

The Design of High Power Density Annular Fuel for LWRs

by

Yi Yuan

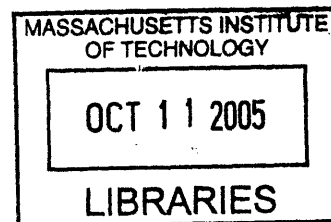
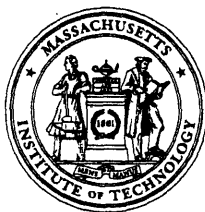
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ABSTRACT

Fuel performance models have been developed to assess the performance of internally and externally cooled LWR annular fuel. Such fuel may be operated at 30-50% higher core power density than the current operating LWRs, and to a burnup of 80-100MWd/kgU. The models are used to optimize the fuel design so that it is able to achieve high power density and high burnup, and to identify the features of this fuel that will impact its operation limits.

The annular fuel performance codes have been developed based on the NRC licensed FRAPCON-3 code with major modifications to the code structure and with implementation of new fuel performance models. A heat split calculation was enabled by adding a heat flux iteration loop. The radial power peaking and the rim effects at both the inner and outer fuel surfaces have been modeled by a modified radial power/burnup fit to the neutronic calculations. The temperature profile calculation method was updated with new boundary conditions and meshing scheme to capture the internal cooling and the double power peaking at the rims. The annular fuel performance codes are able to simulate both sintered annular fuel and Vibration-Packing (VIPAC) fuel with internal and external cooling.

For the sintered annular fuel, the anchor ring location of fuel thermal expansion is determined to be the innermost ring, and the fuel dimensions are calculated considering the effects of thermal expansion, swelling and densification. Fuel relocation is assessed via a new empirical model that has been implemented in the code. A fuel cladding mechanical interaction model has been developed with three regimes: the free standing cladding regime, the single closure regime and the fuel cladding full contact regime. The interaction mechanisms for each regime are analyzed and solutions are provided. A low temperature fission gas release model is implemented for sintered annular fuel by taking into account the double surface effects.

It is found that the sintered annular fuel rod has lower fission gas release than that of a solid PWR rod at the same power density. The cladding hydrogen concentration and the oxide accumulation of the annular fuel are comparable to those of the solid fuel due to comparable cladding heat flux and irradiation. Fuel gap conductance asymmetry caused by outward thermal expansion has been identified as a major concern due to its potential effects on MDNBR. A sensitivity study has been performed to evaluate the impact of fuel parameters on fuel performance. The gap asymmetry problem can be circumvented by combining several approaches including: (1) allowing a larger outer gap and a smaller inner gap, (2) enlarging the

fuel and cladding surface roughness, and (3) spattering the fuel surface with ZrO_2 particles. The optimized sintered annular fuel shows great potential for achieving high burnup (up to 86MWd/kgU rod average) and operating at 150% power density without compromising fuel safety.

A VIPAC annular fuel performance model has also been developed and implemented. An empirical VIPAC fuel thermal conductivity formula has been developed as a function of temperature, burnup, porosity and gas pressure. A fuel-clad interface thermal conductance model has been developed incorporating the effects of fuel particle size, gas pressure and interfacial pressure. The VIPAC fuel bulk dimensional changes have been calculated assuming the same bulk thermal expansion and densification coefficient as the sintered fuel, but the fission product swelling is assumed to be accommodated by porosity. The VIPAC fuel-cladding mechanical interaction model applies the full gap closure regime of the pelletized annular fuel model except that fuel cladding “slippage” is assumed. An athermal fission gas release model for VIPAC is developed incorporating the surface and resolution effects of the fuel particles.

Compared with the sintered fuel, the gap conductance imbalance problem is completely eliminated for VIPAC fuel but the fission gas release fuel is higher. The VIPAC annular fuel EOL cladding oxide thicknesses and hydrogen concentrations are comparable with those of the sintered annular fuel but the VIPAC annular rod cladding strains are significantly reduced. A sensitivity study of the important parameters of the VIPAC fuel identified that the optimum initial helium gas pressure is within 1.4-2.0 MPa and the optimum particle size is within 300-600 μm . The optimum smear density is in the range of 85%-90%.

A preliminary study of Reactivity Initiated Accidents behavior involving the annular fuel has been made. During a RIA, the annular fuel peak enthalpy is found to be comparable to the solid fuel. However the permanent hoops strain of the sintered annular fuel is lower, and of the VIPAC annular fuel is higher, than that of the solid fuel.

Analyses of the annular fuel testing at the MIT reactor and planning of the annular fuel post irradiation examination are also presented.

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NOMENCLATURE

AECL	Atomic Energy of Canada Limited
BOL	Beginning of Life
BWR	Boiling Water Reactor
BNL	Brookhaven National Laboratory
DNB	Departure from Nulceate Boiling
DNBR	Departure from Nulceate Boiling Ratio
EBWR	Experimental Boiling Water Reactor
EFPD	Effective Full Power Days
FGR	Fission Gas Release
HM	Heavy Metal
I&E	Internal and External
LWR	Light Water Reactor
LBE	Lead-Bismuth Eutectic
LB-LOCA	Large-Break Loss Of Coolant Accident
LOCA	Loss Of Coolant Accident
MCNP	Monte Carlo N-Particle code
PCMI	Pellet-Cladding Mechanical Interaction
MOX	Mixed-Oxide
MITR	MIT Reactor
NRC	Nuclear Regulatory Commission

PIE	Post-Irradiation Examination
PNL	Pacific Northwest National Laboratory
ppm	parts per million (10^{-6})
PRTR	Plutonium Recycle Test Reactor
PWR	Pressurized Water Reactor
RIA	Reactivity Initiated Accidents
RIAR	Research Institute of Atomic Reactor
TD	Theoretical Density
VIPAC	Vibration-PACking

Chapter 1 Introduction

1.1 Background

Good performance of nuclear fuel under radiation and high temperature conditions is vital for safety and economic viability of any reactor. Significant effort has been invested into achieving excellent performance of PWR fuels in the last four decades. The cylindrical design of PWR fuel has remained unchanged since the time of its conception. Evolutionary improvements in fuel and cladding quality allowed remarkable reduction in fuel failure rate, and fuel assembly design changes have allowed appreciable power density increases and performance improvements during loss of coolant accidents. However, larger increments need to be realized to achieve better plant economy. Also, larger safety margins would be useful to relax the time available to respond to coolant transients. Therefore, it is desirable to develop new generations of PWR fuels that have higher power density, larger safety margin and lower manufacturing cost, that are readily capable of being installed in the current PWRs with minimum modifications to the plant design.

Based on the extensive experience of current UO_2 fuel, an internally and externally cooled annular fuel concept that introduces an additional cooling channel compared with conventional PWR fuel [Kazimi, et al, 2001] has been proposed. Annular fuel pellets with voided central regions have been used in VVER reactors and evaluated for other PWRs [Mildrum, 1980; Caner and Dugan, 2000]. Annular fuels with both internal and external cooling have been employed in research reactors and proposed for high temperature gas cooled reactors [Bujas, 1975]. However, commercial PWR reactors have not used such annular fuel rods. With such fuel, the larger cooling surface results in lower operating heat flux to the coolant, therefore a significantly higher margin to departure from nucleate boiling. With lower fuel temperatures, the annular fuel provides significant benefits in terms of low peak cladding temperature following a LOCA [Kim et al, 2002 a and b]. Therefore, the fuel performance at both steady state and accident scenarios is expected to be better than the current solid PWR fuel design. Hence, high fuel burnup can be pursued utilizing the benefit of very low fuel temperature. The

high burnup reduces the waste production and increases proliferation resistance of the spent fuel. [Hejzlar et al ,2001]

1.2 Objectives & Scope

The goal of this thesis is to identify the power density limits and fuel performance constraints of internally and externally (I&E) cooled annular fuel from a materials performance perspective. To accomplish the goal, capabilities for modeling I&E cooled annular fuel will be developed for both sintered and VIPAC annular fuel. Fuel models will be developed reflecting the new fuel geometry. The burnup and power distribution, fuel thermal and irradiation dimensional changes as well as fuel-cladding interaction mechanisms will be modeled. The fission gas release and high burnup effects will also be discussed and models will be developed to capture these effects. The fuel performance codes will be developed and the annular fuel performance will be analyzed through computer codes incorporating these models. The design of both the sintered and VIPAC annular fuel rods will be evaluated and optimized. A parametric study will be performed to identify the constraints and design limits as well as sensitivity of the fuel design to fabrication and operation uncertainties. A preliminary assessment of the annular fuel transient behavior will be performed. A conceptual design of the annular fuel testing at MITR will be evaluated. Finally, annular fuel performance of different manufacturing processes will be compared and a specific fuel design will be proposed for commercial applications of such fuel.

1.3 Sintered I&E Cooled Annular Fuel

Sintered annular pellets with central voids (10% by volume) have been manufactured for VVER reactors and evaluated for PWRs [Mildrum, 1980; Caner and Dugan, 2000] to lower the fuel maximum temperature below that of solid fuel. It was also found that annular fuel with 45% void could have 10% reactivity gain at the beginning of the cycle compared with solid fuel. [Mildrum, 1980] Annular fuel with natural uranium oxide in the central region was also experimented with [Jensen, 1978] and it was found that the radial expansion of the fuel was reduced, resulting in low mechanical interaction between the fuel and cladding. An annular fuel with internal and external cooling was proposed in the advanced plutonium fuel assembly

(APA) concept in France [Brochard et al, 2001]. The $\text{PuO}_2\text{-CeO}_2$ CERCER annular fuel was fabricated with concentric claddings to allow cooling at both the inner and outer sides. Analysis showed significant reduction of fuel temperature and small bulking risk. However, the asymmetry of fuel-clad gaps and fabrication of fuel pellets turned out to be restrictive [Puill et al, 2001]. The sintered UO_2 internally and externally cooled annular fuel was proposed at MIT to be able to fit in the current operating PWRs while achieving equivalent or higher core power density. Therefore, it is desirable to keep the assembly dimensions as well as the coolant to fuel ratio the same as those of today's PWRs. Based on the Westinghouse PWR fuel assembly dimensions, different fuel array sizes were explored with fixed assembly dimensions. A schematic of a sintered I&E cooled annular fuel cell (from a 13×13 assembly) is illustrated in Figure 1.1.

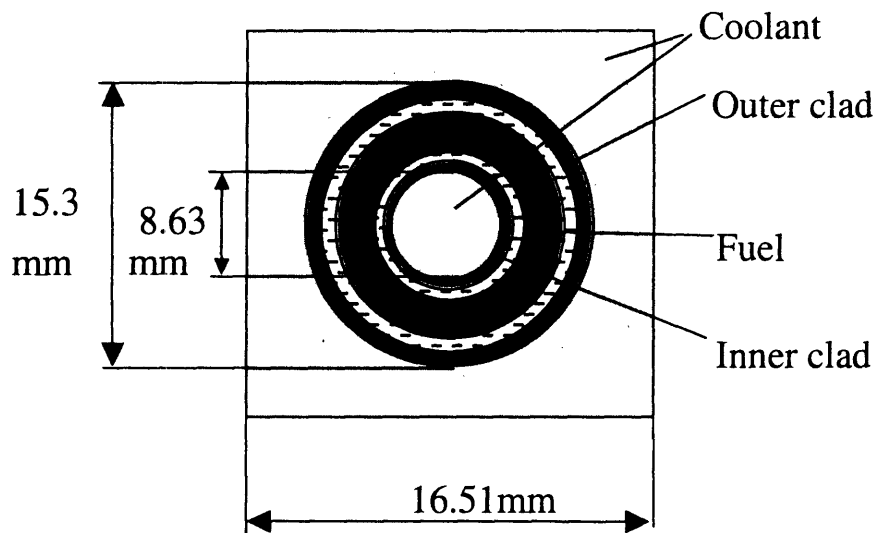


Figure 1.1 Schematic of an I&E Cooled Annular Fuel from a 13×13 Lattice

1.4 VIPAC I&E Cooled Annular Fuel

Vibration Packing (VIPAC) of fuel is an alternative fuel manufacturing technique. The VIPAC I&E cooled annular fuel has the same dimensions as the sintered annular fuel except that fuel-cladding gaps are eliminated. The VIPAC fuel was first developed at Pacific Northwest

Laboratory in 1959 [Freshley and Burley, 1969] and MOX fuel rods were manufactured for the Experimental Boiling Water Reactor (EBWR) and Plutonium Recycle Test Reactor (PRTR) [Sharp et. al, 1964]. The fuel was pre-pressed with high energy pneumatic impact and then vibratory compacted into the tube. The VIPAC MOX fuel has been irradiated in some US test reactors and commercial LWRs and achieved burnups up to 285,000MWd/t [Chidester, et al, 2001]. The US VIPAC fuel development halted due to the disadvantages of the lower smear densities. Russian experience with VIPAC fuel was later revealed [Mayorshin et al, 2000]. Extensive tests on VIPAC MOX fuel were performed back in the 1970's in BOR-60 and BN-600 fast reactors at the Research Institute of Atomic reactor (RIAR) in Russia. By utilizing the vibration packing manufacture technique, UO_2 particles can achieve a smear density of ~77%, With a small amount of metallic uranium powder, the smear uranium density can reach 85% of that of the UO_2 . [Chidester, et al, 2001, Mayorshin et al, 2000]

The VIPAC fabrication technique was able to achieve a smear density between 82% and 86%. If this is routinely achieved, it is expected to provide more economic production of the fuel due to the following advantages [Chidester, et al, 2001; Freshley and Burley, 1969]:

- The VIPAC technique can be easily applied to different geometries.
- Smaller number of discrete production and inspection operations is required.
- Fuel-cladding gaps no longer exist in VIPAC fuel and thermal resistance between the fuel and cladding is significantly decreased.
- Greater automation and control is possible.
- It facilitates large scale processing and requires smaller investment.
- The process generates less scrap than does processing of pellets.

The disadvantages of the VIPAC fuel have been found to be:

- Non-uniform distribution of particles may result in a hot spot or burnout of the fuel, therefore inspection of uniformity is required for VIPAC production process.
- The VIPAC thermal conductivity is expected to be lower than the pelletized fuel. Hence, higher fuel temperature can result. However, in-situ sintering of the fuel and redistribution of porosity can improve thermal conductivity.

- In order to achieve the high packing density of the fuel, high density feeding material is required.

It is difficult to achieve high smear density in the VIPAC fuel by simply vibrationally packing rough fuel powers. The sol-gel approach has been applied to produce uniform particles that can then be packed in the cladding. By the sol-gel process, a relatively high degree of uniformity and reproducibility can be achieved and particle size can be controlled to reach higher smear density. The radioactive dust problem would be greatly relieved. Therefore the production process is less hazardous. However, the use of a solution will add to the volume of liquid radioactive waste and the control of the solution is very important. Russians have developed two VIPAC processes: the gaseous-fluoride and electrochemical molten salt synthesis. The electrochemical process produced particle sizes up to 800 microns and showed sufficient homogeneity. [Herbig et al, 1993]

The irradiation test of VIPAC fuel has shown less fuel-cladding mechanical and chemical interaction compared with the sintered fuel, due to the ability of the fuel particles to accommodate the strains and to the improved conductance at fuel-clad interface. By using uranium metal getter, the Russians were able to achieve fairly high smear density (9.0-9.4 g/cc) in some of Russian VIPAC fuel rods. It was also found that the presence of uranium powder was instrumental to control the oxygen to metal ratio and to reduce the release of cesium. In the meantime, the fuel cladding chemical interaction was greatly reduced as well.[Mayorshin et al, 2000] However, the addition of uranium powder may enhance the fission gas release since the metallic fuel powder has higher fission gas release than that of ceramic fuel. In addition, the uranium powder melting during operation can be a concern due to its low melting point.

The disadvantages involve greater amount of fission gas release at high burnup due to the enlarged open surface and sintering of the particles at high fuel temperatures. [Fitts and Miller, 1974] Also, the lower smear density achievable for the VIPAC fuel results in shorter cycle length for fixed enrichment.

The Loss of Coolant Accident behavior of the VIPAC fuel was compared with that of pelletized fuel at PNL up to failure. It was found that the cladding failure threshold energies are

the same. In VIPAC fuel the cladding was penetrated by the ejection of molten UO_2 and in pellet fuel the cladding was damaged without molten UO_2 . The metal water reaction is comparable for both types of fuels. [Freshely and Harrison, 1972]

Twelve VIPAC fuel samples have been irradiated in Swiss Goesgen PWR to more than 50 MWd/kgU [Stratton, 2004]. The most recent comparative test revealed that: (1) the VIPAC fuel has much reduced creepdown behavior than pelletized fuel due to the support provided by the fuel; (2) that the VIPAC fuel has higher fission gas release than pelletized fuel; and (3) restructuring takes place in the fuel rims.

The VIPAC annular fuel combines the advantages of the VIPAC fuel concept and annular fuel concept. The fuel production of VIPAC annular fuel could be easier than the pelletized annular fuel. The VIPAC annular fuel eliminates the fuel-cladding gap and allows cooling from both the inner and outer channel; hence the fuel temperature can be greatly reduced although the VIPAC fuel has lower thermal conductivity than the palletized fuel.

A VIPAC I&E cooled annular fuel is illustrated in Figure 1.2

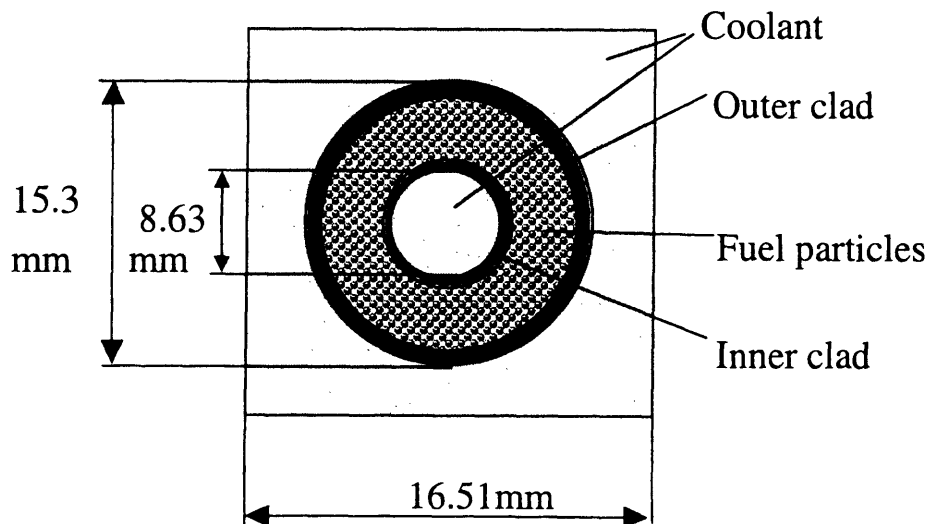


Figure 1.2 A Schematic of an I&E Cooled VIPAC Annular Fuel from a 13×13 Lattice

1.5 PWR Core with Annular Fuels

A PWR core with internally and externally cooled annular fuel has the potential for a large power density increase while at the same time maintaining adequate safety margin due to the larger fuel surface to volume ratio. The advantage of the annular fuel core design is that heat can be transferred to the coolant at both the inner and outer channels. However, it is critical to achieve a heat flow split between the inner and outer channels such that the coolant enthalpy rise and the minimum values of the Departure from Nucleate Boiling Ratio (DNBR) for both channels are approximately the same.

The internally and externally cooled annular fuel core design should be compared to a reference PWR design to weigh the relative performances. To this end, a typical Westinghouse 3411MWth four-loop PWR plant has been selected as the base case and the parameters for the base case are given in APPENDIX A.

The design objective of internally and externally cooled annular fuel has been set to achieve low peak fuel temperature, low core pressure drop and high MDNBR. Different array configurations have been evaluated while maintaining:

- the same assembly dimensions and minimum modification of the control assembly of the existing PWR core.
- the same fuel to moderator ratio (but around 90% of fuel volume) compared to the reference PWR design,
- the same power peaking as that for the reference Westinghouse PWR core (i.e. 2.5 total, and the axial power shape is assumed to be chopped cosine with a peaking of 1.55)
- the same mass flow rate per an average assembly.

The dimensions and characteristics of the sintered I&E cooled annular fuel for various array configurations are listed in Table 1.1 [Kazimi, 2002]

Table 1.1 Dimensions (cm) and Characteristics of Sintered Annular Fuel for Various Arrays

Array	Outer Diameter (Outer Clad)	Inner Diameter (Inner Clad)	Fuel Inner Diameter	Fuel Outer Diameter	Inner Diameter (Outer Clad)	Outer Diameter (Outer Clad)	Pitch
11x11	1.073	1.188	1.20	1.700	1.712	1.827	1.952
12x12	0.953	1.068	1.08	1.540	1.552	1.667	1.789
13x13	0.863	0.978	0.99	1.410	1.422	1.537	1.651
14x14	0.753	0.868	0.88	1.294	1.306	1.421	1.533
15x15	0.673	0.788	0.80	1.198	1.210	1.325	1.431
17x17-ref.	Solid pin	-	-	0.826	0.838	0.952	1.263
Cladding material	Zircaloy -4						
Filling gas	Helium						
System pressure	15.5MPa						
Core inlet temperature	292.7C						

Larger number of rods per assembly could increase the total cooling surface of the assembly, which would result in lower fuel temperatures. However, the pressure drop will also increase with the number of rods per assembly. The cost of fabrication will also increase with the number of rods of an assembly. Initial thermal hydraulic analysis identified the 13x13 array annular fuel design as a promising concept to upgrade the core power density and maintain large safety margin. A 13x13 internally and externally cooled annular fuel assembly is shown in Figure 1.3.

Comparison to 17x17 W fuel - ratios

$V_{\text{fuel}} - 0.90$

$V_{\text{coolant}} - 0.95$

$V_c/V_f - 1.06$

Scooling - 1.53

13x13 fuel assembly

Pitch=1.651cm

160 fuel rods, 9 guide tubes

Inner clad ID = 8.633mm

Outer clad OD = 15.37mm

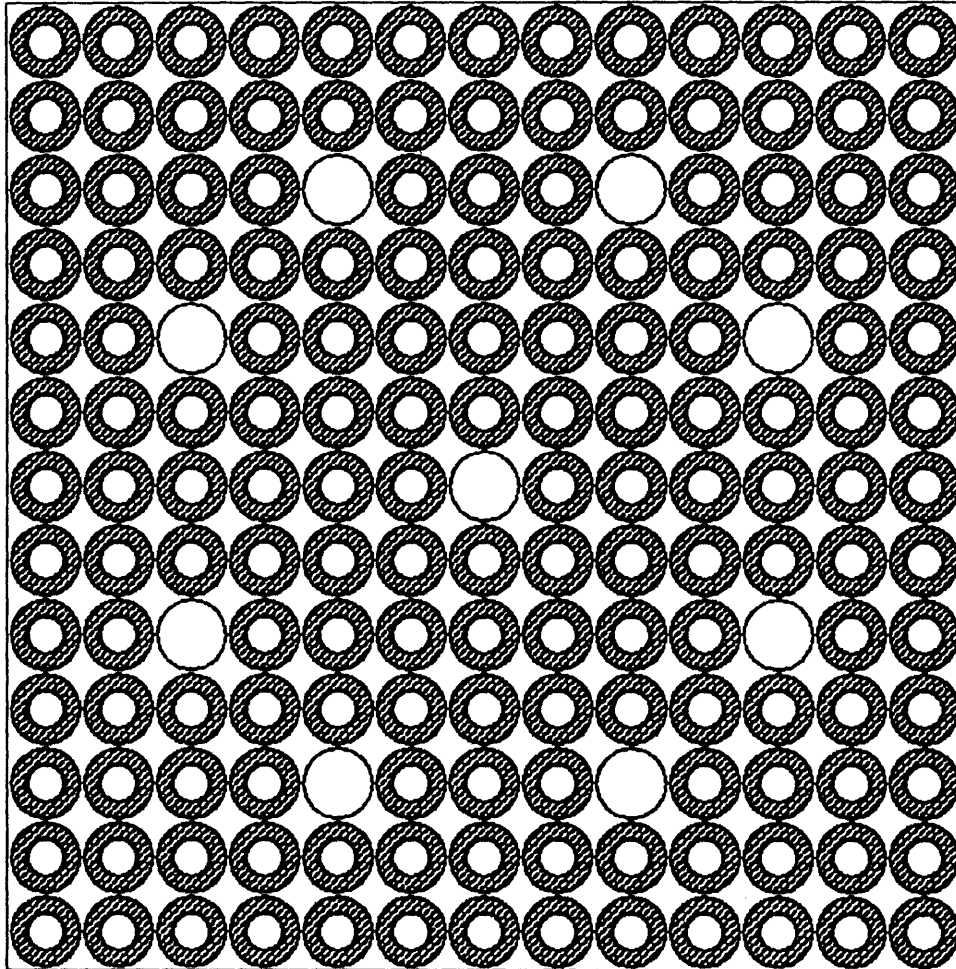


Figure 1.3 A Schematic of a 13x13 Internally and Externally Cooled Annular Fuel [Kazimi, 2002]

The configuration of VIPAC annular fuel core is the same except that the fuel-cladding gaps are eliminated.

