User's Guide to the PLTEMP/ANL Code [12].
- Typical value given in Appendix V of [12]. Typical fuel fabrication tolerance is used because specific tolerances for SLOWPOKE HEU fuel rod fabrication are not available; however, differences are not expected to be significant, thus not producing significantly different results.
- The typical value for early light water reactors given in [15].
- It is assumed to be equal to the uncertainty in pin radius.
- In natural circulation, each channel's flow is induced by the power of the single fuel pin in the channel. However, the hot channel's flow is dragged by the neighboring channel's. The conservative approach used to estimate this flow correction involved considering a weighted average of the flow velocity for the hot channel and its neighbors according to the shared perimeter between them and the wetted perimeter of the hot channel as weights. The estimate of the flow velocity of each neighboring channel is obtained based on

eq. 13 of ref. [20]. Therefore, the coolant flow velocity in each of the channels varies approximately as $[\text{Power}]^{0.53} \times [\text{Flow Area}]^{0.47} \times [\text{Hydraulic Diameter}]^{0.94}$. In this case, the resulting averaged flow velocity is 98.8% of the original for the hot channel A and 89.0% for the hot channel B. The flow reduction coefficients are therefore 1.2% and 11.0% respectively.

- f. $u_7 = (\text{Peak pin-to-average pin power ratio}) \times 1.003 - 1.0$ where 1.003 represents the 3-sigma uncertainty, 0.3 % , in MCNP statistics for the calculated peak pin power.
- g. $u_8 = 0.5/22 = 0.0227$. This uncertainty in flow rate is due to the uncertainties in the coolant channel friction factor and hydraulic loss factors. The flow rate uncertainty equals the uncertainty in the measured coolant temperature rise of 22 °C in the thermal-hydraulic test at 20.53 kW because the PLTEMP/ANL hydraulic model is calibrated to this test. An uncertainty of 0.5 °C is assumed in the measured coolant temperature rise of 22°C in the reactor. A fractional uncertainty of 0.5/22 in ΔT_c implies an equal uncertainty in the flow rate.
- h. The Churchill-Chu Nusselt number (Nu) correlation developed for natural circulation on a vertical flat plate and implemented in the code is used in this thermal hydraulic analysis for a vertical circular rod. The natural circulation Nu on a rod's vertical surface (convex surface) is greater than that on a flat vertical surface which is in turn greater than that on the inner surface of a tube (concave surface). The rod-to-plate Nu ratio was estimated using Eq. (4-44) of [16] to be about $\zeta / \ln(1 + \zeta) = 0.187 / \ln(1.187) = 1.091$ where $\zeta = 1.8 (L/D)/(\text{Nu}_{\text{plate}}) = 1.8(0.227/0.00526)/(\approx 415) = 0.187$. Hence, this uncertainty is lowered from the typical 20% to 10% in this analysis ($1.2/1.091 = 1.1$) to compensate for the extra conservatism.

System-wide or Global Hot Channel Factors:

- FFLOW = a factor to account for the uncertainty in total reactor flow
 FPOWER = a factor to account for the uncertainty in total reactor power
 FNUSLT = a factor to account for the uncertainty in Nusselt number correlation

Local Hot Channel Factors:

- FBULK = a hot channel factor for local bulk coolant temperature rise
 FFILM = a hot channel factor for local temperature rise across the coolant film
 FFLUX = a hot channel factor for local heat flux from cladding surface

Table 5: Calculated operating parameters of the JM-1 LEU reactor

Parameter	Inlet at 20.0°C	Inlet at 40.5°C
Nominal Power (kW)	21.23	21.23
Average Fuel Pin Power (W)	107.2	107.2
Peak Fuel Pin Power (W)	164.1	164.1
Core Flow Rate (kg/s)	0.225	0.265
Peak Clad Surface Temp. (°C)	74.4	89.1
Average Fuel Temp. (°C)	73.5	89.0
Peak Fuel Temp. (°C)	103.0	117.8

There is an additional safety consideration related to the HEU and LEU cores fuel material properties. First of all, regarding the fuel meat, the materials involved are UAl_4 and UO_2 for the HEU and LEU cores, respectively, whose corresponding melting temperatures are 731°C and 2865°C. Secondly, with regard to the fuel cladding materials, they are ASTM-1050 aluminum with a melting point of 650°C, and Zircaloy-4 with a melting point of 1850°C for the HEU and

LEU cores. The cladding surface temperature should actually be limited to a lower temperature than that of melting to prevent rapid oxidation and deterioration of mechanical properties. The NRC established limit for light water nuclear power reactors can be used as reference, resulting in a temperature limit of 1204°C [22]. The actual temperature limit for the aluminum cladding should also be lower than the mentioned melting temperature but there's no need to establish a realistic value here. While operation of the reactor with the LEU fuel will see higher peak fuel temperatures being achieved, the safety margin between the peak fuel temperature and the fuel melting temperature will increase far more drastically, from ~649°C for the HEU to ~2757°C for the LEU core. There will also be a very substantial increase in the safety margin between peak fuel cladding temperature and its temperature limit from ~568°C for the HEU to ~1096°C for the LEU. This results in greater safety margins for the LEU fuel.

3.2.3 Predicted core configuration at commissioning

As stated in Section 3.1.10, the core configuration that is predicted to achieve the maximum allowed excess reactivity according to our estimates will have 196 fuel pins instead of the 198 of the reference model. The final fuel loading will also have some slightly different parameters, according to final fuel manufacturing tests. These new values are listed at the beginning of section 3.1.10. Therefore, the previous thermal hydraulic analysis may not apply precisely to the actual LEU core. In this section the summarized results for a fuel cage with 196 fuel pins is presented to ensure that this change does not significantly affect the reactor safety margins. The set of results for this 196 pins core configuration, along with the comparison with the 198 fuel pin model, can also be used as a general reference for the results expected in case the final fuel loading at commissioning was, for instance, 195 or 197 fuel pins. Performing a detailed analysis for each possible configuration within credible error margin is not justified based on the small outcome differences observed, which do not significantly affect safety parameters nor the corresponding conclusions.

The power form factors are not affected by the change from 198 to 196 fuel pins configuration. Axial, radial and total power form factors are 1.13, 1.53 and 1.76. The total reactor power to achieve $10^{12} \text{ n}\cdot\text{cm}^{-2}\text{s}^{-1}$ in the inner irradiation sites is reduced slightly to 20.84 kW, while the power per pin also decreases to 106.33 W. The highest power pin generates 163.0 W, while the hottest pin power, which is the most relevant one for this analysis, is 134.47 W.

The reactor power to reach ONB at the hottest pin with an inlet temperature of 40.5°C and without taking uncertainties into account would be 39.8 kW, while when considering all HCFs the power would be 31.95 kW, just slightly higher than the 31.85 kW in the 198 fuel pins configuration case. Apparently, the small differences in terms of higher average power per pin are compensated by small differences in power distribution from the neutronics side. Since the nominal power would be also slightly smaller in the 196 pins core, the reactor power safety margin would be a little larger, 1.53 instead of 1.50.

A comparison of some additional reactor parameters between 196 and 198 fuel pins core configurations are summarized in Table 6. The differences are minimal and of no safety or performance significance. It is expected that other possible configurations involving the addition or removal of another fuel pin will not result in relevant changes to any of the key

reactor parameters either. This provides additional confidence in the reactor behavior despite possible eventual small core configuration changes during commissioning.

Table 6: Operating parameters comparison between JM-1 LEU reactor core configurations (40.5°C inlet)*

Parameter	198 Fuel pins	196 Fuel pins
Nominal Power (kW)	21.23	20.84
Average Fuel Pin Power (W)	107.2	106.3
Peak Fuel Pin Power (W)	164.1	163.0
Core Flow Rate (kg/s)	0.265	0.263
Peak Clad Surface Temp. (°C)	88.8	88.5
Average Fuel Temp. (°C)	91.7	91.3
Peak Fuel Temp. (°C)	109.2	108.7

* Hot Channel Factors not considered.

3.3 SELF-LIMITING POWER EXCURSION BEHAVIOUR

As previously discussed, overpower transients as a result of hypothetical reactivity excursions are difficult to model accurately on reactors with natural circulation. Despite that also applies to some extent to regular operation, for single-phase flow the combination of established theoretical flow principles and suitable calibration, using experimental data as reference, can provide accurate and reliable models, as shown in Figure 14 (Section 2.2.1) and Figure 25 (Section 3.2.1). The situation changes when dealing with transients that go beyond ONB conditions. That may be the case for transients resulting from relatively large reactivity insertions. In that kind of transients a significant amount of steam may be produced in the core, altering the normal flow behavior. For reasonably accurate predictions of the reactor flow and relevant fuel parameters in such conditions complex computational fluid dynamics (CFD) and heat transfer codes would be required, coupled with neutronics models that determine the flux and power profile in the core as the flow evolves, along with additional experiments to properly validate the CFD model under such conditions. This kind of approach would also involve demanding computational requirements and very detailed geometric models. This would be significantly more complex, costly and unpractical due to the lack of suitable data for validation. The significant number of experiments that physically tested the reactor behavior far beyond the maximum achievable reactivity insertion in the JM-1 allows avoiding such detailed modeling and analysis effort.

The reactivity excursion experiments carried out with SLOWPOKE reactors covered reactivity insertions of up to 6.48 mk on SLOWPOKE-1 HEU cores (Chalk River) [7], 6.05 mk on SLOWPOKE-2 HEU cores (Tunney's Pasture) [7] and up to 4.3 mk on SLOWPOKE-2 LEU cores (EPM) [17]. The maximum credible insertion for the JM-1 LEU core is 4.83 ± 0.12 mk, which would correspond to a full control rod withdrawal with maximum allowed excess reactivity immediately after commissioning or shims addition (4.0 mk), plus the reactivity insertion resulting from a flooded inner irradiation tube and 7 ml samples of a good moderator in all other available irradiation sites. The configuration that actually results in the highest overall reactivity is a moderator sample in each of the inner available irradiation sites (4 samples) and the flooding of the outer large irradiation site. The flooding of the inner irradiation tube hosting

the neutron flux detector and 5 additional moderator samples adds slightly less reactivity. The simultaneous occurrence of all these events is highly unlikely, so the analysis conditions are considered to be very conservative.

Since that the reactor conditions resulting from a large reactivity insertion are expected to be the most demanding for the reactor components in terms of temperature, the analysis will be addressed here using three complementary approaches, providing greater assurance by the agreement of their respective conclusions.

3.3.1 Reactor kinetics parameters

Experimental tests on HEU-fueled SLOWPOKEs show that larger reactivity insertions than the credible maximum value for the JM-1 LEU reactor result in self-limiting power excursions that safely bring the reactor to stable conditions without damage to the fuel [7,17]. That is an essential feature of the reactor design, with core inlet and outlet orifice areas (unchanged by the reactor conversion) specifically selected to accomplish that goal. Assuming a similar flow response, what is key to ensure those experimental results also apply to this safety analysis is to show that all relevant kinetic parameters of the reactor response fit those of the previously tested reactors, so that no differences may lead to significantly different outcomes. Regarding thermal hydraulics, it has been shown that the developed models match the behavior of the existing reactors closely, and that since the geometry is also identical to the LEU cores, the flow response is also the same. On the other hand, reactivity coefficients analysis with MCNP5 has shown that the estimated LEU core reactivity feedbacks are strongly negative under operating conditions and the obtained values are very comparable to what was expected. The additional relevant characteristics of the new LEU core to ensure consistent behavior with experimental data are the delayed neutron fraction and the average neutron lifetime.