1.6 Potential Advantages of the I&E Cooled Annular Fuel

The advantages of the annular fuel arise primarily from the reduction of fuel temperature. For 100% core power, the 13x13 annular fuel has higher pin power than the 17x17 reference pin due to the smaller number of fuel pins in the core. However, the fuel peak temperatures for I&E cooled annular fuel are significantly lower than those of the reference solid PWR pin as seen in Figure 1.4. The VIPAC I&E cooled annular fuel temperature is lower than sintered I&E cooled annular fuel due to the much larger thermal conductance at the fuel-cladding interfaces.

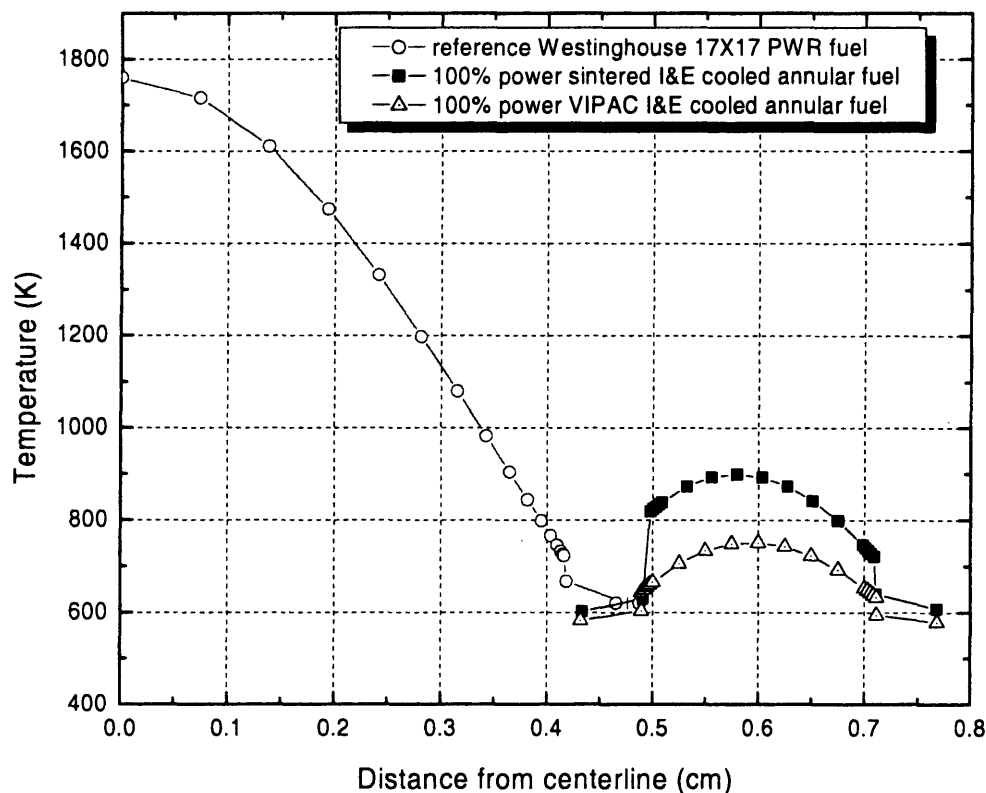


Figure 1.4 Comparison of Peak Temperatures of the Annular Fuel with the Reference PWR Fuel

The lower fuel temperatures of the I&E cooled annular fuel can be explained by: (1) increased cooling surface, (2) reduction of conduction thickness and in the VIPAC annular case, the elimination of gaps, and (3) higher thermal conductivity at low temperature regime.

The lower fuel temperature of the annular fuel will reduce the stored thermal energy in the fuel and consequently less energy needs to be removed during a Lost of Coolant Accident (LOCA). The reduced stored energy for annular fuel results in ~300 K smaller Peak Cladding Temperature (PCT) than the solid fuel at 100% power and ~600 K lower than NRC 1473K limit. [Kim et al, 2002 a and b]

The fuel performance will also benefit from the lower fuel temperature of I&E cooled annular fuel. The fission gas release is expected to be lower since the fuel temperatures fall in the low temperature fission release regime where fission gas migration by thermal diffusion is not activated and knock-out and recoil are the major mechanism for fission gas release [Yuan, et al, 2003]. The I&E cooled annular fuel has comparable or lower cladding temperatures while operating for the same period of time in the reactor. Thus, the cladding oxidation and hydrogen pick-up is expected to be comparable to that of the reference PWR solid pin. However, for a single pin, the linear power is much higher. Therefore it is possible to achieve much higher burnup than the solid pin. Neutronic analysis confirmed that with an enrichment of 8%, the I&E cooled annular fuel can achieve a discharge burnup of 88MWd/kgHM. [Xu et al, 2002]

Fabrication studies of annular pellets and VIPAC annular fuel revealed that, with existing commercial capability, the concentric cladding can be produced without prohibitive high cost. As a demonstration, 70 green annular pellets were fabricated in the Columbia commercial production facility of Westinghouse Fuel Company with 100% yield, and 60 pellets were sintered under commercial production facility with 100% yield. [Kazimi et al, 2003 a]. In another demonstration of VIPAC fuel fabrication, AECL has produced six annular fuel test specimens with proven capability. [Kazimi et al, 2004]. As the annular fuel can be operated at 150% power and high burnup can be achieved, the annular fuel would be economically attractive although the fuel fabrication cost appears to be somewhat higher than that of the operating PWR fuels.[Kazimi et al, 2002]

1.7 Annular Fuel Irradiation Program at MIT

The annular fuel development project includes irradiation testing of VIPAC annular samples at the MITR-II, which is the research reactor of MIT. A dummy MITR fuel element which contains VIPAC annular fuel capsules has been inserted in the reactor for a certain period of time. The capsule is designed to simulate representative neutronic and thermal conditions so that the fuel behavior such as dimensional and microstructure changes, fission gas release, fuel densification and swelling can be assessed. The dummy element is shown in Figure 1.5.

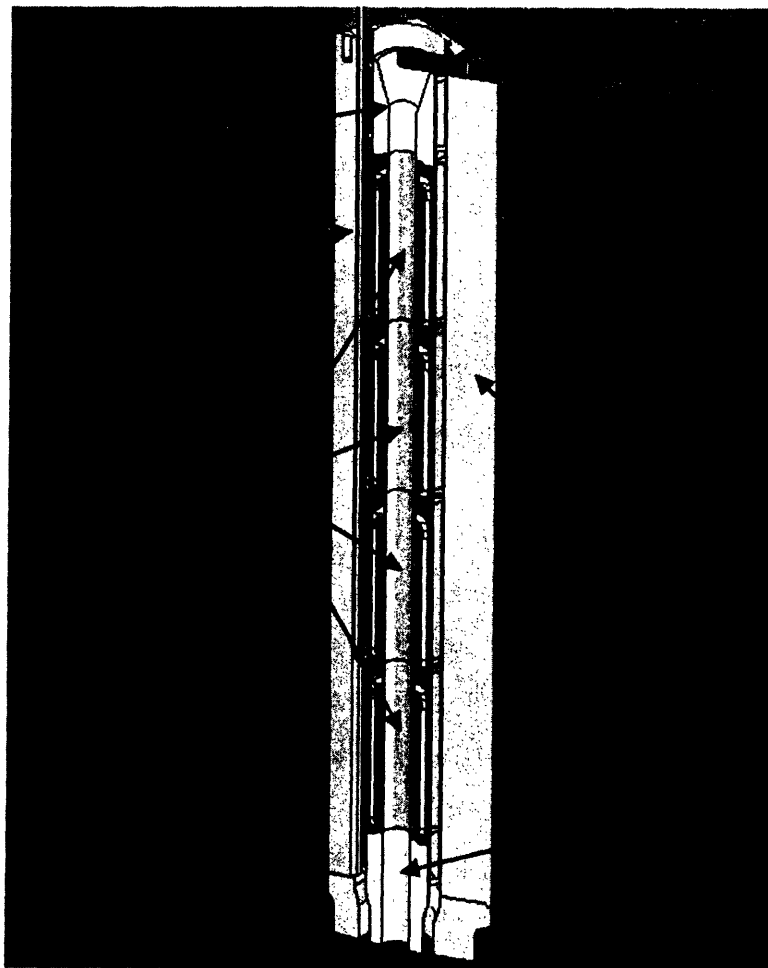


Figure 1.5 Dummy MITR Fuel Element Hosting Annular Fuel Irradiation Segment

[Kazimi et al, 2002]

The fuel sample has been enclosed within aluminum inner and outer thimbles, with a Lead-Bismuth Eutectic (LBE) bond in between to obtain the desired fuel cladding temperatures.

Thermal couples were attached to monitor the cladding temperature during the irradiation. [Kazimi et al, 2002]

Post Irradiation Examination (PIE) will be performed to measure the fission gas release, burnup distribution and densification or relocation.

1.8 FRAPCON-3 Computer Code

Both the sintered and the VIPAC fuel performance codes will be developed based on FRAPCON-3 code, which is the Nuclear Regulatory Commission (NRC) licensed computer code for steady state fuel performance modeling. [Lanning et al, 1997] The development of FRAPCON-3 reflects the continuous efforts in the last several decades and the code provides accurate prediction of fuel behavior during steady state at high burnup (up to 65MWd/kgU). The code was developed from Version 1, Modification 5, of the FRAPCON-2 code with simplified code structure and predictive abilities extended to high burnup.

The code can be utilized to perform steady-state fuel rod calculations and to provide initial input conditions for FRAPTRAN for transient analyses. A single channel coolant enthalpy rise model has been used in the code and a finite difference heat conduction model with different mesh spacing to capture the power peaking at the pellet edge due to rim effects has also been applied in the code. A new fuel-cladding contact gap conductance model and a new model from the Institute for Transuranium Elements for calculating the radial power peaking in high burnup fuel are also implemented in the code. The code also uses a new fission gas release model which is based on Booth diffusion model and incorporates grain boundary gas accumulation and resolution mechanism to calculate gas release during steady-state power and slow power ramp operation. New fuel behavior models including a thermal conductivity (includes burnup degradation) model, a swelling, cracking, and relocation model have been introduced in the code. New cladding behavior models for corrosion and hydriding, axial growth, and mechanical properties are also used. The MATPRO material properties package is also used in FRAPCON-3. [Lanning et al, 1997]

1.9 Thesis Organization

This thesis covers the development of annular fuel performance models and evaluations of annular fuel design.

In Chapter 2, general aspects of annular fuel modeling are discussed. The code structure and the temperature profile calculations with the specific boundary conditions for annular fuel are discussed. A new burnup model which captures the rim effects is presented. The differences between the sintered fuel and VIPAC fuel performance modeling are also analyzed.

In Chapter 3, the specific fuel performance models for the sintered fuel are discussed. Development of the fuel dimensional change model, the fuel cladding interaction model and the fission gas release model is presented. The fuel cladding interaction mechanism will be emphasized since it presents a challenge for the sintered annular fuel performance and design.

In Chapter 4, a parametric study is performed and various designs will be evaluated at different operating power. The parameters such as gap size, gap pressure, cladding thickness, cladding roughness, thermal conductance, operating power, etc will be analyzed to identify the sensitivity and design constraints. The design of the sintered I&E cooled annular fuel will be optimized.

In Chapter 5, the VIPAC fuel modeling models are presented. The models for thermal expansion, swelling, thermal conductivity, thermal conductance will be discussed. VIPAC fuel cladding interaction mechanism will also be discussed.

In chapter 6, a parametric study will be performed for VIPAC I&E fuel to identify the sensitivity, design constraints and optimum fuel design.

In Chapter 7, a short discussion of transient behaviors including a review of Lost of Coolant Accident (LOCA) analysis and a preliminary assessment of Reactivity Initiated Accident (RIA) will be presented.

In Chapter 8, the analyses for annular fuel testing and post irradiation examination will be provided.

In Chapter 9, the thesis contribution will be summarized and future work to extend this study will be recommended.

Chapter 2 General Aspects of Annular Fuel Performance Modeling

2.1 Overview

In order to accurately predict the performance of the internally and externally cooled annular fuel during steady state operation, it is imperative to be able to calculate the following items as a function of time: (1) the temperature profiles in the fuel and the claddings, (2) the heat flux at the inner and outer coolant channels, (3) the fuel cladding mechanical interactions, for the sintered annular fuel, the fuel-cladding interaction takes place at the moment gap closure occurs while for VIPAC annular fuel, the fuel interacts with cladding throughout the irradiation period since there is no gap in VIPAC fuel rod, (4) the fuel swelling, relocation, thermal expansion and densification, (5) the cladding elastic and plastic deformation, (6) fission gas release and rod internal pressure and (7) cladding hydrogen concentration and oxide accumulation. The sintered UO_2 and zircaloy cladding material properties have been extensively investigated since the inception of nuclear power generation. Models for sintered UO_2 thermal conductivity, thermal expansion, mechanical properties and fission gas release as well as models for cladding thermal conductivity, mechanical properties, hydrogen and oxide accumulations have been well developed and there will be no significant modifications to these models. The uniqueness of the internally and externally cooled annular fuel is embodied in its concentric inner and outer claddings, each of which separates the fuel from a thermal cooling channel. Therefore, the fuel temperature profile and mechanical interaction behavior are very different from today's commercial fuel. Furthermore, it is desirable to develop an annular fuel performance code based on an industry standard fuel performance code. The FRAPCON-3, which is a US Nuclear Regulatory Commission licensed code that calculates the steady state-state response of light water reactor fuel rods during long-term burnup (up to 65GWd/MTU) [Berna et al, 1997], has been selected as the basis for the I&E cooled annular fuel performance code development. Modeling and code development should also incorporate some new findings of high burnup fuel fission gas release and cladding corrosion. [Long, 2002]

To model the steady state fuel performance, it is assumed that during each time step, the boundary conditions will not change and the cladding will maintain its cylindrical shape with small dimensional changes (less than 5%). It is also assumed that axial heat transfer is negligible and the heat transfer is azimuthally symmetric. Any power ramp is assumed to be very slow, and the cladding temperatures are taken to be lower than 700K.

Performance codes for internally and externally cooled annular LWR fuels have been developed for both the sintered fuel and VIPAC fuel. Both performance codes are based on the currently available FRAPCON-3 with major modifications in code structure, temperature and burnup calculation models as well as models for fuel cladding interaction. The structure of this code has been designed to reflect the specifics of annular LWR fuel. Calculations of the coolant channel conditions are performed for both the inner and outer channels. Cladding corrosion, crud uptake, hydrogen concentration, coolant film temperature drop, and temperature drops in crud, oxide layer and across the cladding are also calculated for both the inner and outer cladding. The power profile of the annular fuel is calculated including in the rim. For the sintered fuel, the fuel dimensional changes as a result of thermal expansion, swelling, densification and relocation are computed to obtain the sizes of the inner and outer gaps. A new fuel cladding mechanical interaction model, which captures the interaction between the fuel and two claddings, has been developed and implemented. For VIPAC fuel, the thermal conductivity and thermal conductance models are developed. The models for fuel swelling, thermal expansion and densification as well as interactions with porosities are also developed. In this chapter, some of the shared features of the sintered and VIPAC annular fuels are discussed, including the general code designs and calculation schemes as well as the burnup models.

2.2 Issues and Structure of Annular Fuel performance model development

2.2.1 The challenges for Annular Fuel Performance Model Development

Unlike the conventional LWR solid pellet, the annular fuel has two heat transfer channels and two fuel-cladding gaps for the sintered fuel, which adds to the complexity of the fuel performance modeling. First, the addition of an inner cladding will require the calculations of inner cladding stress and strain, cladding hydrogen concentration and oxide thickness, etc. Second, the heat generated in the fuel will be dissipated to two coolant channels and the heat flux split will vary as a function of time. Third, the existence of water at both the inner and outer side of the fuel will cause rim effects at both inner and outer fuel peripheries and the fuel burnup distribution will be different from that in a solid fuel. Fourth, if the I&E cooled annular fuel is to achieve equivalent or higher core power density than the operating LWR fuel, it has to be irradiated to high burnup. The high burnup fission gas release and cladding behavior also pose challenges to fuel model development.

For the sintered fuel, the presence of inner and outer gaps and the interactions of these two gaps with fuel will change the fuel temperature profile and heat flux split, which would in turn affect the gap sizes and fuel-cladding interactions. Therefore the complicated thermal and mechanical responses to gap size changes and fuel-cladding interactions will make the iteration to reach convergence a practical difficulty in the sintered case. The presence of double gaps will allow more freedom for fuel movement; the fuel dimensional changes resulting from thermal expansion, relocation, and swelling and densification will be more complicated than in a solid fuel case. The elastic and plastic strains and the cladding oxide thickness are major constraints for steady state fuel performance; therefore it is vital to capture fuel cladding interactions which is complicated by two fuel-cladding gaps.

For the VIPAC annular fuel, the gaps are eliminated and the fuel-cladding interactions take place throughout the irradiation period. The interactions between the VIPAC fuel and the claddings will be different from the sintered fuel. The fuel dimensional changes from thermal expansion, swelling and densification are complicated by the large porosity, which could accommodate some of these dimensional changes. The thermal conductivity of the fuel varies with burnup, packing density, temperature and etc. The fission gas release from the VIPAC fuel at high burnup is a challenge for model development since model for such fuel has not been developed yet.

The major challenges and tasks for the sintered fuel and VIPAC fuels are summarized in Table 2.1.

2.2.2 Code Structure and Iteration Scheme

Since the fuel boundary conditions and the fuel dimensional changes vary as a function of time, the solution for fuel temperature distributions has to be found through iterations in the I&E cooled annular fuel performance code. For each axial location, the heat flux distributions need to be identified to calculate the boundary temperatures. The rod temperature distribution is constructed by calculating the temperature profile at each axial node. Finally, for each time step, the input operating power, flow condition may be different. Therefore, calculations should be performed for each time step.

Code structure for the Sintered fuel

In the sintered I&E cooled annular fuel performance code, six iteration loops are involved in the calculations: gap conductance loop, radial heat transfer loop, axial node loop, flow split loop(non-functional at the present stage), fission and fill gas convergence loop and time loop.

Table 2.1 Main Tasks and Challenges for Modeling I&E Cooled Fuel Performance

Tasks	Challenges (the sintered fuel)	Challenges (VIPAC fuel)
Fuel temperature profile calculation	• Different boundary conditions. • Meshing scheme • Temperature drops in two gaps	• Different boundary conditions. • Meshing scheme • VIPAC fuel thermal conductivity model • VIPAC fuel cladding interfacial thermal conductance
Fuel-Cladding interaction model	• Interaction at two gaps • Modification of FRACAS-I model	• VIPAC fuel-cladding interaction • Modified FRACAS model for VIPAC fuel
Heat flux split and flow split*	• New iteration loops • Convergence problems	• New iteration loops • Convergence problems
Fission gas release	• Fission gas release model for high burnup	• Fission gas release model for high burnup VIPAC fuel
Fuel dimensional changes	• Thermal expansion, swelling, relocation and densification	• Thermal expansion, swelling and densification models • Porosity effect on swelling
Radial power profile	• The radial power • Rim effect	• The radial power • Rim effect
Cladding condition	• Cladding temperature drop, corrosion, crud uptake, cladding H₂ concentration calculations for both inner and outer cladding	• Cladding temperature drop, corrosion, crud uptake, cladding H₂ concentration calculations for both inner and outer cladding
Other modifications	• Code initialization, burnup calculation, fuel volume calculation, rod pressure	• Code initialization, burnup calculation, fuel volume calculation, rod pressure

* Flow split loop is currently non-functional due to convergence problem

In the gap conductance loop, the gap size is estimated with which the rod and cladding temperature profile can be derived. A new gap size can then be obtained from the dimensional changes resulting from thermal expansion, swelling, densification and fuel-cladding interactions. The new gap size will be compared with the estimated gap size till they converge and the same temperature profiles are obtained in two sequential calculations. The gap conductance variation will cause the heat flux variations during fuel operation. A heat flux split loop iterates for each fuel node to obtain the heat flux split at each axial location. In the axial node loop, local power and flow conditions are provided for each fuel node to calculate the local burnup and local thermal conditions. Fission gas release and stored energy are calculated from the fuel temperature profile. The flow split calculation can be calculated if the cladding axial temperature profile is known for both inner and outer cladding. However, the two-channel calculation of flow split may not be able to accurately reflect the realities of flow conditions in the reactor core and the calculations fail to converge with flow split loop. Hence, this loop is non-functional at present stage and flow split is provided as an input from sub-channel thermal hydraulic code calculations. In the gas pressure convergence loop, the rod internal gas pressure is calculated with known fission gas release and free volume. All of the abovementioned calculations will be performed at each time step so that a complete history of the fuel performance during the irradiation can be obtained. The code structure is illustrated in Figure 2.1.

Code structure for the VIPAC Annular fuel

The same iteration scheme is used for VIPAC annular fuel although both the inner and outer fuel-clad gaps are zero. The gap size iteration loop is bypassed but other iteration loops still remain. The temperature profile will be calculated through the iterations and the fuel-clad mechanical responses are derived from the fuel swelling, thermal expansion and densification as well as cladding deformations through the iterations. The iteration convergence is less severe for the VIPAC annular fuel than for the sintered annular fuel.

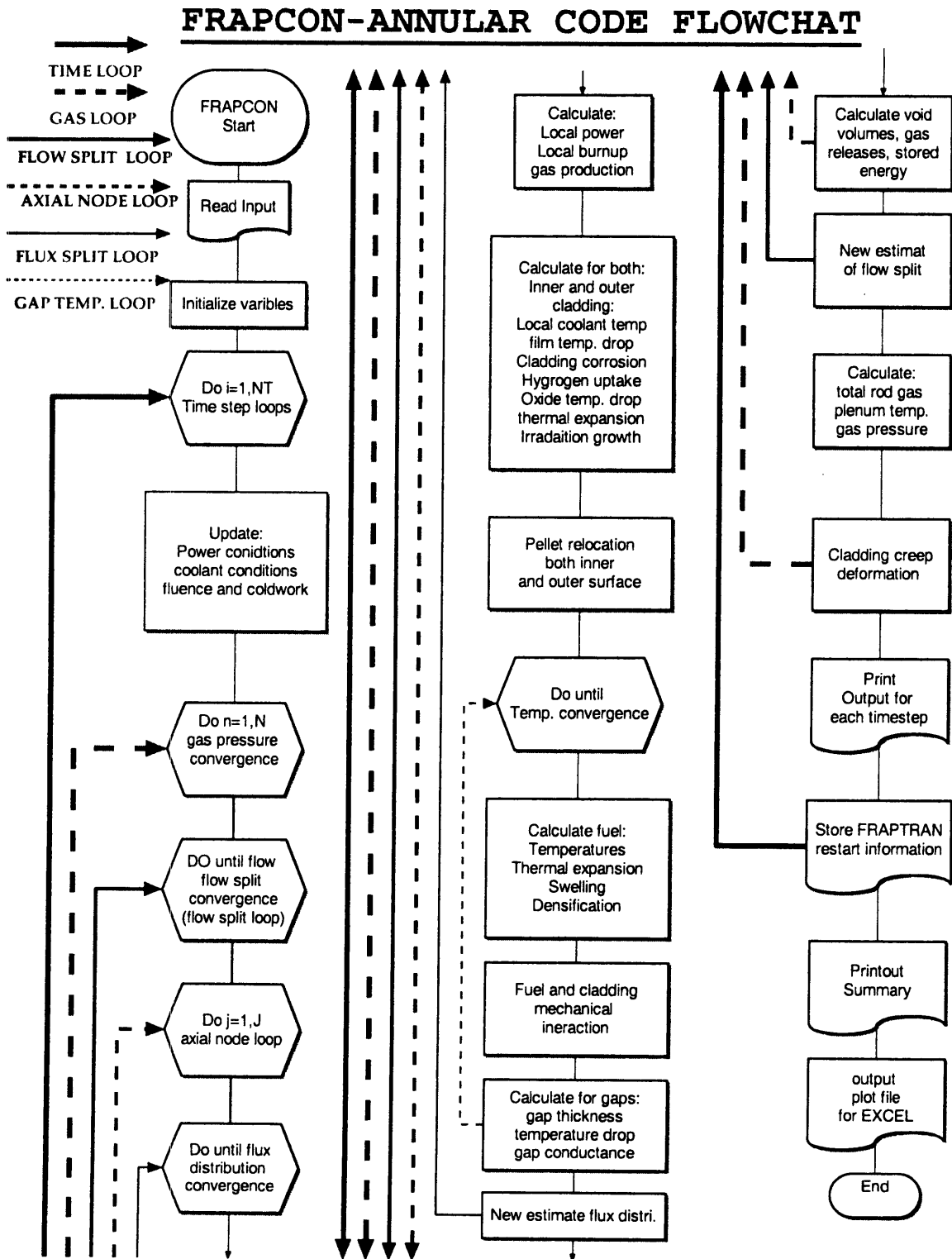


Figure 2.1 Flowchart of FRAPCON-ANNULAR for I&E Cooled Annular Fuel

(* Flow Split Loop is Non-functional)

2.3 Rim Effect of I&E Cooled Annular Fuel

2.3.1 Overview

Due to U-238 resonance capture of epithermal neutrons, the buildup of Pu-239 is significantly larger at the fuel surfaces facing the coolant. This leads to power peaking at both fuel surfaces and consequently higher burnup at fuel surfaces. Unlike the solid fuel with power peaking at the outer surface, the internally and externally cooled annular is expected to have power peaks at both the inner and outer surfaces since U-238 capture effects take place at both surfaces. By the same token, the local burnups at the inner and outer surfaces of internally and externally cooled annular fuel are also expected to be higher. At the higher local burnup region, porous zones (typically 100-200 μm thick) are formed, which are characterized by fine grains of 0.1-0.5 μm and 1-2 μm gas bubbles uniformly distributed at sub-grain boundaries.