The effective delayed neutron fraction value (β_{eff}) obtained with MCNP5 for the HEU core version is $0.787 \pm 0.012\%$. The latter compares very well with the estimated value of 0.788% reported in the JM-1 SAR [7]. In that same report, the stated average neutron lifetime (l) used for related calculations is 8.1×10^{-5} s, while the result obtained from MCNP5 is more than twice as much, $1.757 \times 10^{-4} \pm 0.015 \times 10^{-4}$ s. The corresponding values used in the EPM SAR [17] for the HEU core are $\beta_{eff} = 0.821\%$ and $l = 7.64 \times 10^{-5}$ s. In the case of the LEU core, the figures used are $\beta_{eff} = 0.803\%$ and $l = 7.65 \times 10^{-5}$ s. The results obtained with MCNP5 for the LEU core are $\beta_{eff} = 0.787 \pm 0.006\%$ and $l = 1.6592 \times 10^{-4} \pm 0.0128 \times 10^{-4}$ s. Therefore, the predicted delayed neutron fraction remains essentially unchanged between core versions, not showing the slight decrease predicted on EPM SAR, while its absolute value is slightly lower. Nevertheless, it remains at the same level of the already tested HEU core and well above the maximum credible reactivity insertion and therefore distant from any chance of prompt criticality. On the other hand, the neutron lifetime is slightly reduced in the LEU core compared to the HEU, though still more than twice the value used in the EPM SAR calculations. As a result, the expected reactor period for high reactivity insertions would be longer than that previously predicted, though it would hardly be noticed at the excess reactivity ranges available for this reactor.

3.3.2 Critical heat flux

Another approach that helps provide confidence in the safe reactor behavior in the case of accidental reactivity insertions is related to the critical heat flux (CHF) margins. As mentioned in Section 2.2, it's not feasible to estimate CHF values accurately on a natural circulation 2-phase flow system but some useful related estimates can be used as reference. In the case of natural circulation, the possibility of fuel pins reaching CHF levels is mainly associated to the onset of flow instability (OFI), which can disrupt the normal coolant flow and lead to poor heat removal from the core. OFI is in turn associated with conditions in which the amount of steam generated in the core is substantial and can affect the normal coolant flow, in contrast to the steam bubbles just being transported with the bulk of the liquid water. The onset of significant void (OSV) is the point beyond which steam bubbles get detached from the heated surface and merge with the liquid flow without collapsing, allowing the steam fraction to progressively evolve and grow to significant levels. Even though OSV is not easy to estimate, experiments carried out in the low flow and low pressure conditions characteristic of the SLOWPOKE reactors had been performed and reported [23], resulting in void fraction values between 4% and 10%, being on the higher range for low pressures and the lower one for high pressures. At pressures below 1.5 bar that can be found in the JM-1, the void fraction values would be expected to be on the higher end of that range. Void fractions to reach OFI are expected to be significantly higher than those for OSV. Since the void reactivity coefficient (considering a homogenous void distribution) of the JM-1 LEU core has been estimated as $-2.65 \text{ mk/\% void}$ (Section 3.1.6), the maximum credible excess reactivity insertion ($4.83 \pm 0.12 \text{ mk}$) could be fully compensated by less than 1.9% void fraction. That is far below the void fractions for OSV and OFI, especially if we take into account that the steam starts forming around the fuel pins with the highest power, neutron flux and reactivity values, and in the immediate vicinity of the cladding, where the negative reactivity insertion due to reduced moderation is greatest. The additional heating of the coolant, without which the OSV cannot be achieved due to bubble collapse into the colder liquid, adds additional negative reactivity, limiting the power excursion and the heating effects that could potentially lead to heat transfer problems. This behavior has been observed experimentally, resulting in power peaks of just a few seconds following large reactivity insertions (beyond $\sim 4.7 \text{ mk}$ in HEU cores) [18]. It hasn't been observed yet in LEU cores, since the maximum reactivity insertion tested so far has been 4.3 mk [17]. A more detailed analysis of these experiments is covered in Section 3.3.3.

Additionally, the LEU fuel, unlike the HEU one, has a non-negligible negative temperature reactivity coefficient as seen in Section 3.1.7. According to simulations with PLTEMP, the average fuel temperature on steady state at 90 kW and inlet temperature of 20°C should be around 184°C , resulting in a reactivity effect of about -1.31 mk . Given the low thermal conductivity of UO_2 , in the event of a sudden power increase after a large reactivity insertion, the fuel temperature is expected to increase significantly beyond that level. At a temperature of 250°C the reactivity compensated by the fuel coefficient would be of about -1.84 mk , reaching -2.24 mk at 300°C . If the temperature limit set by NRC in nuclear power plants to prevent rapid oxidation of the Zircaloy cladding (1204°C) is selected as the temperature reference for the fuel (UO_2 fuel temperature should be much higher than that for the cladding surface to reach that temperature), the corresponding estimated reactivity effect would reach -9.4 mk , almost twice the maximum credible excess reactivity achievable.

Therefore, it is expected that a significant fraction of the maximum credible reactivity insertion would be compensated by the fuel temperature rise before the power increase has even started to affect the coolant in any significant way. Consequently, it is expected that the phenomena observed in the early stages of large reactivity insertions in HEU cores would be attenuated in the LEU ones, resulting in prompt peaks of lower power, counteracting the effect of the higher power fraction per pin. It is estimated that the LEU prompt power peak for a reactivity insertion of about 5 mk would reach a similar height than that of HEU cores with a reactivity insertion of roughly 4 mk, that is about 40 kW.

It might be useful to obtain rough estimates of the CHF and CHF ratio (CHFR) for both reactor versions (HEU and 196-pin LEU) assuming the void fraction in the core is small enough not to significantly affect the flow conditions. If that is attempted using the previously developed PLTEMP/ANL models at the minimum power to possibly reach ONB in the LEU core (31.25 kW), the results obtained taking all HCFs into account are as follows. The CHF for the hottest pin of the HEU core at 31.25 kW would be 0.379 MW/m², for a CHFR of 11.39. In the case of the LEU core the CHF would be 0.506 MW/m², for a CHFR of 11.09. The difference in the CHFR is smaller than it could have been expected based only on the differences in power per pin or maximum heat flux in each of the cores. The main reasons for such similar results despite these discrepancies is likely the much larger flow channel area corresponding to the hottest pins in the LEU core and a somewhat higher flow velocity, which significantly increases the CHF value. Even though ONB could be reached at lower power levels (31.25 kW vs. 36.45 kW) due to the significantly higher heat flux, the average coolant temperature of the flow channel will be lower despite the higher power per pin. As a result, nucleate boiling may become a more efficient way to transfer heat from the cladding to the slightly colder coolant, having to reach similar reactor power where the steam bubbles do not easily collapse into the coolant flow under subcooled boiling conditions, hindering heat removal. This shows that even at 31.25 kW the heat flux at the fuel pins surface would be very far from its maximum possible level as long as the assumption of insignificant void fraction is valid.

3.3.3 Experimental transients reactivity analysis

To properly address this part of the analysis, it is appropriate to study first the existing experimental transient data available and to understand the observed behavior and what drives it.

Figure 27 shows experimentally measured data of a typical HEU-fueled SLOWPOKE-2 power transient after a large reactivity insertion. The reactivity insertion in this case is 5.88 mk, about 1 mk higher than the maximum credible reactivity for the JM-1 LEU core, so it can be used conservatively as a limiting case.

It is important to understand the power evolution throughout the transient and the underlying physical phenomena that determines its shape in order to proceed with the analysis. Detailed explanation of the reactor transient behavior is provided in section 10.1.5.3 of the safety analysis report of the EPM SLOWPOKE-2 reactor [17]. The main highlight is the presence of three different power peaks, which become apparent if the reactivity insertion is large enough.

Figure 27: HEU SLOWPOKE-2 power and reactivity evolution after a 5.88 mk reactivity insertion

The peak that is always present regardless of the magnitude of the reactivity insertion is also the one to show up the latest in time, hence called the ‘delayed peak’. For small reactivity insertions, however, the slope on the right-hand side of the peak may be virtually zero, basically becoming a step. The peak occurs when all the reactivity inserted has been compensated by the coolant heating, which eventually recirculates and starts increasing the inlet temperature, progressively driving down the reactor power. At power levels above 100 kW, such as in the case shown in Figure 27, boiling is also detected, in which case void contributes to excess reactivity compensation as well. In this particular power transient the peak happens about 115 seconds after the reactivity insertion. Given the time scale of the plot, the slow power decline after the peak cannot be observed.

For reactivity insertions of more than about 4 mk a second earlier and shorter peak starts becoming noticeable roughly 20 seconds after the reactivity insertion. In HEU cores the height of this ‘prompt peak’ may surpass the one of the delayed peak for reactivity insertions beyond 6 mk (shown later in Figure 29). The peak happens as a result of rapid heating of the fuel and core coolant before natural circulation flow has had time to develop. This rapid moderator heating introduces a substantial amount of negative reactivity; partially due to voids if power reaches high enough levels. These effects do not need to compensate the total magnitude of the initial reactivity insertion to stop the power increase and form the prompt peak. Reactor power will decrease if a sufficiently high rate of negative reactivity insertion is maintained long enough, as illustrated by the steep reactivity slope between roughly 10 and 30 seconds after

control rod withdrawal in Figure 27. Once convective flow develops (bulk circulation of water is observed to begin around second 12) cold water starts flowing into the core, while in parallel reactor power is somewhat reduced in this case, leading the average core coolant temperature to decrease slightly, marginally increasing total excess reactivity and driving the formation of the slower-rising delayed peak. This temperature drop is hard to determine given the lack of direct core measurements, so it is mostly inferred based on the small reactivity peak observed. However, the homogenization of temperature conditions inside the core may also have a small contribution to the reactivity increase due to its accompanying reduction of reactivity coefficients magnitude.