The fuel behavior will be affected by the rim effects in a number of ways: (1) the larger porosity at the rim zone will decrease the local thermal conductivity which tends to increase the fuel temperatures, (2) the fuel cladding interactions can be aggravated due to larger swelling at the rim zone and during an RIA event, the early separation of rim zone may lead to cladding brittle failure, (3) the porous nature of the rim zone poses a concern for spent fuel geological behavior since larger surface areas of rim zone are available for leaching. [Long, 2002]

The burnup models for the internally and externally cooled annular will be developed and the rim effect influence on the fuel thermal behavior will be taken into account in the FRAPCON-ANNULAR model.

2.3.2 Calculated Radial Power Profile

The radial power profile of the internally and externally cooled annular fuel was calculated using a Monte Carlo neutron physics code MCNP4C. The comparison between the solid fuel radial profile and annular fuel radial power profile at various burnups is shown in Fig 2.2. [Xu et al, 2002]

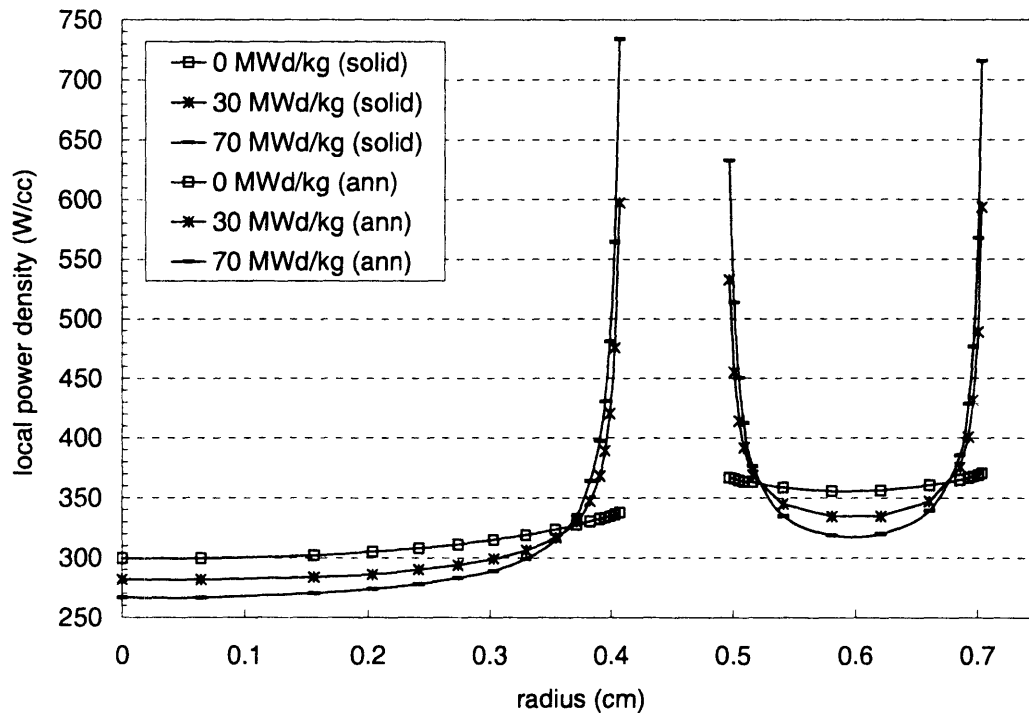


Figure 2.2 Radial Power Profile for Solid and Annular Fuel [Xu et al, 2002]

It can be seen from Figure 2.2 that the internally and externally cooled fuel has power peaks at both surfaces, which can be explained by the shielding effects at both surfaces. At the same burnup, the I&E cooled annular fuel has lower power peaks than the solid fuel. Similar to the solid fuel, the rim effect will take place in I&E cooled annular fuel. Since the I&E cooled annular fuel is affected by rim effects at both surfaces, the aggregated rim effects may be larger than the solid fuel although the power peaks at each rim region is smaller than in the solid fuel.

The VIPAC annular fuel radial power profile is slightly different from that of the sintered annular fuel depending on the VIPAC smear density. However, the same radial power profile model is applied in VIPAC annular fuel the difference is small.

2.4 I&E Cooled Annular Fuel Radial Power /Burnup Model

2.4.1 Radial Power/Burnup Model Description

Although the power shape and burnup distribution can be calculated using a complex neutron physics code, it is also desirable to use a simple burnup model in the fuel performance code. To calculate the radial power profile in an I&E cooled annular pellet, a power/burnup distribution model has been developed for the fuel performance code based on the TUBRNP model. [Lassmann et al, 1994] This new model solves a simple one-group diffusion equation to obtain the radial power and burnup distribution. It incorporates the resonance absorptions at both the inner and outer surfaces of an I&E cooled annular fuel.

The neutron flux distribution of the solid fuel or annular fuel without internal cooling is solved by a simple one-group, one-dimensional diffusion process to get:

$$\Phi(r) = C \left(I_0(\kappa r) + K_0(\kappa r) \left(\frac{I_1(\kappa r_0)}{K_1(\kappa r_0)} \right) \right) \quad (2-1)$$

where

$$\kappa = \sqrt{\Sigma_a / D}, \Sigma_a = \sum \sigma_{a,i} \bar{N}_i \text{ and } D = \frac{1}{3\Sigma_s} = \frac{1}{3\sigma_s \bar{N}_{tot}} \quad (2-2)$$

and

I,K= Modified Bessel functions

C= a constant

σ_a, σ_s =absorption and scattering microscopic cross sections

$\bar{N}$ =pellet-average atom concentration

r_0 =the pellet outer diameter

r =radial location of neutron flux

i=subscript indicating all U, Th and Pu isotopes

The I&E cooled annular fuel neutron flux can be solved using equation (2-1) and it will be different from that of the solid fuel. However, a flat neutron flux shape is applied since the annular fuel thickness is fairly small (~2mm) and MCNP result shows very flat neutron flux at the BOL. Based on the flat neutron flux profile, the Pu-238 absorption rate will be represented by an empirical curve to fit the neutronic calculations by MCNP.

The changes of isotope concentrations with burnup can be described with following differential equations:

$$\frac{d\overline{N}_{235}(r)}{dt} = -\sigma_{a,235}\overline{N}_{235}(r)\phi(r) \quad (2-3)$$

$$\frac{d\overline{N}_{238}(r)}{dt} = -\sigma_{a,238}\overline{N}_{238}(r)\phi(r) \quad (2-4)$$

$$\frac{d\overline{N}_j(r)}{dt} = -\sigma_{a,j}\overline{N}_j(r)\phi(r) + \sigma_{c,j-1}\overline{N}_{j-1}(r)\phi(r) \quad (2-5)$$

where , j=Pu-240, Pu-241,Pu-242

σ_a =absorption microscopic cross sections

$\sigma_{c,j-1}$ =the capture cross section

$\overline{N}$ =pellet-average atom concentration

However, $\sigma_{a,238}(r, N_{235}, N_{238})$ are equivalent absorption cross sections including thermal and resonance absorption, and are therefore functions of radius:

$$\sigma_{a,238}(r, N_{235}, N_{238}) = \overline{\sigma}_{a,238}(N_{235}, N_{238})f(r) \quad (2-6)$$

The shape function $f(r)$ for a typical annular fuel dimensions from a 13×13 lattice can be fitted to MCNP calculations by the following empirical equation:

$$f(r) = 1.5 \cdot e^{-8.2(r-r_i)^{0.56}} + 1.74 \cdot e^{-7.3(r_o-r)^{0.5}} + 0.26 \cos\left(\frac{r - \frac{r_o + r_i}{2}}{r_o - r_i}\right) \quad (2-7)$$

2.4.2 Comparison of MCNP and FRAPCON-ANNULAR Burnup Model

The I&E cooled annular fuel burnup radial power/burnup model is compared to the MCNP calculations at various burnups (Figure 2.3). At 0 MWd/kgU burnup, the power shape is flat since there is no plutonium build-up in the fuel. At 30 MWd/kgU burnup, the power peaks at both inner and outer peripheries become evident due to non-uniform formation of Pu-239. At 70 MWd/kgU burnup, the power peaks at fuel peripheries are about twice the value of the power in the interior of the fuel.

The power/ burnup model in FRAPCON-ANNULAR code has achieved reasonable agreement with the MCNP calculations. Although there is a small discrepancy between the two calculations due to the crude nature of the former model, the effect on fuel performance is negligible.

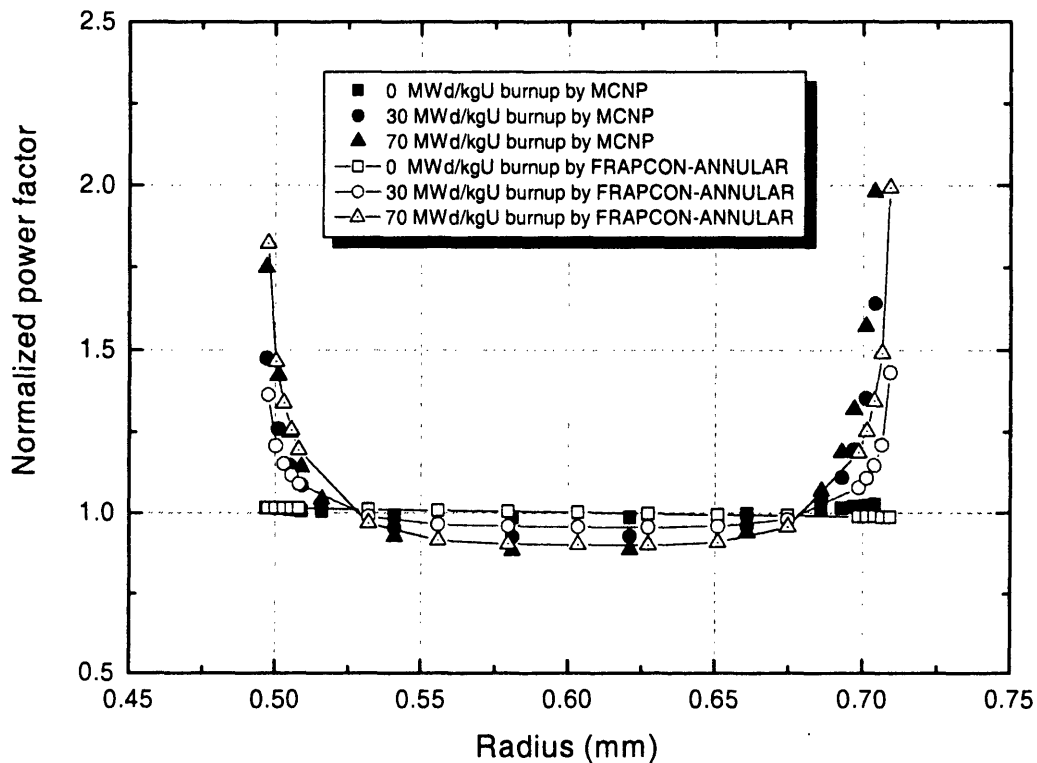


Figure 2.3 Calculated Power Distributions by MCNP and FRAPCON-ANNULAR Model

2.5 I&E Cooled Annular Fuel Temperature Profile Calculation

2.5.1 Radial Meshing Scheme

Since the fuel temperature profile is calculated by solving the integral heat transfer equation using the finite difference method, the fuel rod is divided into numerous grids along the radial axis. In order to capture the steep power rises and rim effects at the fuel regions close to inner and outer surface, the meshing scheme for the finite difference calculations of the fuel temperatures is designed such that more mesh points are placed in the regions with larger power gradients.

The meshing scheme for an I&E cooled annular fuel is compared to that of a solid fuel in Figure 2.4.

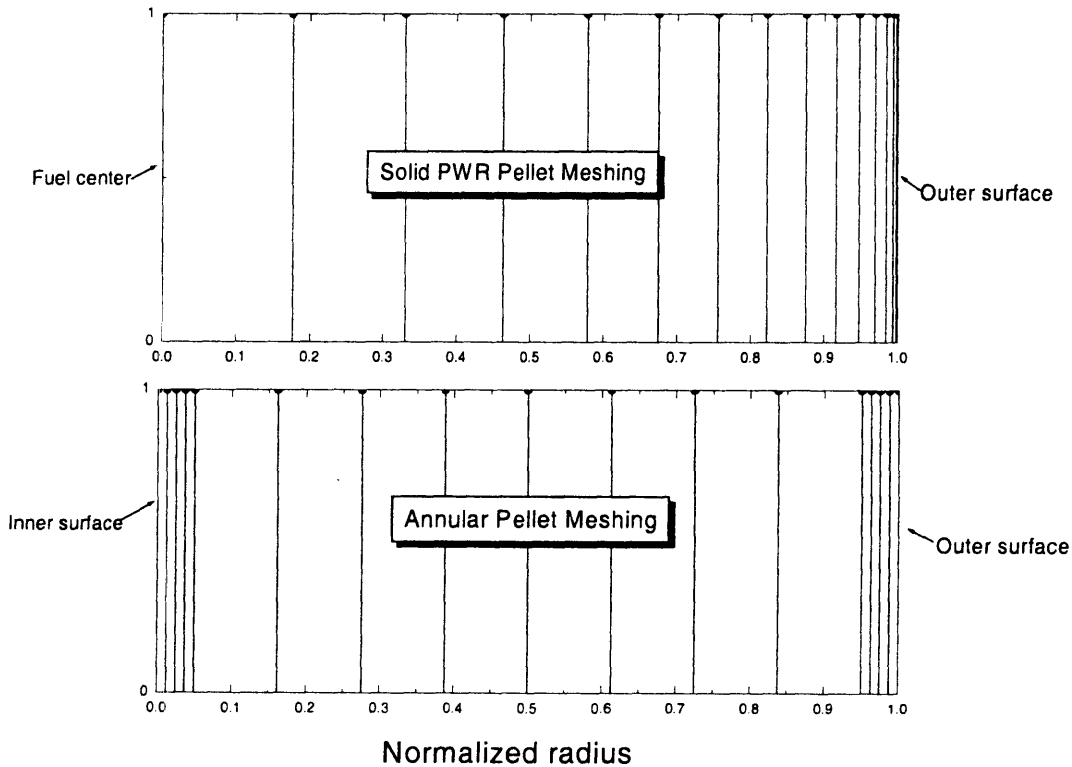


Figure 2.4 Radial Meshing Schemes for the Solid and Annular Fuel Pellets

2.5.2 The Annular Fuel Radial Temperature Profile Calculation

The generic steady-state integral form of the heat conduction equation is

$$\iint_s K(T, \vec{x}) \vec{\nabla} T(\vec{x}) \cdot \vec{n} ds = \iiint_V S(\vec{x}) dV, \quad (2-8)$$

where

K = thermal conductivity (W/m-K),

s = surface of the control volume (m^2),

n = the surface normal unit vector,

S = internal heat source (W/m^3),

V = control volume (m^3), and

x = the space coordinates (m),

T =temperature (K)

The following assumptions were made to solve the integral equation: [Berna et al, 1997]

- Fixed geometry,
- Symmetrical heat transfer azimuthally,
- Negligible heat conduction in the axial direction,
- Steady state conditions, and
- Average material properties are applied at each meshing point.

Two boundary conditions are used to solve the thermal conduction equation for the annular fuel:

$$(1) T_{\text{inner_surface}} = T_{\text{inner_clad}} + \Delta T_{\text{inner_gap}}$$

$$(2) T_{\text{outer_surface}} = T_{\text{outer_clad}} + \Delta T_{\text{outer_gap}}$$

For the sintered annular fuel, $\Delta T_{\text{inner_gap}}$ and $\Delta T_{\text{outer_gap}}$ are the inner and outer gap temperature drops. For VIPAC fuel, $\Delta T_{\text{inner_gap}}$ and $\Delta T_{\text{outer_gap}}$ are the temperature drops at the inner and outer interface.

These boundary conditions are different from those of the solid fuel (or annular fuel without internal cooling). For solid fuel, the boundary conditions include the fuel surface temperature and the centerline temperature gradient. For I&E cooled annular fuel, the boundary conditions are dictated by both the fuel inner and outer surface temperatures which can be obtained by calculating the cladding temperatures plus gap (or fuel-clad interface) temperature drops. With boundary conditions specified, the fuel radial temperature profile can be calculated and the fuel thermal mechanical responses corresponding to the temperature profile and irradiation history can be derived. For the sintered annular fuel, the gap sizes can be obtained by iterating on the temperature profile and the resulting dimensional changes. The boundary conditions and the iteration scheme for the sintered annular fuel are shown in Figure 2.5.

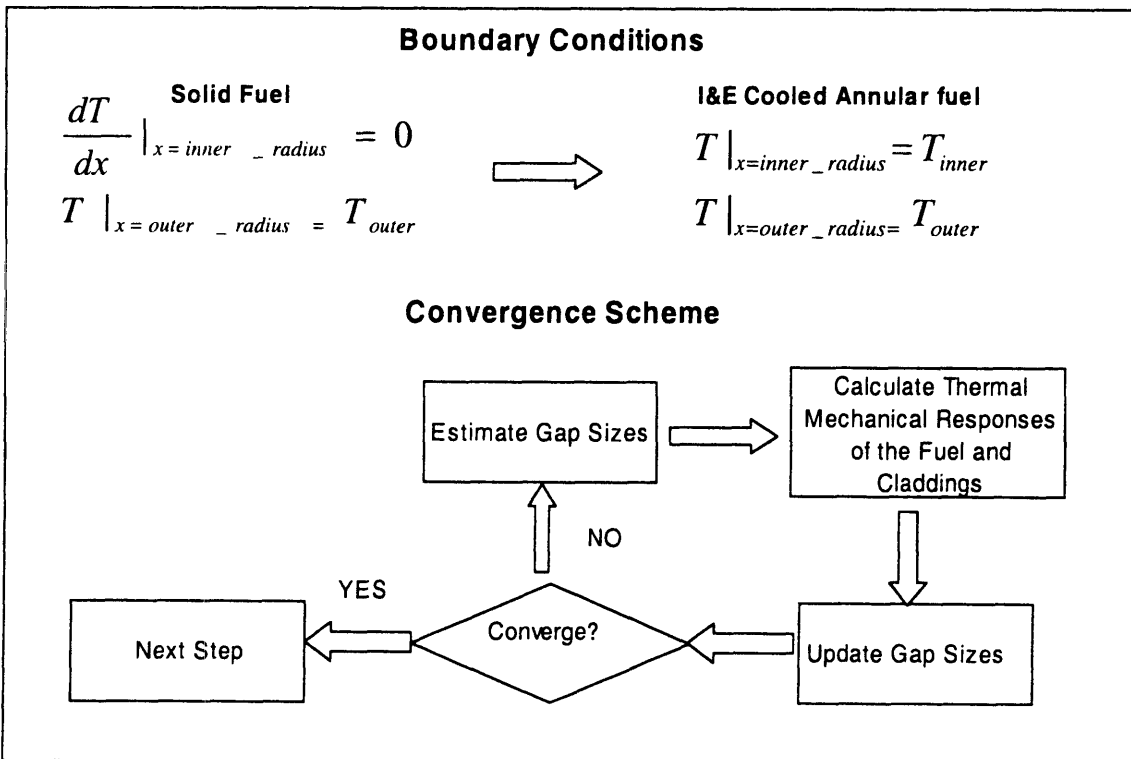


Figure 2.5 Boundary Conditions for Sintered I&E Cooled Annular Fuel and Gap Temperature Convergence Scheme

For VIPAC annular fuel, the iteration scheme follows the same steps as described in Figure 2.5. However, both the inner and outer gaps are zero. The temperature drops at the inner and outer interfaces are dictated by the interfacial pressures at each side. Therefore, reaching convergence is much easier in the VIPAC case.

The steady-state integral heat conduction equation of the annular fuel is solved by a finite difference approach. The meshing points are arranged such that more meshing points are placed at the fuel regions with larger power gradient. A typical mesh point is shown in Figure 2.6.

The finite difference approach used in the FRAPCON-ANNULAR code to solve the heat conduction equation is the same as that applied in FRAPCON-3 except the boundary conditions have changed. At each meshing point, the volume weighed average material properties are used.

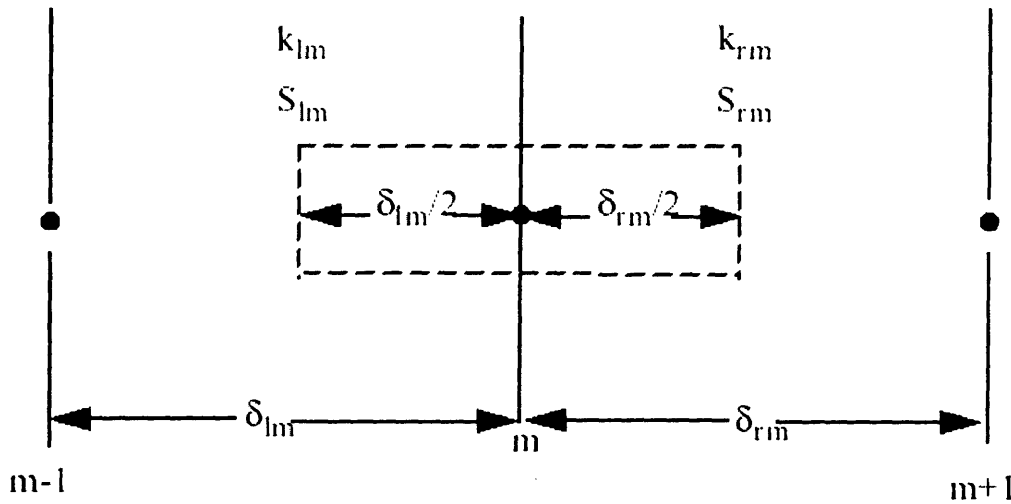


Figure 2.6 A Typical Mesh Points for Finite Difference Approach [Berna et al, 1997]

The heat conduction equation at each mesh point can be converted into numerical form using the following approximations: [Berna et al, 1997]

$$\iint_s K(T, \bar{x}) \bar{\nabla} T(\bar{x}) ds \approx (T_{m-1} - T_m) K_{lm} \delta_{lm}^s + (T_m - T_{m+1}) K_{rm} \delta_{rm}^s, \quad (2-9)$$

$$\iiint_V S(\vec{x}, t) dV \approx P_f P (Q_{lm} \delta_{lm}^v + Q_{rm} \delta_{rm}^v), \quad (2-10)$$

T is the temperature, K is the conductivity, δ is the mesh point spacing, subscript m denotes the m^{th} node, subscript l, r denote the left and right of the mesh point respectively. Superscripts s, v denote the surface and volume weighing factors respectively. $P_f P$ is the power density at a particular node, Q is the power factor at a radial position. While the definitions for $P_f, P, Q_{lm}, \delta_{lm}^v, Q_{rm}, \delta_{rm}^v$ are the same as in FRAPCON-3 manual. [Berna et al, 1997]

The algebraic equation for the m-th node can be written as

$$a_m T_{m-1} + b_m T_m + c_m T_{m+1} = d_m. \quad (2-11)$$

where

$$\begin{aligned} a_m &= -(k_{lm} \delta_{lm}^s) \\ b_m &= -a_m - c_m \\ c_m &= -(k_{rm} \delta_{rm}^s) \\ d_m &= P_f P ((Q_{lm} \delta_{lm}^v + Q_{rm} \delta_{rm}^v)) \end{aligned}$$

The system of linear equations can be written in a matrix form

$$\begin{bmatrix} b_1 & c_1 & & & & \\ a_2 & b_2 & c_2 & & & \\ & \bullet & \bullet & & & \\ & \bullet & \bullet & \bullet & & \\ & & & a_{m-1} & b_{m-1} & c_{m-1} \\ & & & & a_m & b_m \end{bmatrix} \begin{bmatrix} T_1 \\ T_2 \\ \bullet \\ \bullet \\ T_{m-1} \\ T_m \end{bmatrix} = \begin{bmatrix} d_1 \\ d_2 \\ \bullet \\ \bullet \\ d_{m-1} \\ d_m \end{bmatrix} \quad (2-12)$$

This matrix is solved using Gaussian elimination to obtain the radial temperature profile.