A third and earliest ‘fast peak’, which happens just about 15 seconds after the reactivity insertion, is also present in Figure 27, though only barely noticeable. This fast peak only occurs when the magnitude of the reactivity insertion is very large, rapidly driving reactor power to high levels. The reactivity feedback observed from the beginning of this initial fast power increase is attributed to the heating of the water layer around the pins, happening before coolant bulk temperature could have increased enough by the energy released to compensate that amount of reactivity or coolant subcooled boiling could have occurred. Taking into account that coolant temperature reactivity coefficient is not linear (Figure 11), a large temperature increase of a small fraction of the coolant, particularly around the fuel pins, would have a greater reactivity effect than the same amount of energy being evenly distributed throughout the coolant, resulting in a homogeneous temperature. After 15 seconds only about 445 kJ of energy have been released, which distributed uniformly would result in a core coolant temperature increase of roughly 13°C, for a reactivity effect of approximately –1.5 mk. Instead, if only a quarter of the core coolant volume was heated, increasing its temperature by 52°C, the estimated reactivity effect would be of about –2.3 mk, which matches quite closely the observed reactivity response at the time. Fuel temperature coefficient may marginally contribute as well in the HEU core with about –0.1 mk. Eventually, if the fast peak is high enough, the coolant layer in contact with the pin may start boiling, introducing more negative reactivity and limiting the fast peak height. The latter seems to be lower than that of the prompt peak along all the range up to the highest reactivity insertion magnitudes that have been tested (6.48 mk in a SLOWPOKE-1).

Figure 28 displays additional experimental core coolant inlet and outlet temperature data from the same Figure 27 transient. The arithmetic mean of the two temperatures has been calculated and plotted in the same figure as a crude approximation of core average temperature. The power and reactivity curves have also been superimposed (without units or scale) as timing reference for the temperature evolution. An attempt to infer the actual core coolant average temperature based on the reactor power, energy released, coolant flow, excess reactivity and reactivity coefficients is included as well. This estimate is to be used only as general reference of the expected behavior and not as an accurate prediction, since some factors, such as void fraction and flow velocity evolution, add significant uncertainty to the precise shape of the plotted line. Its main purpose is to try to capture the effect introduced by the delayed development of convective flow, which has a substantial impact on the outlet temperature readings.

Figure 28: HEU SLOWPOKE-2 core coolant temperature evolution after a 5.88 mk reactivity insertion

Figure 29 summarizes some valuable experimental data about maximum power peak heights on SLOWPOKE-2 transients after sudden reactivity insertions [7]. Both delayed and prompt peaks are plotted for HEU core experiments, while only delayed peaks are shown for LEU core, since no prompt peaks have been observed up to the reactivity magnitudes studied. The highest prompt peak data point corresponds to a SLOWPOKE-1 experiment.

Nevertheless, given the nature of the phenomena involved in the prompt peak, no significant differences are expected between SLOWPOKE-1 and SLOWPOKE-2 reactors prompt peak heights. This data point actually seems to fit with the SLOWPOKE-2 prompt peaks trend, though the trendline does not seem to follow a power equation ($y = ax^b$) as assumed in the UWI safety report [7], fitting much better to a polynomial one.

The combination of all this experimental data with previously estimated reactivity coefficients allows us to infer additional information about the transients. With the objective to determine an upper threshold for the LEU core void fraction during such transients.

The precision of the analysis based on these plots (Figures 27 and 28) is somewhat limited mainly due to a few factors, namely, the lack of direct fuel temperature measurements, the unknown coolant temperature/density distribution throughout the core, and the lack of coolant

Figure 29: SLOWPOKE-2 transient power peaks measurements after sudden reactivity insertions

flow rate information. The latter affects the delay between the time certain temperature conditions are reached within the core and the time the heated water volume exits the core and its temperature is measured at the core outlet. These limitations, however, do not prevent establishing a reliable void fraction upper boundary. A lower boundary for fuel temperature can be roughly estimated based on steady state conditions at a given power using PLTEMP/ANL, taking into account that during fast power transients the low thermal conductivity of UO_2 would contribute to higher temperatures being achieved inside the fuel. The somewhat reduced cooling at the beginning of the transient before natural circulation has developed also contributes to higher temperatures. In addition, the expected higher coolant temperature (and the possible bubble formation) right next to the pins surface contributes to magnify the feedback of the moderator and void reactivity coefficients beyond their normal values when their effects are considered to be uniform throughout the core. Finally, if the delay is assumed to be nonexistent, the real core temperature would be higher than the one considered at any given time during the beginning of the transient. Therefore, the effect of the moderator temperature coefficient would be underestimated, overestimating the share of inserted reactivity that becomes compensated by the void fraction. This conservative approach is preferred to using the estimated average core temperature evolution of Figure 28, avoiding any possibility that uncertainties could underestimate the actual void fraction. All that in addition to using as reference a 5.88 mk reactivity insertion experiment instead of one with the JM-1 LEU reactor maximum credible insertion of 4.83 ± 0.12 mk.

Two instants along the transient are considered to be of special interest as potentially most restrictive conditions in terms of the highest void fraction. These are the fast peak and the delayed peak, which in this case are assumed to happen at 19 and 115 seconds approximately after the reactivity insertion. The reactivity balance in these two instants is analyzed for both HEU and LEU cores.

HEU core power transient reactivity balance

The different reactivity coefficients contribution to total reactivity evolution illustrated in Figure 27 is examined for the HEU case to obtain some reference values to compare the LEU analysis presented later. The procedure uses the difference between total reactivity insertion magnitude (5.88 mk) and the remaining excess reactivity at the time of each of the peaks to know the amount of reactivity that is compensated by the reactivity coefficients. Using the arithmetic mean of inlet and outlet temperature as a conservative estimate of the core coolant temperature, the negative reactivity introduced by the coolant heating is calculated based on the previously determined reactivity coefficient (Section 2.1.9), considering conservatively that temperature is homogeneously distributed. The remaining reactivity compensation is assigned to the void coefficient (neglecting the contribution of HEU fuel temperature coefficient), according to the value obtained in Section 2.1.6, assuming again a conservative uniform distribution of voids. Results are presented in Table 7.