2.6 Summary

General aspects of the annular fuel modeling have been discussed, including the challenges and solution schemes for both the sintered and the VIPAC I&E cooled annular fuel. The fuel performance code structures have been designed and iteration loops constructed so that they are able to calculate the fuel temperature profiles, heat flux splits, fuel cladding dimensional changes and interactions at all axial locations and for the complete fuel irradiation span.

The rim effects of the annular fuel have been discussed and a simple burnup model is developed based on the original FRAPCON-3 burnup model with modifications in pellet radial power shape.

The fuel temperature profiles have been calculated with specific boundary conditions for the I&E cooled annular fuel and the mesh scheme for the finite differential calculation scheme is designed so that the rim effects at the fuel surfaces are captured.

Chapter 3 Performance Models for the Sintered Annular Fuel

3.1 The Sintered I&E Cooled Annular Fuel Dimensional Changes

The fuel dimensional changes as a result of thermal response and irradiation effects have significant impact on fuel performance. The mechanisms that cause fuel dimensional changes include: (1) fuel thermal expansion, (2) fuel densification due to disappearance of small size pores during the early stage of irradiation, (3) fuel swelling due to build-up of solid and gaseous fission products during irradiation, and (4) fuel relocation due to the movement of cracked fuel. The fuel temperature profile and cladding behavior are affected by these dimensional changes due to the variations of fuel cladding gaps and Fuel-cladding Mechanical and Chemical Interactions (FCMI). At the initial stages of fuel operation, fuel densification causes shrinkage of the fuel diameters and may be responsible for cladding failure due to cladding creep-down under the coolant pressure. At high burnup, excessive solid swelling becomes a major constraint for cladding performance. In order to describe the fuel behavior, it is important to accurately model the mechanisms for fuel dimensional changes.

3.1.1 Determination of the Anchor Ring for Thermal Expansion

The fuel dimensional changes for a certain temperature profile are calculated by dividing the fuel into multiple rings and summing up the contributions of swelling, densification and thermal expansion from each ring. Therefore, it is critical to find out an anchor ring from which other ring dimensions can be deduced.

The anchor ring can be determined using a sophisticated finite element analysis code. However, it is more realistic to apply an analytical approach with appropriate assumptions:

- temperature is a function of radial distance r only
- no axial strain ($\varepsilon_z = 0$) throughout the cylinder

- boundary conditions: $\sigma_r=0$ at boundary surfaces
- the annular hollow cylinder with inner and outer radii, a and b

For such a case, the analytical solution for radial displacement from thermal expansion can be written as (Young and Budynas, 2001):

$$u(r) = [(1+\nu)/(1-\nu)](\alpha/r) \{ [(1-2\nu)a^2 + a^2]/(b^2 - a^2) \} \int_a^b \Delta T r dr + \int_a^r \Delta T r dr \quad (3-1)$$

Where

$u(r)$ is the thermal displacement at radius r

ν is Poisson's ratio,

α is the thermal expansion coefficient

a,b is the inner and outer radii respectively

$\Delta T = T - T_o$, T is the temperature at radius r and T_o is temperature at the inner surface

Then radial displacements, u_i and u_o at the inner and outer surfaces($r=a$ and $r=b$) can be derived:

$$u_i = [(1+\nu)/(1-\nu)](\alpha/a) \{ [(1-2\nu)a^2 + a^2]/(b^2 - a^2) \} \int_a^b \Delta T r dr \quad (3-2)$$

$$u_o = [(1+\nu)/(1-\nu)](\alpha/b) \{ [(1-2\nu)a^2 + a^2]/(b^2 - a^2) \} [2 \int_a^b \Delta T r dr] \quad (3-3)$$

As the area-averaged temperature gradient is defined as:

$$\Delta \bar{T} = \frac{\int_a^b \Delta T 2\pi r dr}{\int_a^b 2\pi r dr} \quad (3-4)$$

then

$$\int_a^b \Delta T r dr = \Delta \bar{T} (b^2 - a^2) / 2 \quad (3-5)$$

where $\Delta \bar{T} = \bar{T} - T_o$

Then the radial displacements, u_i and u_o at the inner and outer surfaces, $r=a$ and $r=b$ in terms of the area-average temperature can be derived from equation 3-2, 3-3 and 3-5 as:

$$u_i = a(1 + \nu)\alpha\Delta\bar{T} \quad (3-6)$$

$$u_o = b(1 + \nu)\alpha\Delta\bar{T} \quad (3-7)$$

Therefore, the inner radius becomes

$$r_i = a + u_i = a[1 + (1 + \nu)\alpha\Delta\bar{T}] \quad (3-8)$$

However, the inner radius dimension at the hot conditions is derived with the assumption that the axial strain of the cylinder is zero. The contribution of $\nu\alpha\Delta T$ comes from the restriction of axial deformation from axial thermal expansion. Therefore, to relax the zero axial strain assumption, the inner radius can be approximated as following:

$$r_i = a + u_i = a[1 + \alpha\Delta\bar{T}] \quad (3-9)$$

Therefore, the inner radius can be calculated with fuel volume averaged temperature. This inner radius is used as an anchor to place the surrounding rings in such a way as to maintain radial continuity.

3.1.2 Fuel Dimensional Changes from Thermal Expansion, Densification and Swelling

Isotropic expansion in the r , θ , z directions by thermal expansion, swelling and densification is assumed in the fuel. In order to capture the effect of the temperature profile of the fuel, the fuel is divided into a number of rings and each ring is assumed to expand freely. The overall fuel dimensional changes are the cumulative effects of this free ring expansion. The following correlations are developed to describe this phenomenon:

$$r_1 = R_1[1 + \alpha \Delta \bar{T}] \quad (3-10)$$

$$r_2 = r_1 + (R_2 - R_1) \cdot (1 + \varepsilon_1^1) \quad (3-11)$$

$$r_3 = r_2 + (R_3 - R_2) \cdot (1 + \varepsilon_1^2) \quad (3-12)$$

...

$$r_n = r_{n-1} + (R_n - R_{n-1}) \cdot (1 + \varepsilon_1^{n-1}) \quad (3-13)$$

$r_1, r_2, \dots, r_n$ denotes the hot condition ring dimensions of the fuel, starting from the inner radius. $R_1, R_2, \dots, R_n$ denotes the cold state ring dimensions of the fuel. And ε_i^i is the total strain at the i^{th} ring.

$$\varepsilon_i^i = \varepsilon_{thermal} + \varepsilon_{swelling} + \varepsilon_{densification} \quad (3-14)$$

where $\varepsilon_{thermal}$, $\varepsilon_{swelling}$, $\varepsilon_{densification}$ are the thermal expansion, swelling and densification strain respectively.

Similarly, the axial dimensional changes can be calculated as

$$L_{hot} = L_{cold} + \sum_{i=1}^n \Delta z_i = L_{cold} + \sum_{i=1}^n \Delta Z_i (1 + \varepsilon_i^i) \quad (3-15)$$

where L_{hot} , L_{cold} are the hot state and cold state fuel length respectively and Δz_i , ΔZ_i are the axial hot state and cold state node length at the i^{th} node.

The anchor ring (i.e. the inner ring) location and fuel radial dimension changes are illustrated in Figure 3.1.

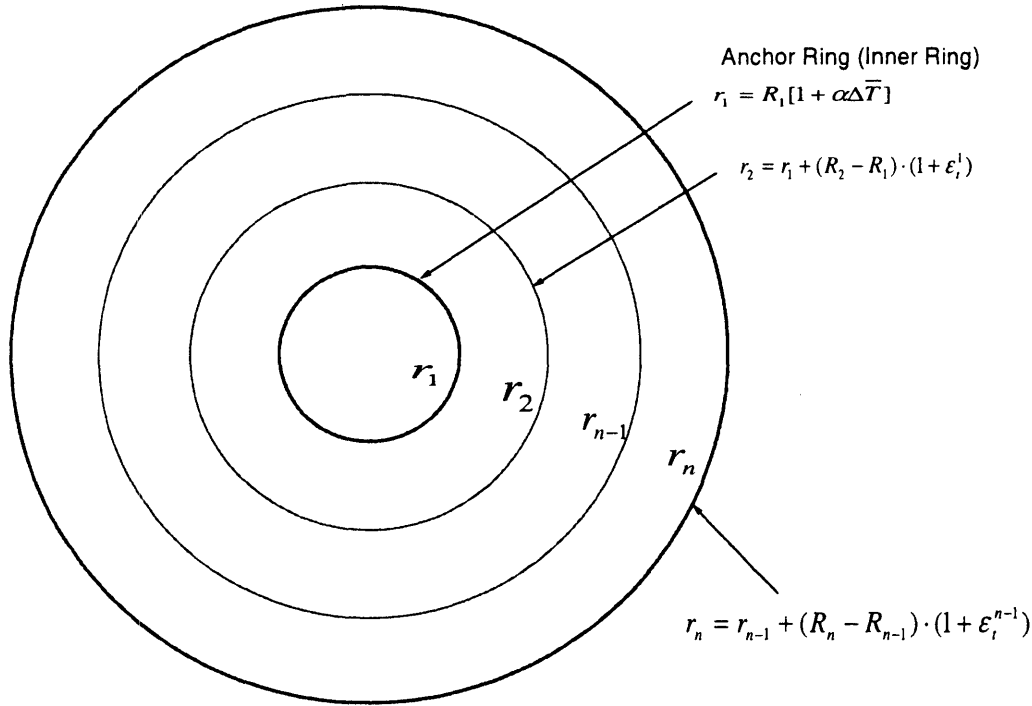


Figure 3.1 Fuel Anchor Ring and Radial Dimensions

3.2 The Sintered I&E Cooled Annular Fuel Relocation

3.2.1 Relocation Assessment

The temperature gradients in the fuel tend to cause cracks and these cracks promote fuel relocations that reduce the gap sizes. This process begins at BOL and quickly reaches equilibrium. [Oguma, 1983] The fuel cracks are mostly oriented in the radial direction. However, some circumferential cracking can occur, which tends to degrade the fuel thermal conductivity. For the solid fuel, the fuel relocation movement due to cracking is mainly outwardly since there is only one degree of freedom. However, for the annular fuel, the cracked fuel can move towards both inner and outer directions. Therefore, the relocation of annular fuel will result in reduction of gap sizes at both the inner and outer gaps if it happens. Since the I&E cooled annular fuel is cooled at both sides and the fuel temperature gradient is expected to be low, it is imperative to evaluate the fuel relocation.

The I&E cooled annular fuel has been analyzed using a finite element analysis code ANSYS and a typical radial temperature profile for I&E cooled annular fuel (73kW/m local power) was provided as an input. (Figure 3.2)

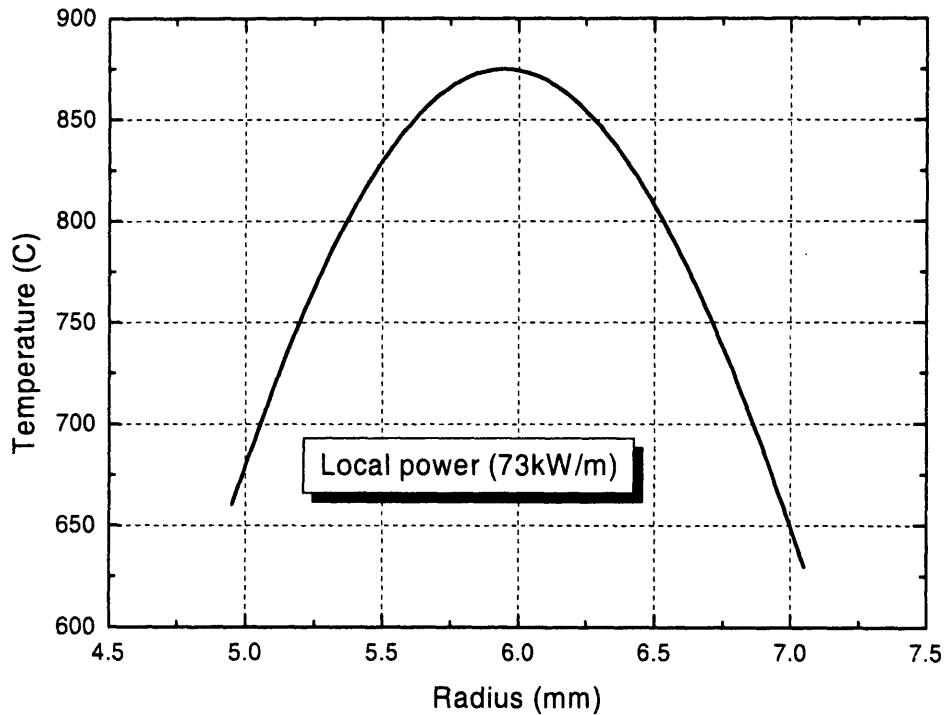


Figure 3.2 I&E Cooled Annular Fuel Radial Temperature Profile for ANSYS Analysis

The VonMises' stresses of the annular pellet are shown in Figure 3.3. It can be seen that the stresses are higher at both the inner and outer surfaces and are relatively low in the interior. For most of the fuel, the stress exceeds the fracture stress of UO_2 (100MPa) and fuel cracking is expected to occur and cracks will develop and propagate. Therefore, it is concluded that fuel relocation will take place, although to a lesser extent compared with solid fuel.

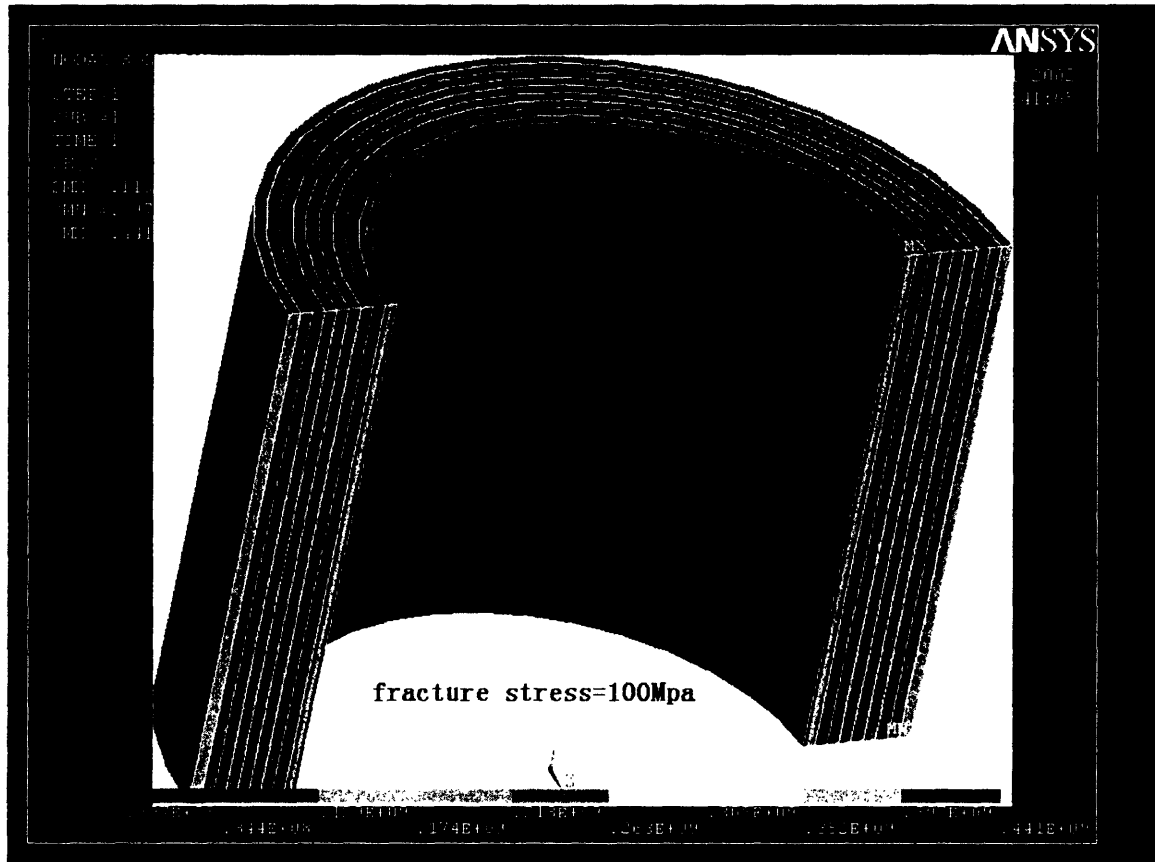


Figure 3.3 The VonMises' stresses of an annular fuel node

3.2.2 A Model for Annular Fuel Relocation

The relocation model in FRAPCON-3 is given as: [Lanning et al, 1997]

$$\Delta G / G(\%) = \begin{cases} 30 + 10 \cdot Fbu & \text{for LHGR} < 20 \text{ kW/m} \\ 28 + Pfactor + (12 + Pfactor) \cdot Fbu & \text{for LHGR} < 40 \text{ kW/m} \\ 32 + 18 \cdot Fbu & \text{for LHGR} > 40 \text{ kW/m} \end{cases} \quad (3-16)$$

where

$\Delta G / G =$ fuel-cladding gap size reduction in percent

$Fbu =$ Burnup/5.0, for Burnup < 5 GWd/MTU

$=$ 1.0 for Burnup $\geq$ 5 GWd/MTU

$Pfactor =$ (LHGR-20)/4, LHGR is linear power in kW/m

The solid fuel relocation model was derived from a representative LWR fuel rod at different linear powers and at different burnups. For sintered internally and externally cooled annular fuel, the relocation is less severe than solid fuel at the same linear power since the temperature gradient is lower. The relocation tends to reduce both the inner and outer gaps. For the reference solid fuel and the sintered annular fuel in a 13×13 lattice, the temperature gradient calculated and the gap volume are compared in Table 3.1.

Table 3.1 Temperature Gradients and Gap/Fuel Ratios of Solid and Annular Fuel

Solid fuel		Annular Fuel	
Linear Power	Temperature Gradient	Linear Power	Temperature Gradient
20kW/m	1018K/cm	55kW/m	1002K/cm
40kW/m	2517K/cm	120kW/m	2517K/cm
Fuel/Gap Volume Ratio: 25		Fuel/Gap Volume Ratio:17	

The solid fuel relocation model has been developed as a function of linear power and burnup. Since the fuel relocation is dictated by the fuel stress distribution, which is caused by the temperature gradient in the fuel, it is a reasonable approach to identify the linear power rates which achieve the same temperature gradients as those of the solid fuel and estimate the annular fuel relocation with solid fuel relocation model. Therefore, the fuel relocation model can be modified considering the equivalent linear powers and gap/ Fuel volume ratios as presented in Table 3.1. All the parameters should correspond to the equivalent linear power rates and are corrected with fuel/gap volume ratios for the solid and the annular fuel.

$$\Delta G / G(\%) = \begin{cases} 20 + 7 \cdot Fbu & \text{for LHGR} < 55\text{kW/m} \\ 19 + Pfactor + (8 + Pfactor) \cdot Fbu & \text{for LHGR} < 120\text{kW/m} \\ 22 + 12 \cdot Fbu & \text{for LHGR} > 120\text{kW/m} \end{cases} \quad (3-17)$$

where, $\Delta G / G =$ fuel-cladding gap reduction in percent

$Fbu =$ Burnup/5.0, for Burnup<5GWd/MTU

$=$ 1.0 for Burnup $\geq$ 5GWd/MTU

$Pfactor =$ (LHGR-55)/4, LHGR is linear power in kW/m

We assume the above correlations can be applied to calculate the reduction for both gap sizes. Therefore, the equivalent gap sizes for thermal calculations are as follows:

$$Gap_{inner}^{equiv} = Gap_{inner} (1 - \Delta G / G) \quad (3-18)$$

$$Gap_{outer}^{equiv} = Gap_{outer} (1 - \Delta G / G) \quad (3-19)$$

where Gap_{inner}^{equiv} , Gap_{outer}^{equiv} are the equivalent gap sizes as results of fuel relocations and Gap_{inner} , Gap_{outer} are the gap sizes derived by fuel and cladding dimensional changes.

3.3 Fuel-Cladding Mechanical Interaction Model for the Sintered Annular Fuel

3.3.1 Overview

The fuel cladding mechanical interaction plays an important role in fuel performance: (1) the cladding stress and strain due to fuel cladding interaction may cause cladding failure and subsequent release of fission products, (2) the fuel cladding gap sizes and fuel cladding interfacial pressures (when gaps are closed) have significant impact on the heat transfer coefficient of the fuel-cladding interface, (3) for the internally and externally cooled annular fuel, the gap heat transfer resistance imbalance due to fuel-cladding mechanical interaction is one of the major constraints for fuel development. Therefore, an accurate prediction of the fuel cladding mechanical interaction by the performance model is critical for identifying the fuel safety margin and optimum fuel design.

Fuel-cladding mechanical interaction is a time dependent process which involves major fuel and cladding behavioral parameters. For the solid fuel, the fuel experiences instantaneous thermal expansion and the fuel-cladding gap size is quickly reduced at the Beginning of Life (BOL). During the first cycle, the difference of cladding inside and outside pressure generally

causes the cladding creep down towards the fuel. Meanwhile, the irradiation induced densification causes fuel shrinkage during the first few thousand hours of reactor operation. The combined effects of cladding creepdown and densification dictate the gap size. The relocation effect considered for thermal calculations is not considered in the fuel-cladding mechanical interaction due to its “soft” nature. At higher burnup, with the production and release of fission gases (mainly Kr and Xe), the rod internal pressure becomes higher, which tends to retard cladding creep down. The swelling induced by fission products comes into play and the fuel cladding gaps will be closed during a long fuel operation cycle. When the fuel-cladding gap is closed, the fuel expansion driven by swelling leads to the build-up of fuel cladding interfacial pressure, which may eventually cause cladding failure at high burnup.

The fuel cladding mechanical interactions of the annular fuel are even more complicated because there are two fuel-cladding gaps and the interaction between these two gaps with the fuel are more complex and difficult to predict. Despite the complexity of development of gap sizes and fuel-cladding contact conditions, for annular fuel cladding interactions, in principle three physical situations can be envisioned:

1) Free standing cladding regime:

At this regime, both gaps are open and cladding stress and strain are purely dictated by the rod internal pressure and the external coolant pressure.

2) Single gap closure regime:

At this regime, one gap is closed while the other gap remains open. The claddings are treated as free standing claddings but the fuel will be shuffled towards the side where the gap is still open.

3) Fuel-cladding full contact regime:

At this regime, both gaps are closed and the interfacial pressures are built up due to swelling.

A fuel cladding mechanical interaction model for internally and externally cooled annular fuel has been developed based on the FRACAS model, which is a pellet cladding interaction

analysis model [Bohn, 1977]. The FRACAS model has been widely used in the industry since it was developed and a simplified version of FRACAS is used in the FRAPCON-3 code.

For the annular fuel cladding mechanical interaction model, it is assumed that when full contact between the fuel and cladding has been made (two gaps are closed), the stress-induced deformation of the fuel can be neglected and the fuel is treated as a rigid pellet. It is also assumed that only small cladding strains would occur and the cylindrical shape of the cladding is retained. Therefore, the model performs small deformation analysis, which includes the following effects:

- Fuel thermal expansion, swelling, and densification
- Cladding thermal expansion, creep and plasticity.
- Fission gas release and external coolant pressure.

3.3.2 Free Standing Cladding Regime

At the free standing cladding regime, both the inner and the outer fuel-cladding gaps are open. The gap sizes can be calculated by taking into account of fuel and cladding dimensional changes. The stress and strain can be calculated considering rod internal pressure and external coolant pressure, as shown in Figure 3.4.