Table 7: HEU power transient peaks key parameters (5.88 mk insertion)

	Fast peak	Delayed peak
Remaining excess reactivity	2.7 mk	0.2 mk
Coolant temperature	27.2°C	53.8°C
Coolant reactivity	−0.74 mk	−4.95 mk
Voids reactivity	−2.44 mk	−0.73 mk
Void coefficient	−3.132 mk/%	−3.132 mk/%
Void upper threshold	0.78%	0.23%

The maximum conceivable value for the void fraction is obtained for the fast peak at 0.78%. If the inferred average core temperature was used instead of the arithmetic mean between inlet and outlet, void fraction threshold would be reduced to 0.1%. With regard to the delayed peak, given the widely conservative assumptions, the void fraction value is significantly higher than the 0.157% for a 6 mk insertion transient calculated using SLOWKIN [24].

LEU core power transient reactivity balance

The same procedure is followed in the case of the LEU core, assuming a similar transient behavior to the HEU core, which has been experimentally verified up reactivity insertions of 4.3 mk. In this case, however, the contribution of the fuel temperature coefficient is significant, being substantially larger than in the HEU core. The fuel temperature is conservatively estimated in this analysis using the previously developed PLTEMP/ANL model as the steady state fuel temperature corresponding to a constant power level equal to that of the power peak. Results are presented in Table 8.

Table 8: LEU power transient peaks key parameters (5.88 mk insertion)

	Fast peak	Delayed peak
Remaining excess reactivity	2.7 mk	0.2 mk
Reactor power	107 kW	128 kW
Coolant temperature	27.2°C	53.8°C
Fuel temperature	205°C (185°C increase)	228.5°C (208.5°C increase)
Coolant reactivity	−0.28 mk	−2.51 mk
Fuel temperature coefficient	−0.008 mk/°C	−0.008 mk/°C
Fuel reactivity	−1.48 mk	−1.67 mk
Voids reactivity	−1.42 mk	−1.50 mk
Void coefficient	−2.62 mk/%	−2.62 mk/%
Void upper threshold	0.54%	0.57%

The maximum void fraction for the LEU core is obtained at the delayed peak, with a value of 0.57%. This figure compares reasonably well with the 0.44% result obtained using SLOWKIN for a 6 mk insertion [24], being more conservative as expected. Though the obtained void upper threshold value is higher than the one obtained for the HEU core, it is still very far from those corresponding to OSV or OFI. If the inferred core average temperature (Figure 28) was used for the prompt peak calculation, the void fraction would also be reduced to just 0.11%.

It is also worth noting that, according to Figure 29, the LEU delayed power peak would be expected to be somewhat lower than in the HEU cores beyond the 4.3 mk reactivity insertion threshold for which experimental measurements are available. On the other hand, prompt power peaks in the LEU core are also expected to be of significantly lower magnitude for the LEU core, as mentioned earlier (Section 3.3.2), given the substantial contribution of the fuel temperature coefficient of reactivity. The almost instantaneous fuel temperature increase during a power excursion would reduce from the start the reactivity available for further power increase, as well as the rate at which it could occur, leading to an even more favorable transient behavior.

In any case, due to the large void coefficient, even larger amounts of reactivity, or any possible uncertainties about the contribution of other coefficients, could be easily accommodated by small and safe increments of the void fraction.

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Appendix A: Sample PLTEMP/ANL input data calculations for the JM-1 HEU core

1. Cladding thickness, fuel meat thickness, and fuel tube mean radius

Each solid fuel pin of JM-1 is modeled as a fuel assembly, using the radial geometry and the 5-layer (cladding/gap/fuel meat/gap/cladding) fuel plate options of the PLTEMP/ANL code. The fuel meat radius is 2.108 mm with a cladding thickness of 0.508 mm and no gap, as specified in the design data. The code requires a coolant channel on *both sides* of each fuel plate (tube or pin in radial geometry) in an assembly, the number of coolant channels being always one more than the number of fuel tubes. Therefore, the fuel assembly model has one fuel tube and two channels. Since these are solid pins without any coolant channel in their center and zero values are not accepted by the code as flow areas, a value of 10^{-50} m^2 is chosen for the inner channel, while actual dimensions are used for the outer ones. The fuel meat thickness (input TAEM0 on Card 0306B) and fuel tube mean radius (input RMID on Card 0308A) in the model are found as:

$$\text{Fuel meat thickness} = \text{Fuel meat radius} = 2.108 \text{ mm}$$

$$\text{Fuel tube mean radius} = (\text{Fuel meat radius})/2 = 2.108/2 = 1.054 \text{ mm}$$

2. Total core flow area

$$\begin{aligned} &= (\text{Flow area inside the annular beryllium}) - \\ &\quad - (\text{Cross-sectional area of all fuel pins}) - \\ &\quad - (\text{Cross-sectional area of tie rods}) - \\ &\quad - (\text{Cross-sectional area of the control rod guide tube}) \\ &= \pi \cdot (\text{Inner radius of annular beryllium})^2 - \\ &\quad - \pi \cdot (\text{Fuel pin radius})^2 \cdot (\text{Number of fuel pins}) - \\ &\quad - \pi \cdot (\text{Tie rods radius})^2 \cdot (\text{Number of tie rods}) - \\ &\quad - \pi \cdot (\text{Outer radius of control rod guide tube})^2 + \\ &\quad + \pi \cdot (\text{Inner radius of control rod guide tube})^2 \\ &= \pi \cdot (110.49)^2 - 296 \pi \cdot (2.616)^2 - 3 \pi \cdot (3.96875)^2 - \pi \cdot (12.7)^2 + \pi \cdot (11.684)^2 \\ &= 30904.85 \text{ mm}^2 \end{aligned}$$

$$3. \text{ Flow area per active fuel pin} = \frac{\text{Total core flow area}}{\text{Number of pins}} = 30904.85 / 296 = 104.408 \text{ mm}^2$$

This is the flow area of the outer coolant channel, i.e., channel 2 in the PLTEMP input data. The inner channel (channel 1) flow area is zero. However, given that the code doesn't accept zero values for this parameter an arbitrarily small figure (10^{-50} m^2) is used. The input values of its hydraulic diameter, wetted and heated perimeters, channel width and channel thickness, are all irrelevant.

4. Outer radius of water, R_e , in an equivalent unit cell containing a single fuel pin is given by the following equation.

$$\pi R_e^2 = (\text{Fuel pin cross-sectional area}) + (\text{Flow area per active fuel pin})$$

$$= \pi \cdot (2.616)^2 + 104.408$$

$$R_e = 6.3307 \text{ mm}$$

5. Wetted perimeter per fuel pin

$$= [(\text{Perimeter of fuel pin}) \cdot 296 + (\text{Perimeter of tie rods}) \cdot 3 +$$

$$+ (\text{Inner Perimeter Beryllium Annulus}) +$$

$$+ (\text{Inner Perimeter of Control Rod Guide Tube}) +$$

$$+ (\text{Outer Perimeter of Control Rod Guide Tube})] / 296$$

$$= [296 \cdot (2\pi \cdot 2.616) + 3 \cdot (2\pi \cdot 3.96875) + 2\pi \cdot 110.49 + 2\pi \cdot 11.684 + 2\pi \cdot 12.7] / 296$$

$$= 19.5525 \text{ mm}$$

6. Heated perimeter per fuel pin

$$= \text{Perimeter of fuel pin}$$

$$= 2\pi (2.616) = 16.4368 \text{ mm}$$

7. Hydraulic diameter for flow around fuel pins

$$= 4 \cdot \text{Total Core Flow Area} / \text{Total Wetted Perimeter}$$

$$= 4 \cdot 30904.85 / (19.5525 \cdot 296) = 21.360 \text{ mm}$$

8. Channel thickness and width

$$\text{Thickness of channel} = (\text{Outer radius of water annulus} - \text{Inner radius of water annulus})$$

$$= 6.3307 - 2.616 = 3.7147 \text{ mm}$$

$$\text{Width of channel} = 2\pi (\text{Mid-radius of the annular flow area in the equivalent unit cell})$$

$$= \pi (2.616 + 6.3307) = 28.1069 \text{ mm}$$

9. Input chimney height

Although there is no actual chimney in the JM-1 design, there is a small gap between the upper end of fuel pins and the shim tray through which the hot coolant coming out of all channels mixes and flows out of the core. This section acts in the same way a chimney would in promoting natural circulation coolant flow. The height of this gap between the top of the fuel pins and the bottom of aluminum shim tray is 10.53 mm.

10. Input power

The power input to the PLTEMP/ANL model equals the average power per fuel pin times the number of fuel pins modeled by the input data file.

$$\text{Average power per fuel pin} = \frac{\text{Reactor operating power}}{\text{Number of fuel pins}} = \frac{19.08 \text{ kW}}{296 \text{ pins}} = 64.46 \text{ W/pin}$$

11. Input guess for flow rate in a fuel assembly

In the PLTEMP/ANL modeling, each fuel pin is an assembly, and therefore the input guess for flow rate *in an assembly* (WFGES on Card 0300) implies the flow rate per fuel pin. An order-of-magnitude estimate can be obtained based on an assumed coolant temperature rise of a few degrees Centigrade in a channel, e.g., 5°C. The code would converge even if the guess for flow rate is 10 times too low or too high (provided that all the other input data are acceptable). Here, the specific heat of water is taken to be 4.186 kJ/kg·°C.

$$\begin{aligned} \text{Estimate for flow rate per fuel pin} &= \frac{\text{Power per fuel pin}}{(\text{Coolant specific heat}) \cdot (\text{Coolant temperature rise})} = \\ &= \frac{0.06446 \text{ kW}}{4.186 \text{ kJ}/(\text{kg} \cdot ^\circ\text{C}) \cdot 5.0^\circ\text{C}} = 0.0031 \text{ kg/s} \end{aligned}$$

12. Pressure at core inlet

The depth of water above core inlet is 4.64 m. The pool water density is 998.23 kg/m³ at the default pool temperature of 20°C. Even though the water temperature may be higher in some of the scenarios considered, the mass of water in the reactor container will remain constant, so the pressure at the core inlet, relatively close to the bottom of the container, will remain approximately constant. Using a value for gravity of 9.80665 m/s², the pressure at the core top is obtained as follows.

$$\begin{aligned} \text{Pressure at core top} &= 1 \text{ atm} + \text{water pressure} \\ &= 1.01325 \text{ bar} + 998.23 \cdot 9.80665 \cdot 4.64 \text{ N/m}^2 \\ &= 1.01325 \text{ bar} + 0.4542 \text{ bar} = 1.4675 \text{ bar} \end{aligned}$$

Appendix B: Main parameters of the MCNP models

This appendix compiles the most relevant reactor parameters that have been used to create the MCNP5 models of the SLOWPOKE-2 reactor. Its purpose is both to serve as reference for potentially useful data related to the analysis, and to allow possible future validation or comparative analysis of the results presented in this report. As seen in the tables below, three models are presented separately: the original HEU core model (based on available information on the JM-1 HEU reactor after commissioning), the reference LEU model (based on engineering drawings, experience on existing LEU SLOWPOKES and early manufacturing information), which has been used for most of the LEU core analysis in this report, and the predicted JM-1 LEU (based on the actual LEU fuel manufacturing tests data).