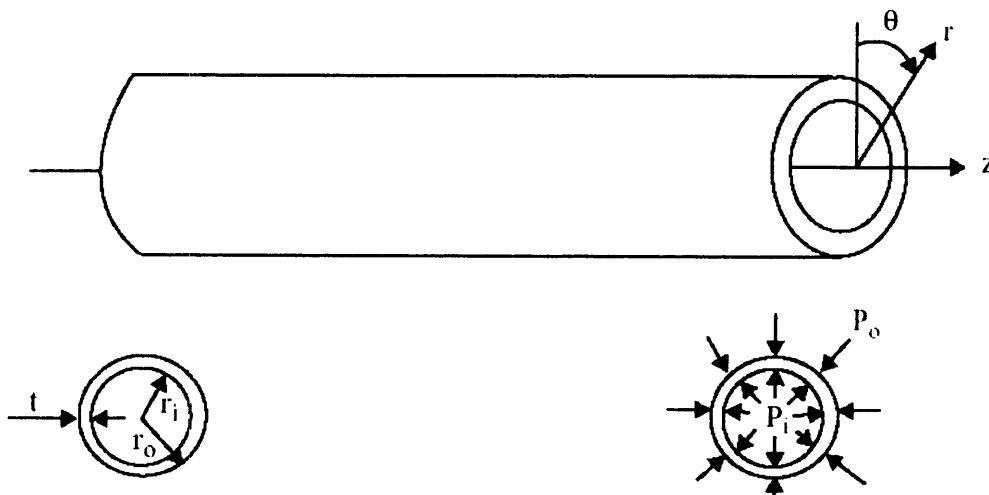


Figure 3.4 Free Standing Cladding with Internal and External Pressure

For the outer cladding, the hoop, axial and radial stresses are calculated as:

$$\sigma_{\theta}^o = \frac{r_i^o P_{\text{int } \text{ermal}} - r_o^o P_{\text{coolant}}}{t_{\text{outer}}} \quad (3-20)$$

$$\sigma_z^o = \frac{[(r_i^o)^2 - (r_i^i)^2] P_{\text{int } \text{ermal}} - [(r_o^o)^2 - (r_o^i)^2] P_{\text{coolant}}}{(r_o^o)^2 - (r_i^o)^2} \quad (3-21)$$

$$\sigma_r^o \approx \frac{P_{\text{inner}} + P_{\text{outer}}}{2} \approx 0 \quad (3-22)$$

where superscripts o, i, refer to outer and inner cladding respectively; subscripts o, i, refer to outer and inner cladding surface respectively; P_{internal} and P_{coolant} refer to rod internal pressure and external coolant pressure respectively, and t_{outer} is the thickness of the outer cladding. The radial stress is negligible compared with hoop and axial stress.

For the inner cladding, the stresses are expressed as:

$$\sigma_{\theta}^i = \frac{r_o^i P_{\text{coolant}} - r_i^i P_{\text{int } \text{ermal}}}{t_{\text{inner}}} \quad (3-23)$$

$$\sigma_z^i = \frac{[(r_i^o)^2 - (r_i^i)^2] P_{\text{int } \text{ermal}} - [(r_o^o)^2 - (r_o^i)^2] P_{\text{coolant}}}{(r_i^i)^2 - (r_o^i)^2} \quad (3-24)$$

$$\sigma_r^i \approx 0 \quad (3-25)$$

Cladding strains for both the inner and outer claddings are calculated using the following equations:

$$\varepsilon_{\theta}^o = \frac{1}{E}(\sigma_{\theta}^o - \nu\sigma_z^o) + \varepsilon_{\theta}^{P(o)} + d\varepsilon_{\theta}^{P(o)} + \int_{T_0}^T \alpha_{\theta}^o dT \quad (3-26)$$

$$\varepsilon_z^o = \frac{1}{E}(\sigma_z^o - \nu\sigma_{\theta}^o) + \varepsilon_z^{P(o)} + d\varepsilon_z^{P(o)} + \int_{T_0}^T \alpha_z^o dT \quad (3-27)$$

$$\varepsilon_r^o = -\frac{\nu}{E}(\sigma_{\theta}^o + \sigma_z^o) + \varepsilon_r^{P(o)} + d\varepsilon_r^{P(o)} + \int_{T_0}^T \alpha_r^o dT \quad (3-28)$$

$$\varepsilon_{\theta}^i = \frac{1}{E}(\sigma_{\theta}^i - \nu\sigma_z^i) + \varepsilon_{\theta}^{P(i)} + d\varepsilon_{\theta}^{P(i)} + \int_{T_0}^T \alpha_{\theta}^i dT \quad (3-29)$$

$$\varepsilon_z^i = \frac{1}{E}(\sigma_z^i - \nu\sigma_{\theta}^i) + \varepsilon_z^{P(i)} + d\varepsilon_z^{P(i)} + \int_{T_0}^T \alpha_z^i dT \quad (3-30)$$

$$\varepsilon_r^i = -\frac{\nu}{E}(\sigma_{\theta}^i + \sigma_z^i) + \varepsilon_r^{P(i)} + d\varepsilon_r^{P(i)} + \int_{T_0}^T \alpha_r^i dT \quad (3-31)$$

where superscripts o, i denote outer and inner cladding surface respectively, superscript P denotes plastic strain and subscript r, θ , z denote radial, hoop and axial directions. E is Young's modulus and ν is Poisson's ratio. σ and ε are stress and strain of the cladding.

The plastic strain calculated for each time step will be stored and provides as input for the next time step.

3.3.3 Single Gap Closure Regime

As predicted by the fuel dimensional change model, the thermal expansion of the internally and externally cooled annular fuel causes the fuel to expand outwardly. Hence the gap size imbalance will evolve and one gap tends to close earlier than the other. When such a condition occurs, the fuel is in contact with cladding at one side while leaving a gap at the other side, as can be seen in Figure 3.5.

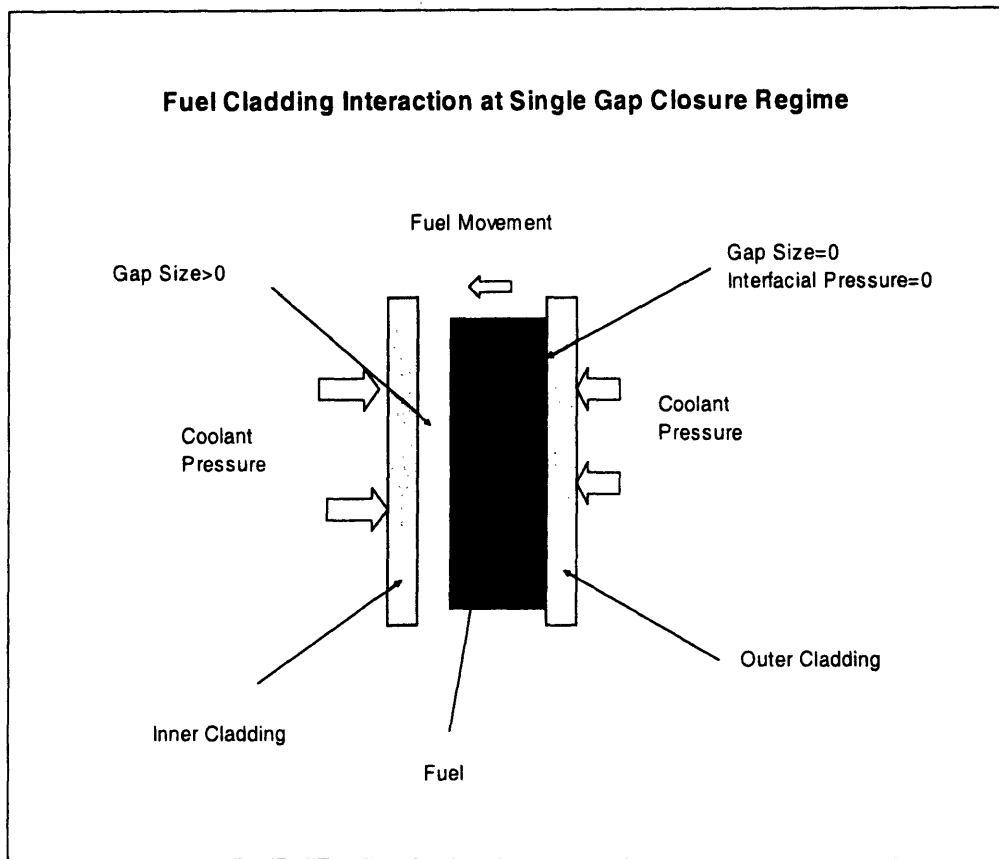


Figure 3.5 Fuel Cladding Interactions at Single Gap Closure Regime

It has been stated in the previous section (Chap 3.2) that fuel cracks will develop and propagate at the beginning of fuel operation. It is expected that at the single gap closure regime, the cracked fuel has freedom to move towards the opposite side where gap is still open. Therefore, although the contact has been made at one side of the fuel cladding surface, the contact is very “soft” and no fuel cladding interfacial pressure builds-up is perceived. At this regime, the cladding stresses and strains are calculated as the free standing cladding case with external coolant pressure and internal rod pressure. However, the fuel dimensions are changed due to shuffling of the cracked fuel. If the outer fuel-clad gap closes while the inner fuel-clad gap is open, the fuel outer surface dimension is assigned as the same of the cladding inner surface while the dimensional increments due to cladding creepdown, fuel densification, fuel swelling are assigned to the fuel inner surface to cause the inner radius shrinkage, and vice versa.

3.3.4 Fuel-cladding Full Contact Regime

At higher burnup, the fuel and claddings will eventually become in full contact when both fuel-clad gaps are closed due to fuel swelling and cladding creepdown. After fuel cladding full contact, both the fuel and claddings will be constrained by each other, hence the claddings can no longer be treated as free standing claddings and the fuel has no freedom to shuffle its position as in the single gap closure regime. The interfacial pressures at both the inner and outer interfaces could build up and contribute to the cladding stresses and strains. The mechanism for fuel cladding interactions at full contact regime is complicated and is subject to many variables, such as the friction between the fuel and claddings, the fuel shape due to stress induced hourglass effects, fuel node blockage, etc. However, a simplified approach has been attempted to simulate the fuel and cladding interactions based on the following assumptions: (1)The fuel maintains its cylindrical shape and no hourglass effect is taken into account; (2)A full lock-up of the fuel and cladding is assumed; (3)The interfacial pressure at the inner and outer interfaces are equal. The diagram for fuel-cladding full contact regime is shown in Figure 3.6.

It is illustrated in Figure 3.6 that when fuel cladding full contact is made, the interfacial pressures can rise at both the inner and outer interface. Considering the self constraining capabilities of the fuel, it is reasonable to assume that both inner and outer interfacial pressures are equal.

Due to fuel and cladding roughness, the fuel and cladding tend to be coupled and the cladding axial strain is found to be the same as the fuel axial strain. This is the so called “lock-up” mechanism, which is characterized by the inter-locking of the fuel and cladding surfaces, as shown in Figure 3.6.

If for the previous time step, the fuel cladding full contact has not been made, it is important to identify the exact point that full contact is made and find out the cladding and fuel strain at that point.

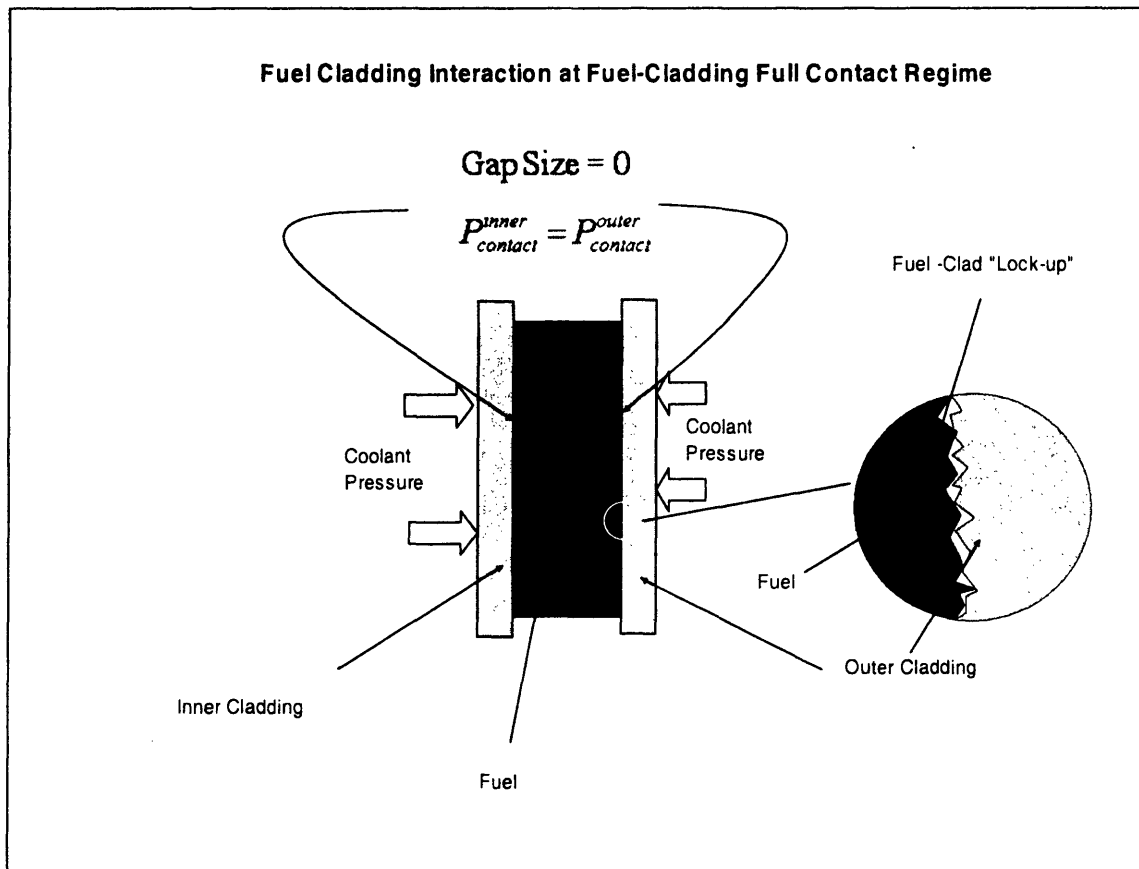


Figure 3.6 Fuel Cladding Interaction at Fuel-Cladding Full Contact Regime

The cladding stress and strain for both the inner and outer claddings after the fuel cladding full contact can be calculated based on the following:

- (1) The axial strains for both the inner and outer claddings are the same as the fuel axial strain;
- (2) The aggregated displacement of the cladding inner surfaces is equal to fuel total dimensional increments;
- (3) The interfacial pressures at the inner and outer interfaces are equal.

The solutions for cladding stresses and strains at full contact regime are described in the following section. (Note that in the following equations, superscript o and i refers to outer and inner cladding while subscript o and i denote cladding outer and inner surface)

Considering that interfacial pressures at inner and outer surfaces are equal,

$$P_{\text{int}}^i = P_{\text{int}}^o \quad (3-32)$$

$P_{\text{int}}^i, P_{\text{int}}^o$ are interfacial pressures at inner and outer cladding inner surfaces. The interfacial pressures can also be written as:

$$P_{\text{int}}^i = \frac{r_o^i P_o^i - \sigma_{\theta}^i t_i}{r_i^i}, \quad P_{\text{int}}^o = \frac{r_o^o P_o^o + \sigma_{\theta}^o t_o}{r_i^o} \quad (3-33)$$

Where P is pressure, r is radius and t is the cladding thickness, σ_{θ} is the hoop stress, and

$$P_o^i = P_o^o = P_{\text{coolant}}.$$

From (3-32) and (3-33), the following equation can be derived:

$$\sigma_{\theta}^i (t_i / r_i^i) + \sigma_{\theta}^o (t_o / r_i^o) = ((r_o^i / r_i^i) - (r_o^o / r_i^o)) P_{\text{coolant}} \quad (3-34)$$

When the two gaps are closed, radial displacement of the claddings should be consistent with fuel dimensional change.

$$\Delta r_i^o - \Delta r_i^i = u(r) \quad (3-35)$$

$u(r)$ is the fuel dimensional increment.

$$\Delta r_i^i = \bar{r}_o \varepsilon_{\theta}^o - \frac{t_o}{2} \varepsilon_r^o \quad (3-36)$$

$$\Delta r_i^o = \bar{r}_i \varepsilon_{\theta}^i + \frac{t_i}{2} \varepsilon_r^i \quad (3-37)$$

$\varepsilon_{\theta}, \varepsilon_r$ are the hoop and radial strain respectively, the aggregated displacement can be written as:

$$\bar{r}_o \varepsilon_{\theta}^o - \frac{t_o}{2} \varepsilon_r^o - \bar{r}_i \varepsilon_{\theta}^i - \frac{t_i}{2} \varepsilon_r^i = u(r) \quad (3-38)$$

The equations for cladding stress and strain are as follows:

$$\varepsilon_{\theta}^o = \frac{1}{E}(\sigma_{\theta}^o - \nu\sigma_z^o) + \varepsilon_{\theta}^{P(o)} + d\varepsilon_{\theta}^{P(o)} + \int_{T_0}^T \alpha_{\theta}^o dT \quad (3-39)$$

$$\varepsilon_z^o = \frac{1}{E}(\sigma_z^o - \nu\sigma_{\theta}^o) + \varepsilon_z^{P(o)} + d\varepsilon_z^{P(o)} + \int_{T_0}^T \alpha_z^o dT \quad (3-40)$$

$$\varepsilon_r^o = -\frac{\nu}{E}(\sigma_{\theta}^o + \sigma_z^o) + \varepsilon_r^{P(o)} + d\varepsilon_r^{P(o)} + \int_{T_0}^T \alpha_r^o dT \quad (3-41)$$

$$\varepsilon_{\theta}^i = \frac{1}{E}(\sigma_{\theta}^i - \nu\sigma_z^i) + \varepsilon_{\theta}^{P(i)} + d\varepsilon_{\theta}^{P(i)} + \int_{T_0}^T \alpha_{\theta}^i dT \quad (3-42)$$

$$\varepsilon_z^i = \frac{1}{E}(\sigma_z^i - \nu\sigma_{\theta}^i) + \varepsilon_z^{P(i)} + d\varepsilon_z^{P(i)} + \int_{T_0}^T \alpha_z^i dT \quad (3-43)$$

$$\varepsilon_r^i = -\frac{\nu}{E}(\sigma_{\theta}^i + \sigma_z^i) + \varepsilon_r^{P(i)} + d\varepsilon_r^{P(i)} + \int_{T_0}^T \alpha_r^i dT \quad (3-44)$$

Where

$\varepsilon_{\theta}, \varepsilon_r, \varepsilon_z$ =hoop, radial and axial strains respectively,

$\sigma_{\theta}, \sigma_r, \sigma_z$ = hoop, radial and axial stresses respectively,

$\alpha_{\theta}, \alpha_r, \alpha_z$ = thermal expansion coefficient at hoop, radial and axial direction

E is Young's modulus and ν is Poison's ratio and superscript p denotes plastic

$\varepsilon_z^o, \varepsilon_z^i$ are treated as known parameters, which are the same as the fuel axial strain.

The equations from (3-38) to (3-44) can be rearranged to yield the following matrix:

$$\begin{pmatrix} A(1) & 0 & C(1) & 0 & 0 & 0 & 0 & 0 \\ 0 & 0 & 0 & 0 & E(2) & F(2) & G(2) & H(2) \\ 0 & 0 & C(3) & D(3) & E(3) & 0 & 0 & 0 \\ 0 & 0 & C(4) & D(4) & 0 & 0 & 0 & 0 \\ 0 & 0 & C(5) & D(5) & 0 & F(5) & 0 & 0 \\ A(6) & B(6) & 0 & 0 & 0 & 0 & G(6) & 0 \\ A(7) & B(7) & 0 & 0 & 0 & 0 & 0 & 0 \\ A(8) & B(8) & 0 & 0 & 0 & 0 & 0 & H(8) \end{pmatrix} \begin{pmatrix} \sigma_{\theta}^i \\ \sigma_z^i \\ \sigma_{\theta}^o \\ \sigma_z^o \\ \varepsilon_{\theta}^o \\ \varepsilon_r^o \\ \varepsilon_{\theta}^i \\ \varepsilon_r^i \end{pmatrix} = \begin{pmatrix} I(1) \\ I(2) \\ I(3) \\ I(4) \\ I(5) \\ I(6) \\ I(7) \\ I(8) \end{pmatrix} \quad (3-45)$$

where,

$$A(1) = t_i; \quad C(1) = t_o;$$

$$E(2) = \bar{r}_o; \quad F(2) = -\frac{t_o}{2}; \quad G(2) = -\bar{r}_i; \quad H(2) = -\frac{t_i}{2}; \quad I(2) = u(r)$$

$$C(3) = -\frac{1}{E}; \quad D(3) = \frac{\nu}{E}; \quad E(3) = 1; \quad I(3) = \varepsilon_{\theta}^{P(o)} + d\varepsilon_{\theta}^{P(o)} + \int_{T_0}^T \alpha_{\theta}^o dT$$

$$C(4) = \frac{\nu}{E}; \quad D(4) = -\frac{1}{E}; \quad I(4) = -\varepsilon_z^o + \varepsilon_z^{P(o)} + d\varepsilon_z^{P(o)} + \int_{T_0}^T \alpha_z^o dT$$

$$C(5) = \frac{\nu}{E}; \quad D(5) = \frac{\nu}{E}; \quad F(5) = 1; \quad I(5) = \varepsilon_r^{P(o)} + d\varepsilon_r^{P(o)} + \int_{T_0}^T \alpha_r^o dT$$

$$A(6) = -\frac{1}{E}; \quad B(6) = \frac{\nu}{E}; \quad G(6) = 1; \quad I(6) = \varepsilon_{\theta}^{P(i)} + d\varepsilon_{\theta}^{P(i)} + \int_{T_0}^T \alpha_{\theta}^i dT$$

$$A(7) = \frac{\nu}{E}; \quad B(7) = -\frac{1}{E}; \quad I(7) = -\varepsilon_z^i + \varepsilon_z^{P(o)} + d\varepsilon_z^{P(o)} + \int_{T_0}^T \alpha_z^o dT$$

$$A(8) = \frac{\nu}{E}; \quad B(8) = \frac{\nu}{E}; \quad H(8) = 1; \quad I(8) = \varepsilon_r^{P(i)} + d\varepsilon_r^{P(i)} + \int_{T_0}^T \alpha_r^i dT$$

These equations are solved using Gaussian elimination.

The interfacial pressure at the fuel cladding interface is composed of two components: the fuel-clad contact pressure and the internal gas pressure. In some cases, the interfacial pressures are lower than the gas pressure in the fuel rod, and in such scenario, the above derivation is no longer valid. The lock-up will be released until the interfacial pressures equal the gas pressure. The solution scheme is shown in Figure 3.7.

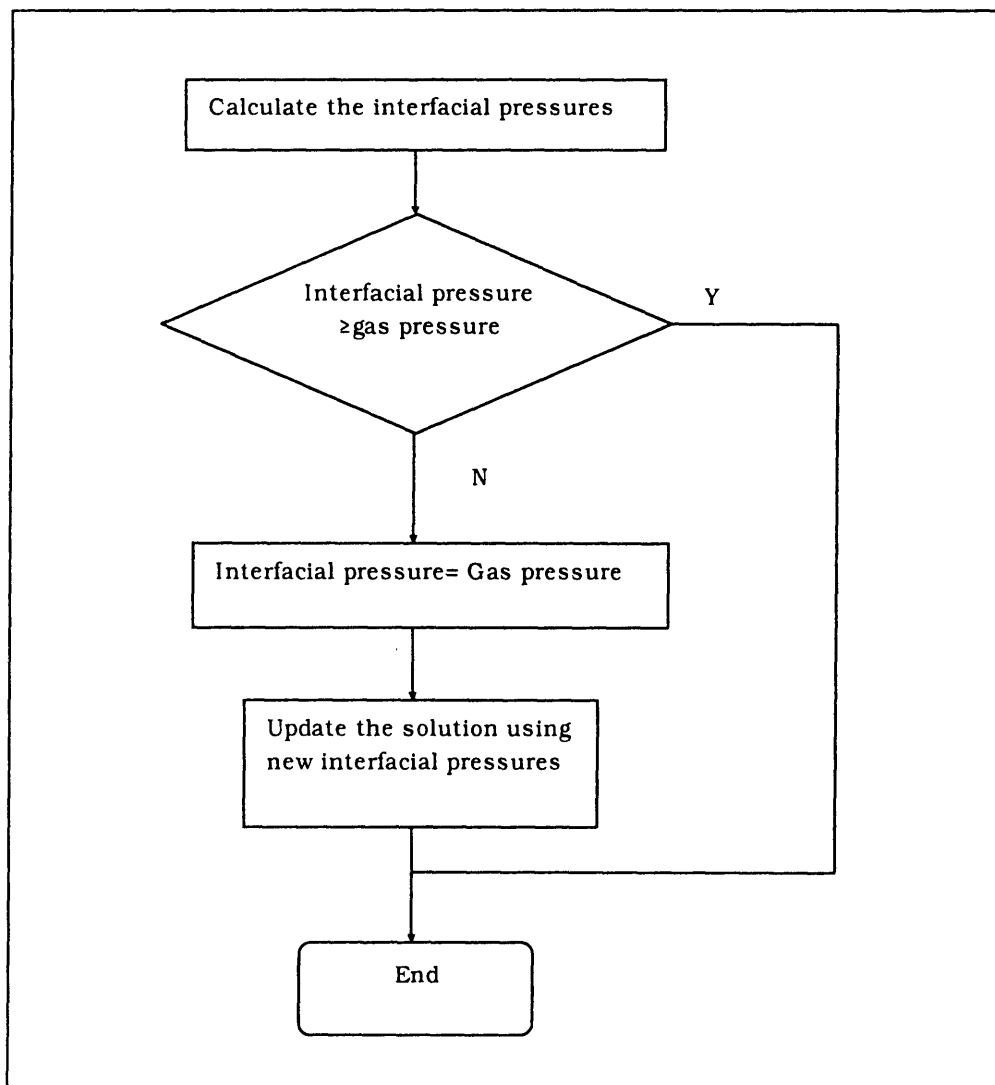


Figure 3.7 Fuel-Cladding Interaction Solutions at Higher Internal Gas Pressure

When interfacial pressure is lower than the gas pressure:

$$P_{\text{int}}^i = P_{\text{int}}^o = P_{\text{gas}} \quad (3-46)$$

And combined with equation (3-33)

$$\sigma_{\theta}^o = \frac{P_{\text{gas}} r_i^o - P_{\text{coolant}} r_o^o}{t_o}, \sigma_{\theta}^i = \frac{P_{\text{coolant}} r_o^i - P_{\text{gas}} r_i^o}{t_i}$$

$\sigma_{\theta}^o, \sigma_{\theta}^i$ can be solved.

Equation (3-43), neglecting $d\varepsilon$ in all equation and since σ_{θ}^i is known, can be written as:

$$\sigma_z^i = E\varepsilon_z^i + \nu\sigma_{\theta}^i - E(\varepsilon_z^{P(i)} + \int_{T_0}^T \alpha_z^i dT) \quad (3-47)$$

Since σ_z^i and σ_{θ}^i are known, (3-42) and (3-43) become:

$$\varepsilon_{\theta}^i = \frac{1}{E}(\sigma_{\theta}^i - \nu\sigma_z^i) + \varepsilon_{\theta}^{P(i)} + \int_{T_0}^T \alpha_{\theta}^i dT \quad (3-48)$$

$$\varepsilon_r^i = -\frac{\nu}{E}(\sigma_{\theta}^i + \sigma_z^i) + \varepsilon_r^{P(i)} + \int_{T_0}^T \alpha_r^i dT \quad (3-49)$$

Substitute these equations back to (3-35) to (3-37), yielding

$$\bar{r}_o \varepsilon_{\theta}^o - \frac{t_o}{2} \varepsilon_r^o = C; C = \bar{r}_i \varepsilon_{\theta}^i + \frac{t_i}{2} \varepsilon_r^i + u(r_i) \quad (3-50)$$

Combined with (3-39), (3-40) and (3-41), one obtains:

$$\sigma_z^o = \frac{(\bar{r}_o + \nu_o/2)\sigma_{\theta}^o - \bar{r}_o E(\varepsilon_{\theta}^{P(o)} + \int_{T_0}^T \alpha_{\theta}^o dT) - \frac{t_o}{2} E(\varepsilon_r^{P(o)} + \int_{T_0}^T \alpha_r^o dT) - EC}{\nu r_i^o} \quad (3-51)$$

Hence the following solutions are obtained:

$$\sigma_z^i = E\varepsilon_z^i + \nu\sigma_\theta^i - E(\varepsilon_z^{P(i)} + \int_{T_0}^T \alpha_z^i dT) \quad (3-52)$$

$$\sigma_z^o = \frac{(\bar{r}_o + \nu t_o / 2)\sigma_\theta^o - \bar{r}_o E(\varepsilon_\theta^{P(o)} + \int_{T_0}^T \alpha_\theta^o dT) - \frac{t_o}{2} E(\varepsilon_r^{P(o)} + \int_{T_0}^T \alpha_r^o dT) - EC}{\nu r_i^o} \quad (3-53)$$

$$\sigma_\theta^o = \frac{P_{gas} r_i^o - P_{coolant} r_o^o}{t_o} \quad (3-54)$$

$$\sigma_\theta^i = \frac{P_{coolant} r_o^i - P_{gas} r_i^o}{t_i} \quad (3-55)$$

The stresses and strains for inner and outer claddings can be calculated using the equations (3-39) to (3-44) and (3-52) to (3-55).

3.3.5 General Solutions for Annular Fuel Cladding Thermal and Mechanical Interactions

For each time step, fuel dimensions are calculated considering fuel thermal expansion, swelling and densification. Cladding dimensions are also calculated considering thermal, irradiation and mechanical effects. From the fuel and cladding dimensions and their relative positions, a judgment regarding gap closure for both the inner and outer gaps is made. If both gaps are open, the code turns to the free standing cladding solution, where cladding stresses and strains are calculated with prescribed internal gas pressure and external coolant pressure. If at least one of the gaps is closed, then the load (including temperature, pressure and resulting dimensional changes) is applied progressively with small increments. At each increment, the gap closure judgments are made. If both gaps are still open, the free standing cladding conditions are calculated and the next increment is subsequently added. If one of the gaps is closed, the fuel will be shuffled within the claddings in such a way that at the contact point, the interfacial pressure is taken to be zero and the dimensional increment is added to the opposite fuel surface. If both gaps are closed, the solution will be derived for the two claddings as described in Section 3.3.4. The diagram for the annular fuel thermal and mechanical interactions solutions is illustrated in Figure 3.8.

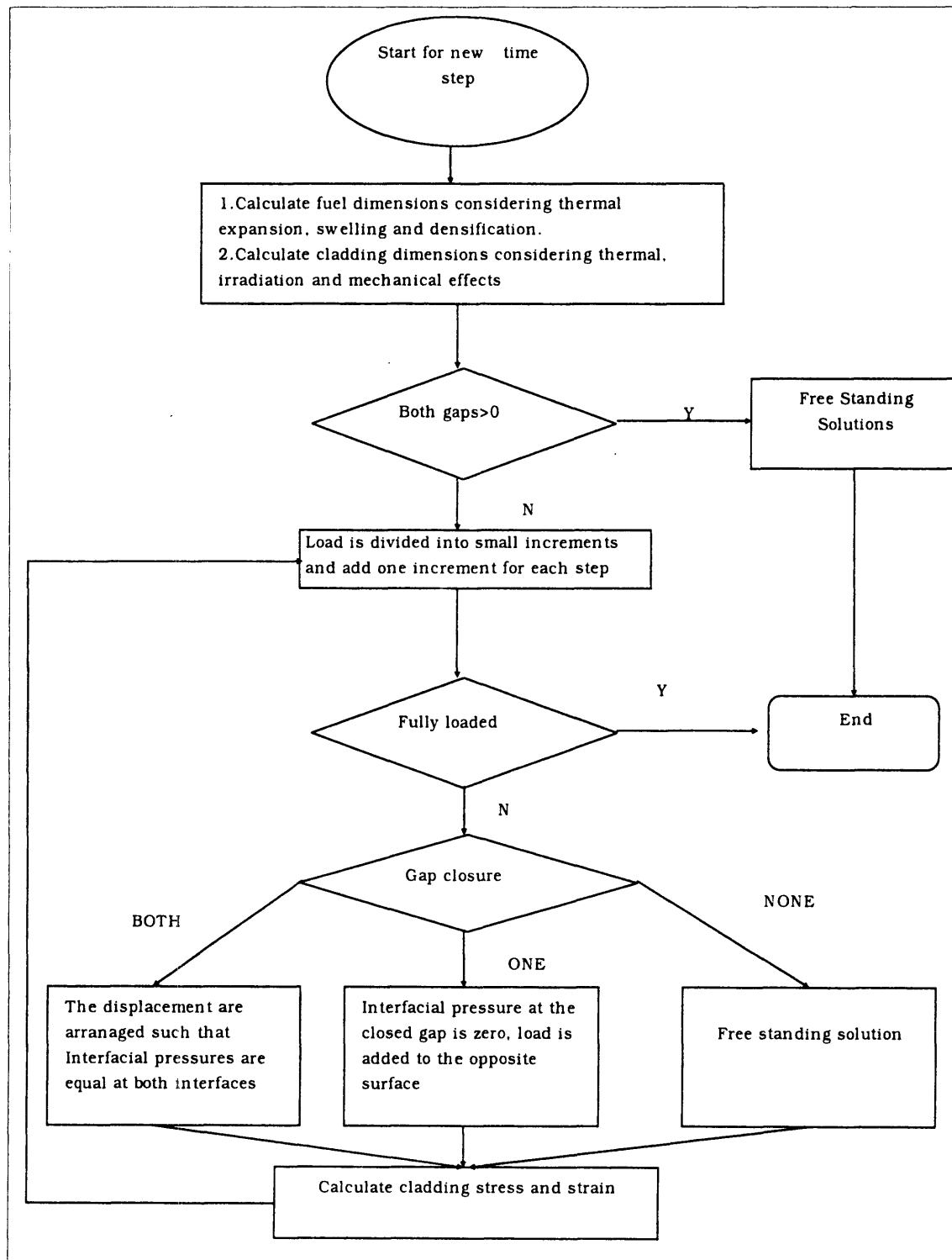


Figure 3.8 Diagrams for Annular Fuel Thermal/Mechanical Interaction Model

3.3.6 The Hard Contact Model

It is assumed in the annular fuel-clad mechanical interaction model that the fuel is cracked and can be nudged towards the opposite position while keeping the contact side interfacial pressure as zero. However, an extremely conservative approach assumes that the fuel is in hard contact with the cladding and the interfacial pressure can rise at the contact side when there is only one gap closed. The hard contact model is developed based on the following assumptions:

- (1) The pellet is strong enough: no deformation due to external load from the outer cladding creepdown but possibly due to swelling, densification, and thermal expansion; swelling only causes deformation of the pellet in the outward direction: the deformations of thermal expansion and densification are isotropic but the deformation due to swelling is isotropic without any cladding constraint, and become anisotropic if any constraint exists.
- (2) No-slip between the cladding and the pellet takes place: therefore, the cladding axial strain is the same as the fuel axial strain.
- (3) The total displacement of the cladding inner surfaces will be equal to the fuel total dimensional changes
- (4) The elastic correlations are still valid but the interfacial pressures and stresses are unknown parameters

The solutions for the hard contact model at the free standing regime and fuel cladding full contact regime are the same as what has been described in Section 3.3.3 and Section 3.3.4. For the single gap closure regime, the solutions are expressed as follows:

For inner gap opening and outer gap closing:

$$\sigma_{\theta}^i = r_i (P_o - P_g) / t_i \quad (3-56)$$

$$r_i \varepsilon_{\theta}^i + (t_i / 2) \varepsilon_r^i = \Delta r_f^i + \delta_{gap}^i \quad (3-57)$$

where $\Delta r_f^i, \delta_{gap}^i$ are the fuel inner radius change and as-fabricated inner gap, respectively.

Other notations are the same as previously described. Stresses and strains can be solved combined with equations (3-42) to (3-44)

For the outer gap open and the inner gap closed:

$$\sigma_{\theta}^o = r_o (P_o - P_g) / t_o \quad (3-58)$$

$$r_o \varepsilon_{\theta}^o + (t_o / 2) \varepsilon_r^o = \Delta r_f^o + \delta_{gap}^o$$

where $\Delta r_f^o, \delta_{gap}^o$ are the fuel outer change and as-fabricated outer gap, respectively. Other notations are the same as previously described. Stresses and strains can be solved combined with equations (3-39) to (3-41).

The calculated inner and outer interfacial pressures for the hard contact interaction model are illustrated in Figure 3.9. The hard contact model predicts very high interfacial pressures (up to ~80 MPa). However, this is not realistic since it assumes that the fuel is not cracked and the accumulation of such high interfacial pressures has not even been encountered in solid fuel where all the fuel expansions are driven toward the cladding. The annular cases used for calculation has a linear power of about 73 kW/m. The core average rod linear power is about 40-50kW/m. Hence temperature gradient in these rods will be smaller. According to Olander [1974], the fuel cracks could develop across the fuel with a temperature gradient of 40-60K/cm, which means at very low LHGR, the fuel cracking should take place. For annular fuel operated at 73kW/m, the radial temperature gradient is about 200K/cm. For annular fuel operated at 40-50kW/m, the temperature gradient is about 110-140K/cm, which is larger than 40-60K/cm. Therefore, it is over-conservative to apply the hard contact model in the evaluation of the internally and externally cooled annular fuel performance. Further investigations with sophisticated finite element analysis code are recommended to asses the fuel cracking and fuel-cladding interaction behavior.

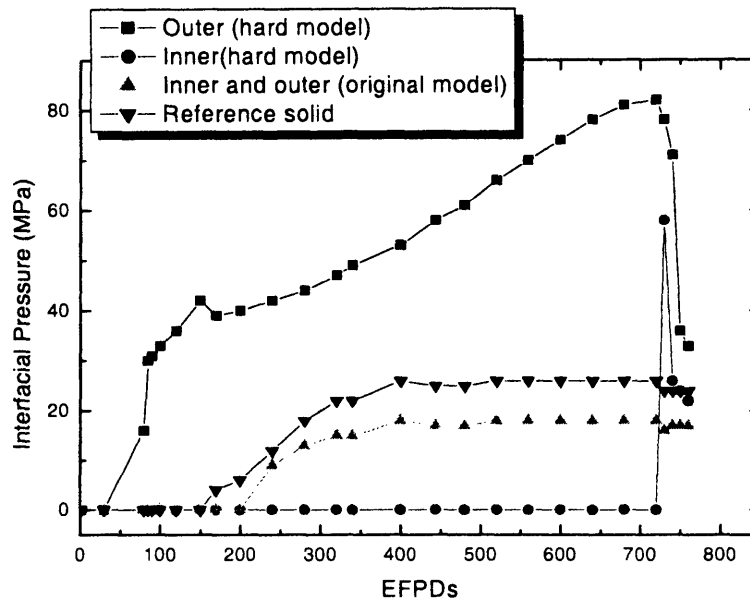


Figure 3.9 The Interfacial Pressures for Hard Contact Model

3.4 Fission Gas Release for Sintered I&E cooled Annular Fuel

3.4.1 Introduction

The behavior of the fission gas is one of the major concerns for fuel design due to its insolubility in the fuel matrix. A large fission gas release from the matrix can be hazardous since it pressurizes the fuel rod and may cause cladding failure during steady state at high burnup condition or during an accident. At high burnup, an excessive amount of fission gas could cause the reopening of the fuel cladding gap during an accident and initiate positive feedbacks to cause a cladding breach. There are two regimes for fission gas release. For fuel temperatures higher than 800 °C, the fission gas is released mainly by a diffusion process which is characterized by migration of fission gas atoms through a temperature gradient. For fuel temperature lower than 800 °C, the fission gas release is predominately from the recoil and knock-out processes. For internally and externally cooled annular fuel, the fuel temperatures are significantly lower than those of solid fuel and the temperatures are mostly below 800°C.

Therefore, recoil and knock-out are the main mechanisms for I&E cooled annular fuel fission gas release.

Since the fission products have a traveling distance of 10-15 μm , it is possible that a fission fragment be rejected out of the fuel when fissions occur within 10-15 μm of the fuel surface. Fission gas atoms are either directly ejected to the free volume when it is still an energetic fragment (recoil) or knocked out by interactions with other fission fragments in a collision cascade. (knock-out) [Olander, 1974]

Recoil and knock-out are proportional to the fuel surface area since the amount of fission gas atoms that can be released by recoil and knock-out exist mainly within 10-15 μm of all fuel surfaces.

3.4.2 High Burnup Effects for I&E Cooled Annular Fuel

The fuel temperatures and burnup are two major factors that affect the fission gas release. The annular fuel has the advantage of lower fuel temperatures than the solid fuel. However, in order to achieve the same fuel cycle length at higher power density as the solid fuel, it will be operated to a higher burnup. Therefore, high burnup fission gas release at low temperatures is an important issue for the internally and externally cooled annular fuel design.

It was found that at burnups exceeding 65 MWd/kgU, microstructure changes take place at the fuel pellet surface which is characterized by a high porosity and optically indefinable grain boundaries. The fission gas atoms migrate to the porosity zone and may account for accelerated fission gas release at low temperatures. [Meyer et al, 1978]. It is also expected that at high burnup, the fuel conductivity is decreased which results in higher fuel temperature and may result in a higher fission gas release. The damaged fuel matrix at high burnup also provides channels for fission gas bubbles to escape.

The internally and externally cooled annular fuel will have rim effects at both the inner and outer surfaces (Section 2.3). Hence the affected zone is expected to be larger. It is also obvious

that annular fuel has inner and outer surfaces for low temperature recoil and knockout. Therefore, the current fission gas release model should be revised to account for these effects.

3.4.3 High Burnup Athermal Fission Gas Release

At high temperatures, the fission gas release is dominated by the diffusion process which is not dependent on fuel geometry. Therefore, the internally and externally annular fuel high temperature fission gas release model will be the same as that used for solid UO₂ fuel. For low temperature athermal fission gas release, the modified Forsberg-Massih model is used in FRAPCON-3 [Forsberg and Massih, 1985] and has been expressed as a function of burnup:

$$F = 7 \times 10^{-5} \cdot B + C \quad (3-59)$$

where, F=the fission gas release fraction

B=burnup in MWd/kgU

C=0 for B<40 MWd/kgU

C=0.001*(B-40) for B>40MWd/kgU

The above model was derived for a typical LWR fuel. However, for annular fuel with inner and outer surfaces, the surface effect should be taken into account. The fuel surface to volume ratio β for the solid and the sintered annular fuel can be expressed as:

Solid fuel:

$$\beta_{solid} = \frac{2\pi r^2 + 2\pi r l}{\pi r^2 l} = 2\left(\frac{1}{l} + \frac{1}{r}\right) \quad (3-60)$$

where l =pellet length(m), r =pellet radius(m)

Annular fuel:

$$\beta_{annular} = \frac{2\pi(r_o^2 - r_i^2) + 2\pi(r_o + r_i)l}{\pi(r_o^2 - r_i^2)l} = 2\left(\frac{1}{l} + \frac{1}{r_o - r_i}\right) \quad (3-61)$$

where l =pellet length (m), r_o =annular pellet outer radius(m) and r_i =annular pellet inner radius(m)

The solid fuel values are taken from the reference solid fuel design APPENDIX A, where

$$l=13.4\text{mm}, \quad r=4.095\text{mm}, \quad \beta_{solid}=637.7 \text{ m}^{-1}.$$

Let ξ be the athermal surface release enhancement factor,

$$\xi = \beta_{annular} / \beta_{solid} = 2\left(\frac{1}{l} + \frac{1}{r_o - r_i}\right) / 637.7 \quad (3-62)$$

where l =annular fuel pellet length (m)

r_o =annular fuel pellet outer radius(m)

r_i =annular fuel pellet inner radius(m)

At low burnup, the fission gas release from the annular fuel rod can be derived by scaling the fission gas release from the solid fuel rod with the surface release enhancement factor ξ .

However, at high burnup, the cracks will develop, and the microstructure change will take place. The fuel surface exposed to the free volume will be enlarged significantly. Therefore, the effect of initial fuel surface difference between the annular fuel and the solid fuel will be negligible and the athermal fuel gas release behavior will be the same for both fuels.

Then the low temperature athermal fission gas release model for the annular fuel can be expressed as:

For Burnup $\leq 40\text{MWd/kgU}$

$$F = 7 \times 10^{-5} \cdot \xi \cdot \text{Burnup} \quad (3-63)$$

For Burnup $> 40\text{MWd/kgU}$

$$F = 7 \times 10^{-5} \cdot \text{Burnup} + 0.001 * (\text{Burnup} - 40) \quad (3-64)$$

where, F =the fission gas release fraction

Burnup =burnup in MWd/kgU

The athermal fission gas release from solid fuel and annular fuel are compared in Figure 3.10.

At lower burnups ($< 40\text{MWd/kgU}$), FGR from the annular fuel is higher than that from the

solid fuel due to larger fuel surface to fuel volume ratio. At high burnup (>50 MW/kgU), the high burnup effects such as fuel cracking and microstructure change dominate the athermal fission gas release and the difference between the two releases are negligible.

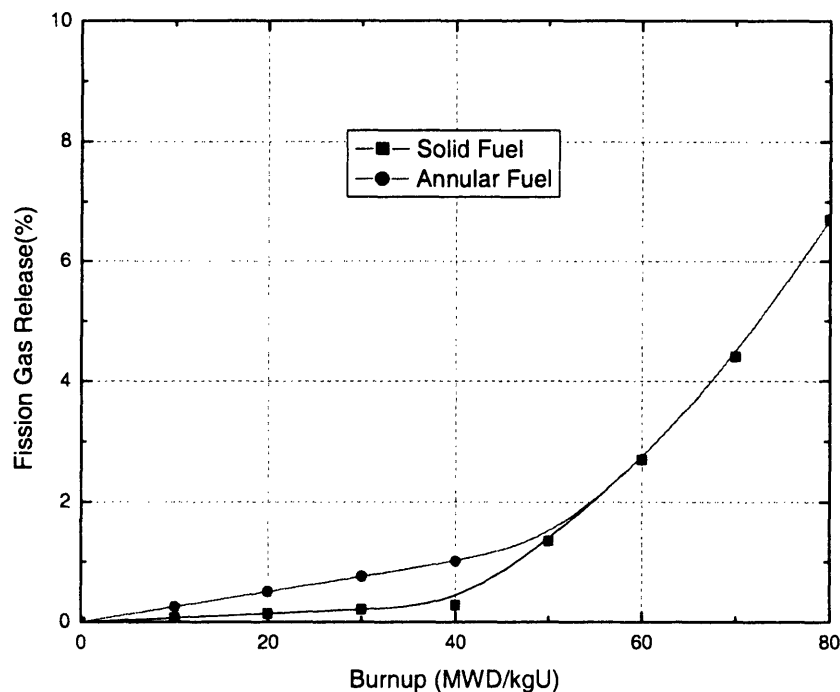


Figure 3.10 Comparison of Athermal FGR from Solid Fuel and Annular Fuel

3.5 Summary

The fuel dimensional changes due to thermal expansions, relocation, densification and swelling have been calculated for the sintered I&E cooled annular fuel. The annular fuel relocation is assessed and a relocation model for I&E cooled annular fuel has been developed. A fuel cladding interaction model has been developed and three regimes are identified for fuel-cladding interaction: the free standing cladding regime, the single closure regime and the fuel cladding full contact regime. The interaction mechanisms for each regime are discussed and solutions provided. A hard contact model with extreme assumption is also discussed. An athermal fission gas release model is developed for I&E cooled annular fuel considering the surface effects.

Chapter 4 The Sintered Annular LWR Fuel Design

4.1 Fuel Rod Design Criteria

The functions of fuel rods in a nuclear reactor include: (1) generating and transferring heat to the coolant, and (2) containing the fuel and fission product and providing barriers against fission product release. Therefore, it is required that fuel rods maintain their structural integrity and comply with the relevant regulatory requirements. For LWR fuel, the fuel design requirements as defined by Nuclear Regulatory Commission (NRC) are stated in 10CFR50 GDC 10 as: [NRC 10CFR50]

“The objectives of the fuel system safety review are to provide assurance that (a) the fuel system is not damaged as a result of normal operation and anticipated operational occurrences, (b) fuel system damage is never so severe as to prevent control rod insertion when it is required, (c) the number of fuel rod failures is not underestimated for postulated accidents, and (d) coolability is always maintained.

Several failure modes are identified for LWR fuel rod failures. Fuel rod failure should occur due to fracture induced by rod internal pressure and fuel-cladding mechanical interactions, which are assisted by irradiation induced stress corrosion cracking and stress corrosion induced embrittlement of the cladding. Fuel failure can be caused by creep rupture burst due to over-pressure or sustained stress induced by fuel cladding differential thermal expansion. Fuel rod could fail due to cladding corrosion, which thins the cladding and increase the cladding temperature. Fuel failure can also be explained by high external differential pressure. Cladding failures due to fatigue or excessive cladding growth are not likely for LWR fuel rods.

[Garkisch, 1997]

The design limits are specified as follows:

1. Rod internal pressure limits

- The rod internal pressure (the sum of gas pressure and contact pressure) is limited to a value below that which would cause the gas gap to increase due to outward cladding creep (ballooning) during steady-state operation which could affect coolant flow.
- To prevent accelerated fission gas release at high burnup by an increase in fuel temperature due to an increase in the temperature drop in the gap.
- To prevent the cladding rupture due to overpressure and to prevent the local heating of the cladding.

2. Clad temperature limits

- For steady-state: $T_{cl} < 750\text{F}(398.9\text{C})$
For transient: $T_{cl} < 800\text{F}(426.7\text{C})$
- To preclude a condition of accelerated oxidation

3. Fuel temperature limits

- $T_{fuel,max} < T_{fuel,melt}$, the fuel temperature should be below the melting point
- To preclude fuel volume expansion due to melting

4. Cladding oxidation limits

The cladding oxide thickness should be less than 17% of the cladding thickness during the transient conditions (and should be even less during normal operating conditions).