Even though the information presented here should be basically enough to build similar models and get reasonably accurate comparisons, it is not practical to include all the details of the models (thousands of lines long) in this appendix. For more complete information the models themselves could be examined.

Core parameters			
	JM-1 HEU	Reference LEU	JM-1 LEU
Fuel material	U/Al	UO ₂	UO ₂
Enrichment (%)	93.18	19.88	19.88
Number of fuel pins	296	198	196 (predicted)
Pellet/meat diameter (mm)	4.216	4.174	4.1738
Pellet/meat density (g/cm ³)	3.45	10.61	10.586
Effective stack density (g/cm ³)	3.45	10.61	10.553
Sheath material	Al	Zircaloy-4	Zircaloy-4
Stack/meat height (mm)	220	226.97	226.9807
Sheath outer diameter (mm)	5.232	5.32	5.32
Sheath inner diameter (mm)	4.216	4.304	4.24684
Sheath thickness (mm)	0.508	0.508	0.53658
Pellet to sheath diametral clearance/gap (mm)	-	0.13	0.0732
Fuel pin length shoulder to shoulder (mm)	225.023	233.74	233.66
Fuel pin total length (mm)	228.346	236.53	236.45
Fuel cage lattice size (mm)	11.04		
Volume of water in the core (L)	7.61	8.01	8.00
Total mass of UO ₂ (g)	-	6524.448	6423.535
Total mass of U/Al (g)	3136.35	-	-
Total mass of U (g)	878.1781	5749.469	5660.542
Total mass of ²³⁵ U (g)	818.2864	1141.844	1125.316

Miscellaneous reactor configuration parameters			
	JM-1 HEU	Reference LEU	JM-1 LEU
Total inner irradiation sites	5		
Available inner irradiation sites	4		
Original Cd self-powered neutron detector	Inoperative and stuck in place in the Be reflector		
Self-powered Cd detector replacement	Located in inner irradiation site #2		
Depletion of the Cd emitter in the detectors (%)	0	8.6	
Outer irradiation sites	1 (unused)		
Shim tray thickness. Right half (mm)	17.04	0	
Shim tray thickness Left half (mm)	15.21	0	
Beryllium reflector parameters			
Annular reflector height (mm)	228		
Annular reflector outer diameter (mm)	424.688		
Annular reflector inner diameter (mm)	220.98		
Bottom reflector height (mm)	102		
Bottom reflector diameter (mm)	322		
Beryllium reflector density (g/cm ³)	1.853		
Control rod parameters			
Fully inserted. Rod distance from bottom (mm)	25.4		
Movement range (mm)	203.2		
Distance from rod tip to cadmium foil (mm)	54.013		
Cadmium foil thickness (mm)	0.508		
Cadmium foil length/height (mm)	253.20625		
Cadmium foil width (mm)	9.525		
Cd foil enclosing tube inner diameter (mm)	2.9845		
Fuel composition			
U-235 (%)	26.05601	17.41913	17.3818
U-238 (%)	1.504414	69.91908	69.7693
U-234 (%)	0.279631	0.134937	0.134648
U-236 (%)	0.123038	0.14808	0.147763

Fuel composition (continued)	JM-1 HEU	Reference LEU	JM-1 LEU
O (%)	0.097611	12.33526	12.5255
Al (%)	71.90509	0.001402	0.001171
B-10 (ppm)	0.438481	0.306885	0.255857
B-11 (ppm)	1.946331	1.235249	1.02986
C (ppm)	195.2423	48.06651	71.2143
N (ppm)	124.9351	0	0
H (ppm)	19.4743	0	0
Ca (ppm)	0	40.05542	31.2143
Cd-106 (ppm)	0	0.002516	0.002513
Cd-108 (ppm)	0	0.001791	0.001789
Cd-110 (ppm)	0	0.02514	0.025105
Cd-111 (ppm)	0	0.025764	0.025728
Cd-112 (ppm)	0	0.048568	0.048501
Cd-113 (ppm)	0	0.024596	0.024562
Cd-114 (ppm)	0	0.057827	0.057747
Cd-116 (ppm)	0	0.015076	0.015055
Cr (ppm)	0	35.44905	31.0714
Cu (ppm)	0	3.604988	5.72143
Dy-156 (ppm)	0	0.000113	0.000113
Dy-158 (ppm)	0	0.000191	0.000191
Dy-160 (ppm)	0	0.004687	0.004681
Dy-161 (ppm)	0	0.03802	0.037967
Dy-162 (ppm)	0	0.051276	0.051205
Dy-163 (ppm)	0	0.05011	0.050041
Dy-164 (ppm)	0	0.056882	0.056803
F (ppm)	0	6.709283	6.7
Fe (ppm)	0	211.2923	179.571
Gd-152 (ppm)	0	0.000403	3.23E-05
Gd-154 (ppm)	0	0.004388	0.000352
Gd-155 (ppm)	0	0.029789	0.002393
Gd-156 (ppm)	0	0.041202	0.003309
Gd-157 (ppm)	0	0.031501	0.00253
Gd-158 (ppm)	0	0.049997	0.004016
Gd-160 (ppm)	0	0.044	0.003534

Fuel composition (continued)	JM-1 HEU	Reference LEU	JM-1 LEU
Mg (ppm)	0	3.104295	2.6
Mn (ppm)	0	4.806651	3.94286
Mo (ppm)	0	22.03048	22.6429
Ni (ppm)	0	21.0291	19
Si (ppm)	0	18.02494	17.875
Th (ppm)	0	4.806651	4.76429

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