5. Clad strain limits

The total permanent uniform strain shall not exceed:

- 1% membrane strain (limiting)
- 2% bending strain
- 5% local strain

The cladding strain requirement is to limit slow rate strain damage with internal differential pressure and pellet cladding interaction.

6. Clad stress limit

$$- \quad \langle \sigma_{eff} \rangle < Y$$

where

$$\langle \sigma_{eff} \rangle = 2\sqrt{3}(P_i - P_o)K^2 \ln K / (K^2 - 1)^2$$

$$K = r_o / r_i$$

$\langle \rangle$: volume averaged, Y: yield stress of cladding

- To avoid excessive clad stress due to local power increase at rapid rate such that clad stress relaxation can not accommodate pellet thermal expansion

7. Grid cell force limit

- fretting wear < 10% of thickness of nominal cladding
- To maintain positive contact on fuel rod to minimize flow-induced fuel rod vibration

8. Plenum spring limit

- holddown force of spring > 6g
- To preclude the fuel movement during shipment , transportation, and refueling

Other limits include:

- Clad fatigue criterion
- Clad fluttering criterion
- Rod growth criterion
- Clad free standing criterion
- End plug weld stress criterion

In general, the cladding strain and oxidation are of most concerns. It is found that at high burnup (60MWd/kgU), the ductility of the cladding is reduced by a factor on the order of 5 and rupture is observed at less than 1% strain. Therefore, at high burnup, the cladding strain limit should be more stringent. [USNRC, 1997]

4.2 Comparison of the I&E Cooled Annular Performance and the Solid fuel

4.2.1 Input Parameters and Power Histories of the Reference Cases

The annular fuel performance code FRAPCON-ANNULAR has been utilized to perform analysis of the sintered I&E cooled annular fuel design. Comparisons have been made among the reference 13×13 I&E cooled annular fuel rods operating at 100% of current PWRs' power and at 150% overpower, a reference Westinghouse solid fuel rod operating at its rated power, and an actual commercial PWR rod operated to high burnup.

The internally and externally cooled annular fuels are assumed to have an average initial LHGR of 50.9kW/m (for 100% power) and 76.8 kW/m (for 150% power), which could achieve equal to or 50% higher core power density than the operating Westinghouse plant (APPENDIX A). It is assumed that the axial power shape is a chopped cosine curve with peak to average ratio of 1.3. The reference Westinghouse solid fuel rod operates with a LHGR of ~31kW/m. These rods are located at the hottest location of the core. The commercial PWR rod case used for comparison was an actual rod operated to extended burnup. It was a Babcock and Wilcox (B&W) 15 x 15-type PWR fuel rod operated at Oconee reactor for 1,553 effective full-power days (EFPD), which is identified as Oconee 5-Cycle rod 15309.[Newman, 1990] The major parameters of these cases are shown in Table 4.1 and the power histories of these cases are shown in Figure 4.1.

Other parameters such as fuel densities, coolant inlet temperatures, coolant pressures, cladding materials and etc. are assumed to be the same as the presented in APPENIX I. The cladding thickness and gap size of the I&E cooled annular fuel are assumed to be the same as the reference solid fuel at the initial stage and are subject to optimization in the following Chapter.

Table 4.1 Key Parameters for Different Cases

	Annular rod	Reference solid rod	Oconee rod 15309
Cladding outside diameter (mm)	Outer: 15.4	9.5	10.9
	Inner: 8.6		
Cladding thickness (mm)	Outer: 0.57	0.57	0.67
	Inner: 0.57		
Cladding material	Zircaloy-4	Zircaloy-4	Zircaloy-4
Fuel pellet diameter (mm)	Outer: 14.1	8.2	9.3
	Inner: 9.5		
Diametral gap size (mm)	Outer: 0.12	0.16	0.25
	Inner: 0.12		
Fuel pellet height(mm)	13.4	13.4	13.4
Fuel stack height(m)	3.66	3.66	3.58
Fuel volume(mm ³)	2.9×10^5	1.9×10^5	2.4×10^5
Plenum length(mm)	250	250	267
Plenum volume	2.1×10^4	1.4×10^4	1.9×10^4
External coolant pressure (MPa)	15.5	15.5	15.5
Initial helium pressure (MPa)	1.4	1.4	3.2
Initial rod average linear power(kW/m)	52 (100% Power)	30	19
	78 (150% Power)		
q' Peak to average ratio	1.3	1.3	1.3
EOL burnup (MWd/kgU)	57.3 (100% Power)	50.7	45.7
	85.8 (150% Power)		

The power histories for all four cases, given in Figure 4.1, should be decreasing with operating time to avoid high temperatures at the end of the fuel irradiation, after degradation of fuel thermal conductivity, and to thermal barrier due to oxide and crud accumulation on the claddings. All the fictitious cases are following the same trend and operate to the same Effective Full Power Days (EFPDs) to provide a better comparison of different scenarios. The Westinghouse reference has an average power that matches the real commercial rod (Oconee rod 15309 in B&W 846MWe reactor), as shown in Figure 4.1. and the fuel burnup for different cases are shown in Figure 4.2. Lower linear power of the Oconee rod is because of lower power density.

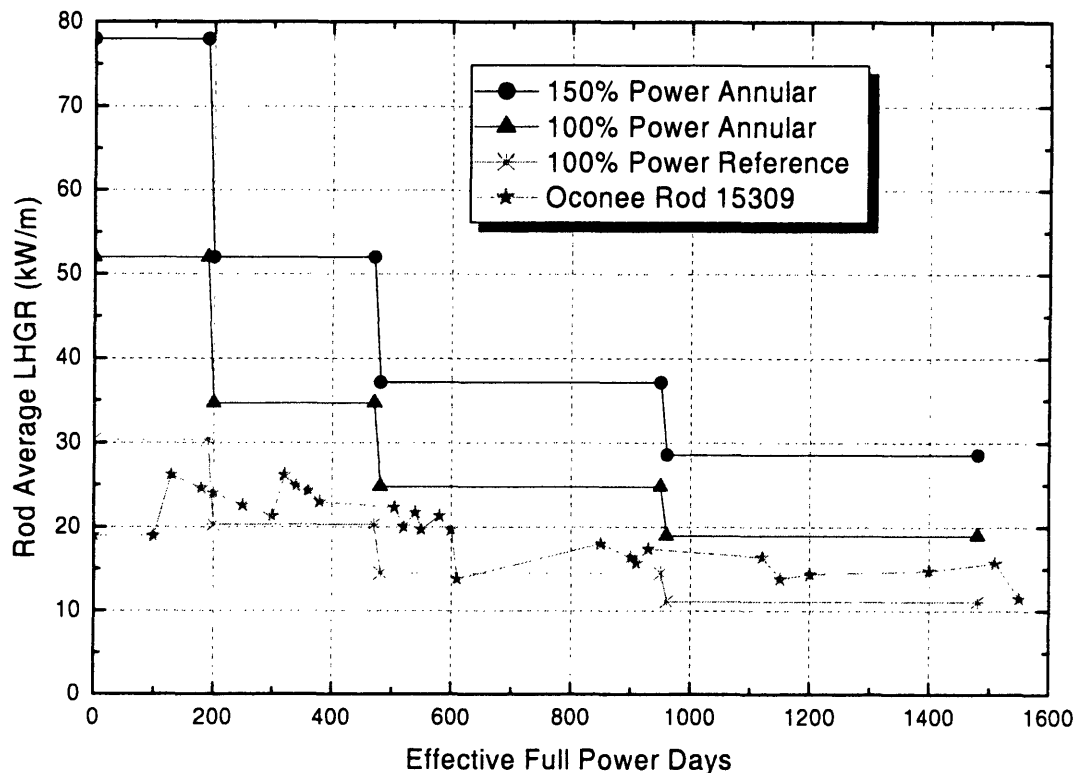


Figure 4.1 Input Power Histories for the Benchmark Cases

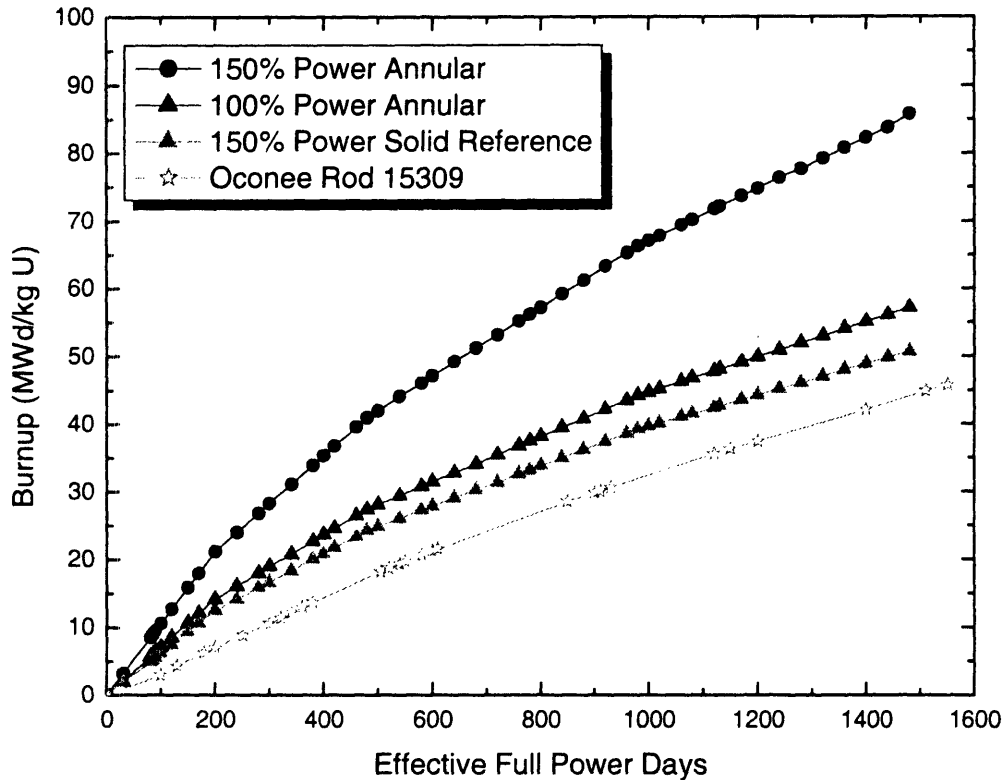


Figure 4.2 Fuel Burnup for Different Cases

4.2.2 Fuel Temperatures of Solid and Annular Fuel

The internally and externally cooled annular fuel temperatures are much lower than those of the solid fuel although the LHGR for the annular fuel is much higher (Figure 4.3 and Figure 4.4). For the annular fuel at BOL, the peak temperature location is skewed towards the inner side of the fuel due to instantaneous fuel thermal expansion that reduces the outer fuel-cladding gap. Hence gap conductance at the outer gap is larger than that at the inner gap. Even for 150% power case, the I&E cooled annular fuel has ~250K lower fuel average temperature than the solid fuel. For the 100% power case, the fuel average temperature is about 350-400K lower. The fuel temperature reduction arises from the fuel geometry which allows fuel cooling from both the inner side and outer side. Lower fuel temperatures are advantageous in reducing fission gas release and lowering fuel stored energy.

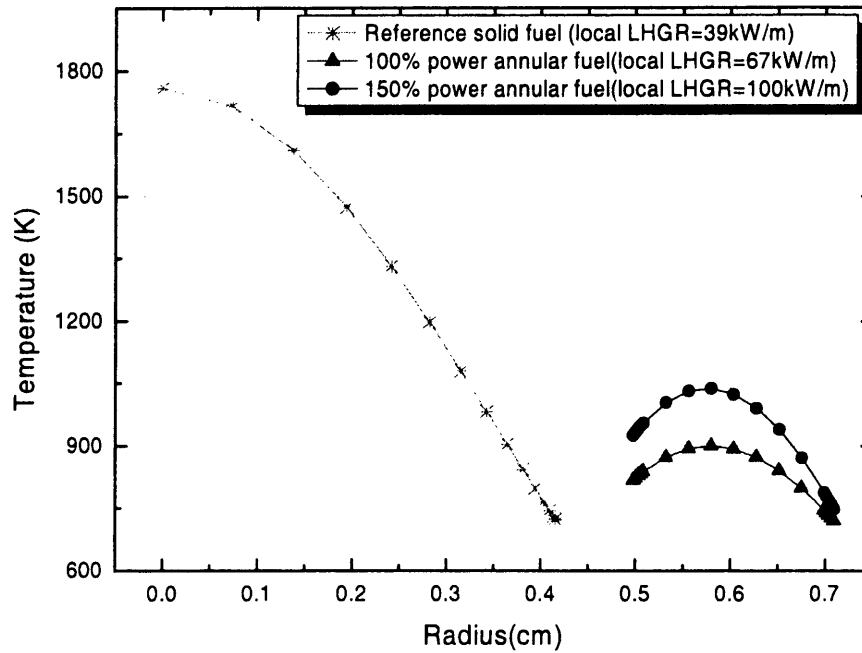


Figure 4.3 Peak Node Radial Power Profiles at BOL for Different Cases

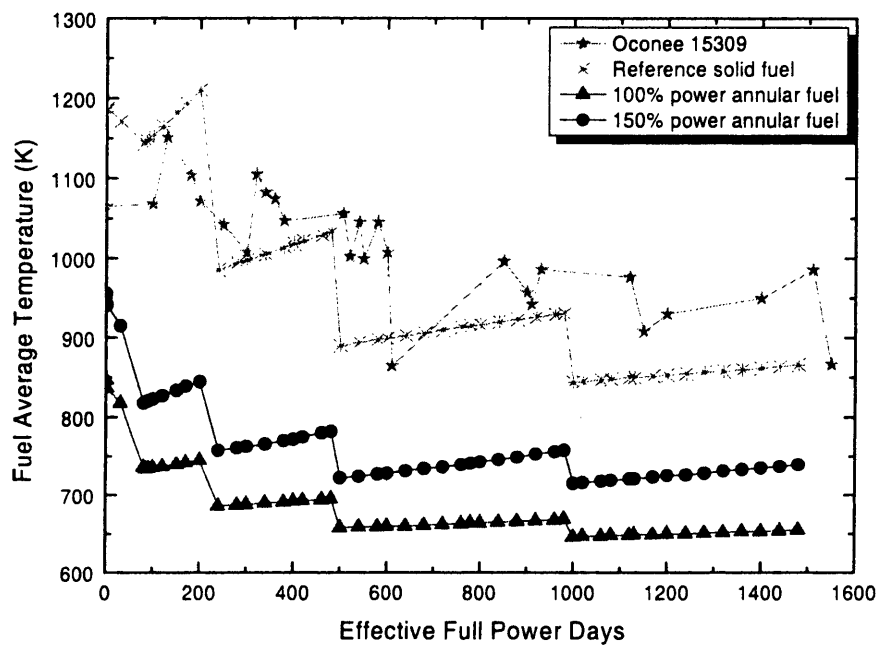


Figure 4.4 Fuel Average Temperatures for Different Cases

4.2.3 Fission Gas Release of Solid and Annular Fuel

The fission gas release of the internally and externally cooled annular fuel is dictated mainly by the following factors: (1) the reduction of fuel temperatures, (2) the increase of the free surfaces for low temperature fission gas release and (3) the extended burnup of the fuel. The reduction of the fuel temperatures for I&E cooled annular as seen in Section 4.2.2 has significant impact on fission gas release in that the fission gas release by the diffusion process is not initiated and fission gas release is mainly from the low temperature knock-out and recoil. At low burnup only the fission gas atoms generated within 10-15 microns of the fuel surface have a possibility to be released. The low temperature fission gas release at low burnup is very small compared with the release by diffusion. Therefore, although the I&E cooled annular fuel has enlarged fuel surface for athermal gas release, the fission gas release is low compared with the solid fuel which has a high temperature regime where fission gas is released by thermal diffusion. It can be found in Figure 4.5, for fuels irradiated below 600 days, that the solid fuel fission gas release is higher even compared with 150% power annular which has very high linear power (the rod average linear power reaches $\sim 78\text{kW/m}$ at BOL).

However, due to reduced fuel volume in the core, the internally and externally cooled annular fuels are irradiated to higher burnup in order to achieve the same core power density. For 100% power annular fuel, the EOL burnup is 57.2MWd/kgU compared with 50.7MWd/kgU for the reference solid fuel. The fission gas release from the I&E cooled annular fuel is lower or comparable with the reference solid fuel during its irradiation history, as illustrated in Figure 4.5.

Fission gas release from the 150% power I&E annular fuel is larger than the reference solid fuel case due to much higher burnup (rod average: $\sim 86\text{MWd/kgU}$) since the low temperature fission gas release start to take off at burnups higher than 45MWd/kgU due to microstructure changes of the fuel. However, the FGR from 150% annular fuel is still relatively low (less than 6%). The fission gas release spike for the reference solid fuel case at 200 EFPDs may come from enlarged gap size due to fuel densification and therefore higher fuel temperatures. The FGR gradually decreases due to reduction of fuel temperature as a result of gap closure and then increases due to temperature rise resulting from thermal conductivity degradation. For

Oconee 15309 rod, the fission gas release is the lowest (~1%) since it is irradiated to lowest burnup (45.7MWd/kgU) of the four cases and the low temperature fission gas release take-off effects at high burnup is not significant. For the calculated EOL rod internal gas pressure, the 100% power annular fuel has a lower rod internal pressure, slightly lower than the reference solid case(4.6 MPa vs. 4.9 MPa), while the 150% power annular fuel has a higher rod pressure (7.5MPa) than the reference case. Oconee rod 15309 has the highest EOL rod internal pressure of all these cases because: (1) its initial helium pressure is higher (3.2 MPa vs. 1.4 MPa), (2) the EOL fuel average temperature is higher, (3) although fission gas release is small; the fission gas is only a small fraction of total gases in the rod plenum.

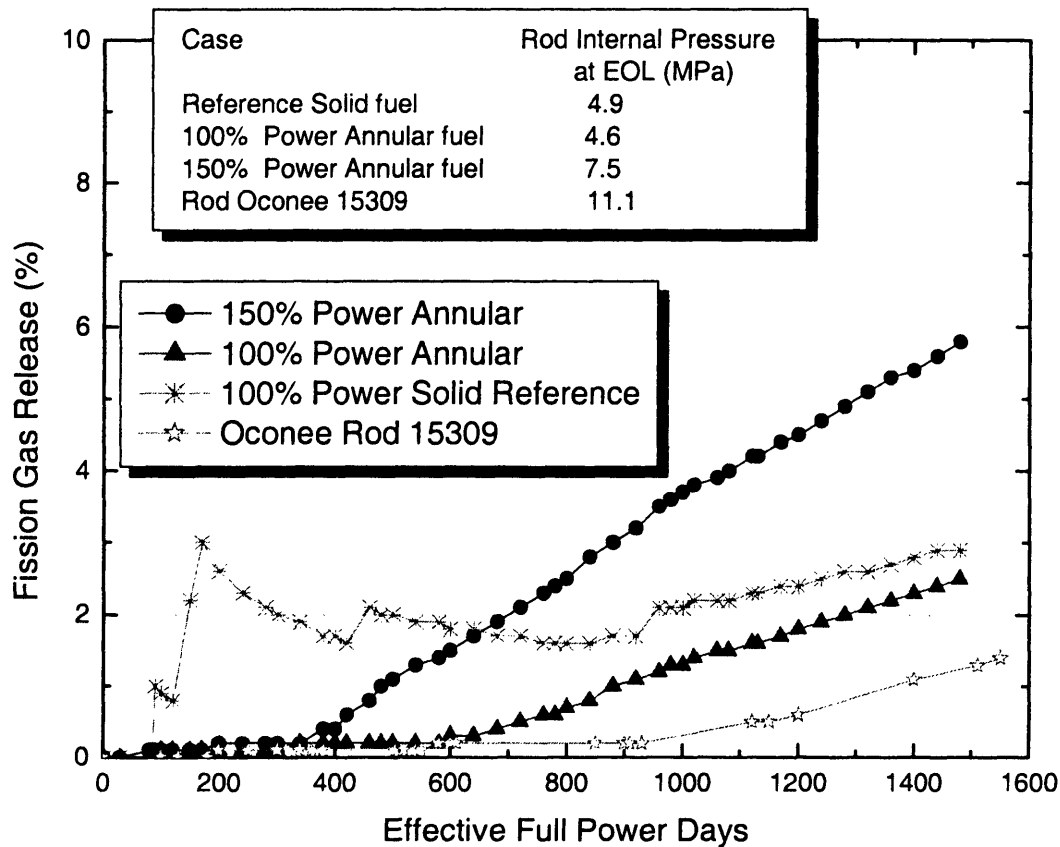


Figure 4.5 Fission Gas Releases and Rod Pressure at EOL

4.2.4 Cladding Performance of Solid and Annular Fuel

Zirconium based alloy cladding demonstrated good performance for commercial PWRs for the past several decades. The main concerns for cladding performance arise from corrosion and hydride problems as well as cladding strain. The corrosion problem is dependent on factors such as the coolant temperature, heat flux, irradiation, etc. The accumulated ZrO_2 layer due to corrosion could constitute a severe heat transfer barrier and can be detrimental during an accident including loss of coolant. The “spall-off” of ZrO_2 oxide layer could lead to reduction of cladding wall thickness and poses serious concerns for cladding damage. Hydrogen produced during the corrosion process can penetrate into the metal and form hydrides. The presence of brittle hydrides in the cladding reduces the cladding ductility and could also accelerate the waterside corrosion. In addition, plastic deformation of the cladding is not allowed to exceed 1% during the fuel operation according to the US Nuclear Regulatory Commission (USNRC, 2000) limit. The ZrO_2 thickness and hydrogen pick-up for different cases are shown in Figure 4.6 and Figure 4.7, respectively.

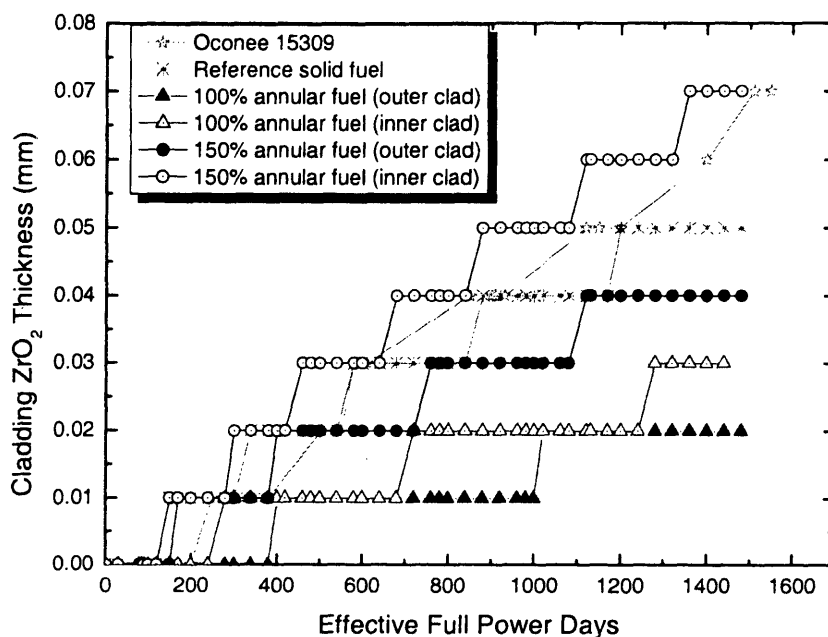


Figure 4.6 Cladding ZrO_2 Thickness Comparisons

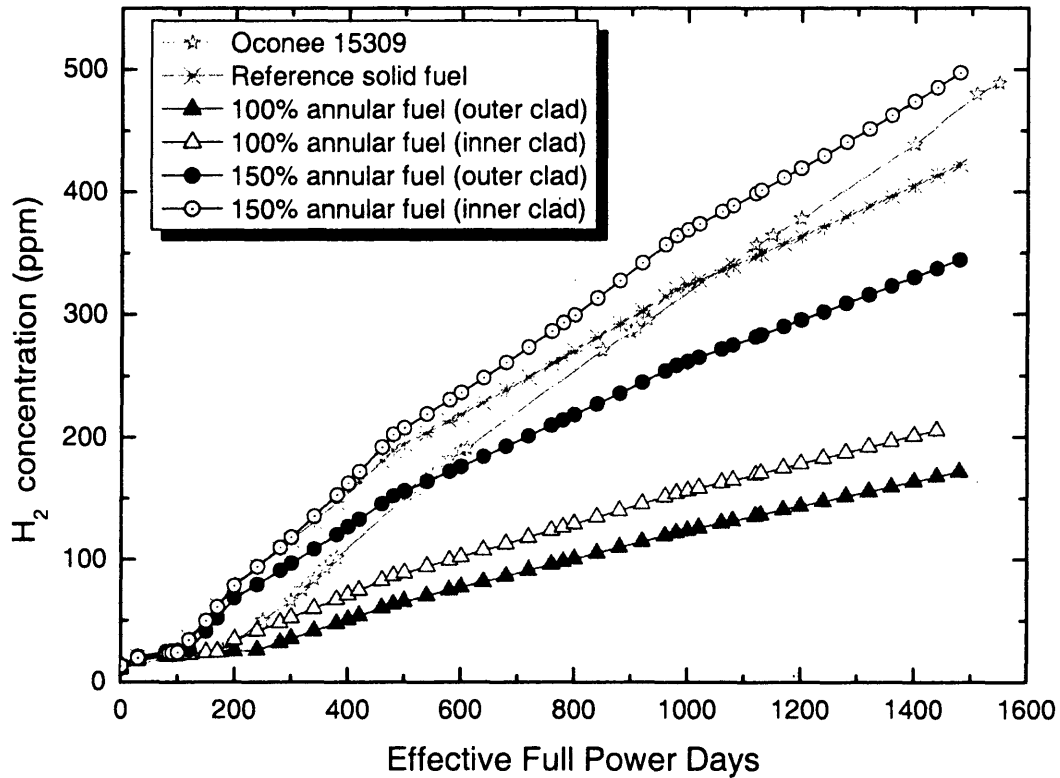


Figure 4.7 Cladding H₂ Concentration Comparisons

As shown in Figure 4.6 and Figure 4.7, except for the 150% power I&E cooled annular fuel inner cladding, the I&E cooled annular fuel has better cladding performance than the reference solid fuel. The annular fuel claddings have lower cladding temperatures due to smaller heat fluxes. While the irradiation time for the I&E cooled annular fuel is the same as that of the reference solid fuel. The oxide thickness and hydrogen concentration in the cladding are dictated by the cladding temperature and fast neutron flux. Therefore, the annular fuel tends to have smaller oxide layer thickness and lower hydrogen concentration. For the outer cladding of the 150% power annular case, the cladding temperature is higher than that of the reference solid fuel case due to higher linear power and gap conductance asymmetry which result in higher heat flux in the outer cladding. Therefore, it has higher cladding oxide thickness and hydrogen concentration. Oconee 15309 rod has higher oxide thickness and hydrogen

concentration than reference solid fuel because the higher q'' at the later stage of operation.

Cladding strains for different cases are compared in Figure 4.8. The outer claddings of the I&E cooled annular fuel follow the same trend as the reference solid fuel cladding, while the inner claddings follow an opposite trend. However, claddings for annular fuels experience a larger strain spike during the first hundred days of operation, which can be explained by larger cladding creepdown due to the presence of larger initial gap size (the total initial gap size is twice the solid fuel gap size). Although the strains do not exceed 1% strain limit, the larger strain should be avoided by adjusting the gap sizes and fuel positions relative to gaps.

Overall, the projected I&E cooled annular fuel cladding performance are comparable to that of the reference solid fuel. The strains of annular fuels appear to be larger but they are still within the design limit.

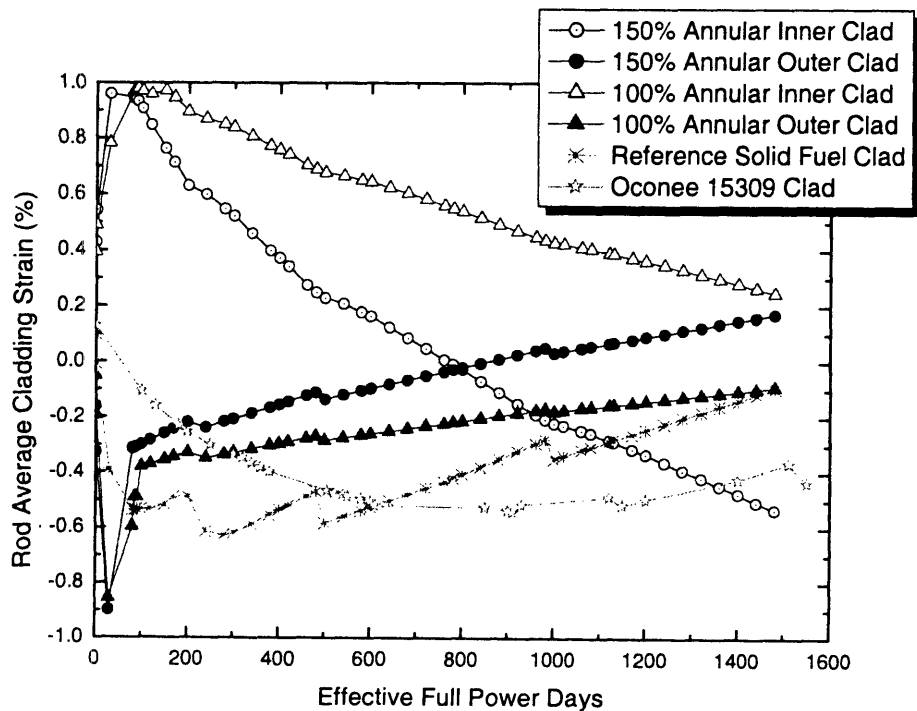


Figure 4.8 Cladding Strain Comparisons

4.3 Concern for Gap Conductance asymmetry

The interactions between the fuel and the claddings during irradiation result in evolution of both gap sizes with time. In Section 3.1, the thermal expansion of the annular fuel is found to be anisotropic and thermal expansion of the fuel tends to enlarge the fuel inner and outer radii. Therefore, a gap size asymmetry will develop and the gap conductances for the inner and outer gaps are expected to be different. Due to the imbalance of gap conductances, the heat flux would be larger where gap conductance is larger, which could lead to the impairment of Minimum Departure from Nucleate Boiling Ratio (MDNBR). For I&E cooled annular case with 100% power as presented in Section 4.2, the gap conductances as a function time are shown in Figure 4.9.

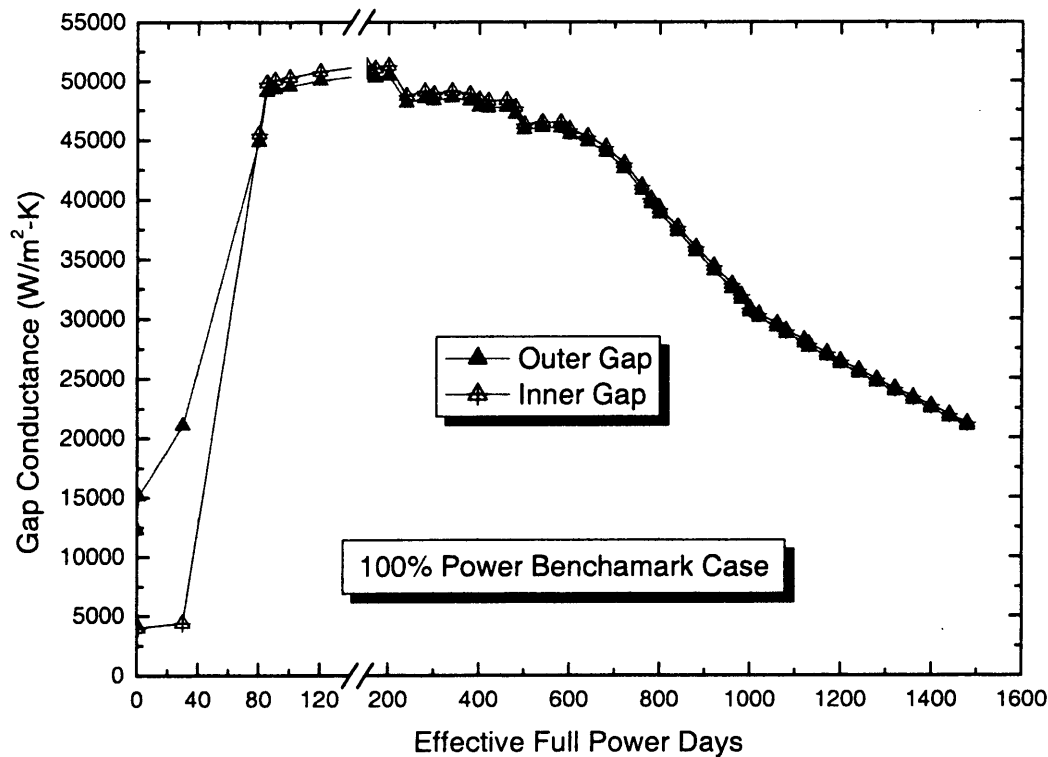


Figure 4.9 Gap Conductance Evolvement of the Annular Fuel with Time

As shown in Figure 4.9, the gap conductance of the outer gap is larger than that of the inner gap at initial stage of the fuel irradiation. This is because the instantaneous thermal expansion

causes fuel expansion towards the outer direction and as a result, the outer gap size is reduced while the inner gap is enlarged. The difference becomes the largest at the point when the outer gap is closed (~30days). After outer gap closure, the outer cladding nudges the fuel towards the inner direction, due to external coolant pressure, which reduces the inner gap size. Therefore, the gap conductance asymmetry becomes smaller (from ~30 days to ~80 days). When both the inner and outer gaps are closed, the interfacial pressures at both the inner and outer interfaces are equal, and so are the gap conductances.

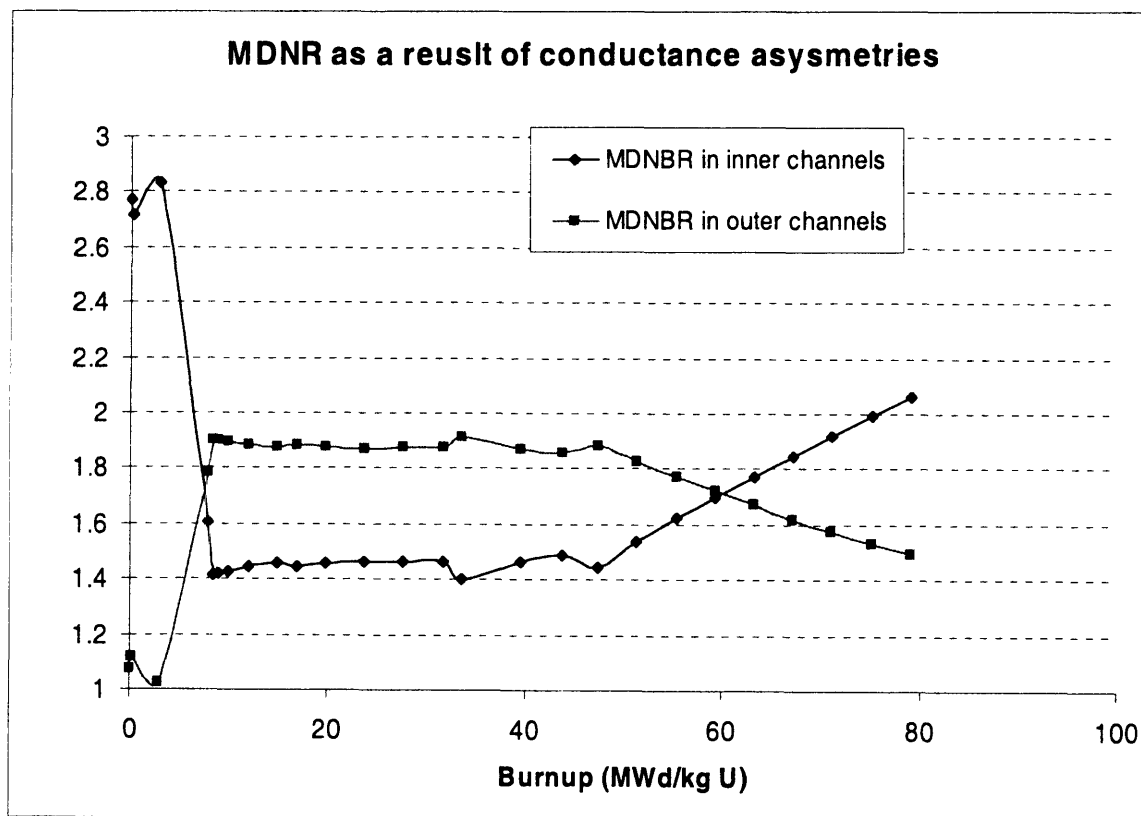


Figure 4.10 MDNBR due to Gap Conductance Asymmetries

The MDNBRs as a result of conductance asymmetries are illustrated in Figure 4.10. It is obvious that at the initial stage of the irradiation, the outer MDNBR is less than 1.3, which is not allowed for the fuel design. The small value of MDNBR at the fuel outer channel can be explained by the gap conductance asymmetry which results in a larger heat flux at the outer channel. Therefore, it is imperative to adjust the I&E cooled annular fuel design such that the

gap conductance asymmetry problem can be avoided.

Table 4.2 Sensitivity of DNBR to Gap Conductance Asymmetry (Feng, 2003)

Inner Gap conductance (w/m ² .k)	Outer Gap conductance (w/m ² .k)	Gap conductance Asymmetry*	MDNBR (inner)	MDNBR (outer)
6000	6000	1.00	1.373	1.928
5000	5500	1.10	1.591	1.791
5000	6000	1.20	1.776	1.67
7000	8000	1.14	1.592	1.789
7000	9000	1.29	1.792	1.649

*Ratio of outer gap conductance to inner gap conductance

The sensitivity of DNBR to gap conductance asymmetries is shown in Table 4.2. For larger gap conductances, the allowable gap conductance asymmetry is larger. Therefore, the MDNBR problem can be circumvented by reducing the gap conductance asymmetry and improving gap conductances.

4.4 Parametric Study of I&E Cooled Annular Fuel

4.4.1 Effect of As-Fabricated Gap Sizes

The gap conductance asymmetry due to anisotropic thermal expansion can be avoided by adjusting the as-fabricated gap sizes of the annular fuel. For the annular fuel cases shown in Section 4.2, the as-fabricated gap sizes for both the inner and outer gaps are the same as the reference LWR solid fuel gap size. A reasonable approach to tackle the problem is to reduce the inner gap size and enlarge outer gap size. In the meantime, it is important that the I&E cooled annular fuel core be able to achieve higher power density than the conventional operating LWRs. Hence, the design of as-fabricated gap sizes should assume that (1) the fuel content should not be reduced; (2) the fuel should be able to achieve 150% core power density than the reference LWR; and (3) the cladding performance should not be compromised by gap size adjustment.

The first attempt to adjust the gap sizes is to move the fuel towards the inner cladding while keeping fuel volume and cladding dimensions unchanged. The results are presented in Table 4.3 and Figure 4.11.

Table 4.3 The Effect of As-fabricated Gap Sizes on I&E Cooled Annular Performance

As-Fabricated Diametral Gap Size (inner)(mm)	As-Fabricated Diametral Gap size (outer)(mm)	Maximum Clad Strain (inner)(%)	Maximum Clad Strain (outer)(%)	Gap conductance (inner)(W/m ² -K)	Gap conductance (outer) (W/m ² -K)
0.124	0.124	0.980	-0.879	4280	21920
0.093	0.146	0.968	-0.822	5190	21990
0.062	0.167	0.963	-0.783	8900	21890
0.050	0.176	0.961	-0.767	8490	9610
0.031	0.189	0.960	-0.732	10580	5780

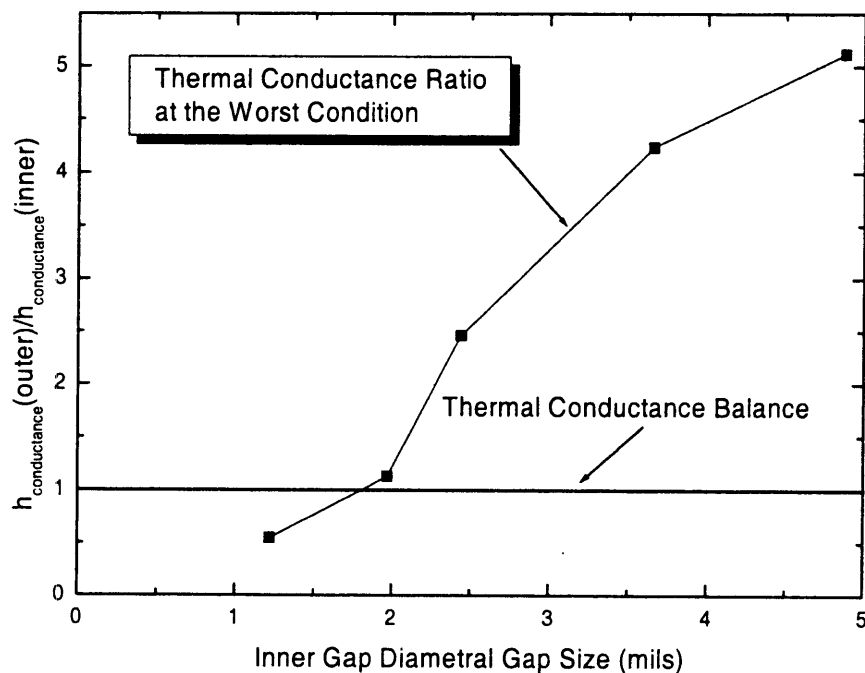


Figure 4.11 The Gap Size Effect on Thermal Conductance Balance

As shown in Figure 4.11, the gap conductance imbalance problem can be alleviated by reducing the inner gap size (which implies enlarging the outer gap size since the fuel volume is

constant). The heat imbalance and the strains presented in Table 4.3 and Figure 4.11 are the worst conditions during fuel operation. For all these cases, the gap conductance asymmetry is no longer a problem after both gaps are closed. The diametral gap size of the inner gap that is able to achieve the conductance balance is about 2 mils ($\sim 0.051\text{mm}$). However, such small inner gap size is difficult to achieve by commercial pellet fabrication facilities and the diametral gap size that could be attained is around 0.01mm (0.005mm radial) with refined punch/die designs and operational provisions for achieving a tighter dimensional tolerance of the sintered pellets. [Hao, 2004]. It is also noticed from Table 4.3 that reduction of the inner gap size has negligible effect on the maximum cladding strains since the maximum cladding strain occurs at the outer cladding, which is caused by the cladding creep down due to the external coolant pressure. This phenomenon can be explained by the larger total gap size of the annular fuel. The reduction of the inner gap size while maintaining fuel volume does not alter the total gap size significantly, hence the gap size adjustment has little impact on the cladding maximum strain.

The large cladding strain at initial stage of fuel operation due to the external pressure can be reduced by reducing the total gap thickness, which simultaneously enlarges the fuel volume. An analysis has been performed by keeping the inner diametral gap size as $\sim 0.1\text{mm}$ (achievable by the industry) and enlarging the fuel volume, i.e. reducing the total gap size. The results are presented in Table 4.4.

Table 4.4 The Effect of Enlarging Fuel Volume and Reducing Gap Size

As-Fabricated Diametral Gap Size (inner)(mm)	As-Fabricated Diametral Gap size (outer)(mm)	Fuel Volume increase	Maximum Inner Clad Strain (inner)(%)	Maximum Outer Clad Strain (outer)(%)	Gap conductance (inner) ($\text{W/m}^2\text{-K}$)	Gap conductance (outer) ($\text{W/m}^2\text{-K}$)
0.1	0.14	0.00%	0.971	-0.849	4890	22020
0.1	0.12	0.56%	0.925	-0.738	5100	21890
0.1	0.11	0.84%	0.904	-0.676	5540	21900
0.1	0.10	1.12%	0.884	-0.643	6050	21900

It is demonstrated in Table 4.4 that by reducing the total gap thickness the maximum cladding strains can be significantly reduced. However, the reduction of the total gap sizes is limited by

the tolerance that commercial fabrication facilities could achieve. It is also seen from Table 4.4 that the conductance asymmetry concerns still exist. In conclusion, the conductance asymmetry can be reduced by allowing smaller gap size in the inner gap and larger gap size in the outer gap. The maximum cladding strain can also be reduced by reducing the total gap size. However, it is unrealistic to achieve the design objective by adjusting the gap sizes alone since the achievable gap sizes are limited by the capabilities of production facilities.

4.4.2 Effect of Initial Rod Internal Gas Pressure

The fuel rod plenum is initially filled with helium gas to improve the fuel-cladding gap thermal conductance due to its high thermal conductivity of the helium compared to air and fission gases. The initial helium pressure also provides resistance against the cladding creep-down due to the external and internal pressure difference. The sensitivity of rod internal gas pressure has been evaluated assuming that the fuel inner and outer cold state gap sizes are equal. The impact of the initial gas pressure on the BOL gap conductance imbalance and EOL rod pressure can be seen in Figure 4.12.

The gap conductance imbalance problem is not affected by the initial rod helium gas pressure although higher gas pressure contributes to retarding the cladding creepdown process and improving the gap conductance. The gap conductance imbalance is mainly caused by the fuel dimensional expansion towards the outer cladding. Higher helium gas pressure only serves to delay the occurrence of gap closure but the imbalance still exists as long as the regime of gap closed the other is open exists. However, increasing the initial gap pressure can result in high EOL rod pressure, leading to fuel failure. Therefore, initial gas pressure should be maintained the same as the reference case.

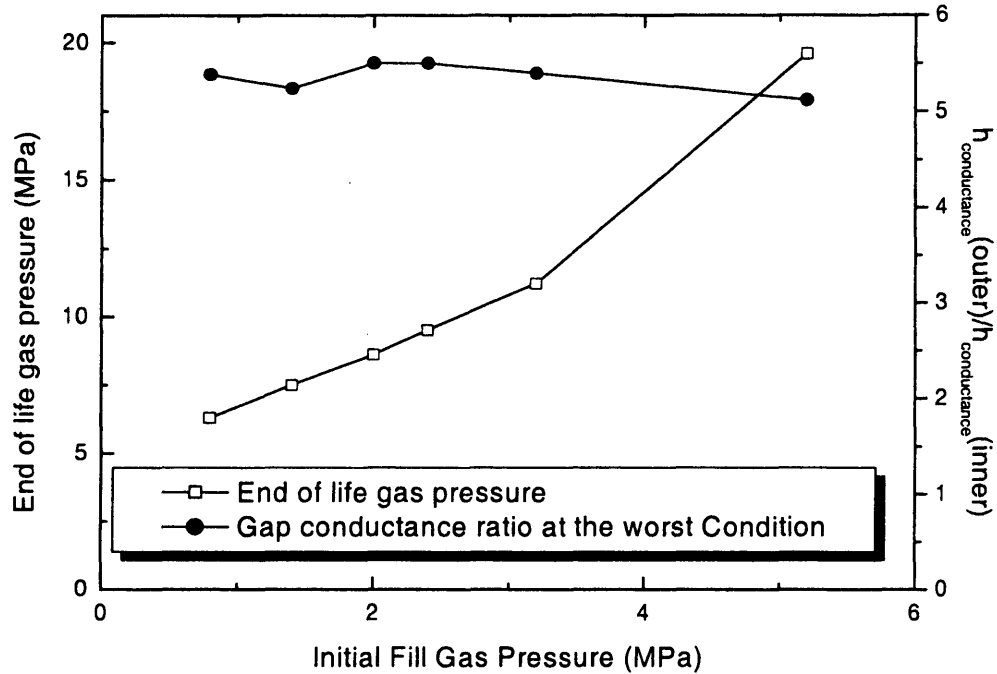


Figure 4.12 The Impact of Initial Filled Helium Gas Pressure

4.4.3 Effect of Cladding thickness

Equal thicknesses are used for both the inner and outer cladding in the reference annular fuel cases in Section 4.2. A sensitivity study has been performed by varying the cladding thicknesses in both the inner and outer claddings to identify the optimum cladding thickness that could reduce the gap conductance asymmetry and improve the cladding resistance against corrosion, and most importantly, reduce the cladding strain.

As a first step, the inner cladding thickness is unchanged and the outer cladding thickness is reduced while maintaining the same inner and outer gap sizes. As expected, the outer cladding maximum strain increases with the reduction of outer cladding thickness. However, the inner cladding maximum strain is reduced as the outer cladding thickness decreases. (Figure 4.13) The maximum strain of the inner cladding occurs when both gaps are closed and the cladding

strain is dominated by fuel swelling. Since the outer cladding is more deformable than the inner cladding due to the reduction of outer cladding thickness, the inner cladding strain is reduced as the strains caused by swelling are more easily directed towards the outer side.

Although decreasing the outer cladding thickness could mitigate the inner cladding strain by allowing some swelling-induced strains be shifted towards the outer side, the outer cladding oxidation and hydrogen content become of concern. At the end of life, the oxidation layer fraction of the outer cladding thickness increases from ~7% to ~9% and the H₂ concentration increases by ~20% at the outer cladding. (Figure 4.14) The increased oxide layer thickness fraction and H₂ fraction reduce the safety margin during steady state operation and can be hazardous in an accident.

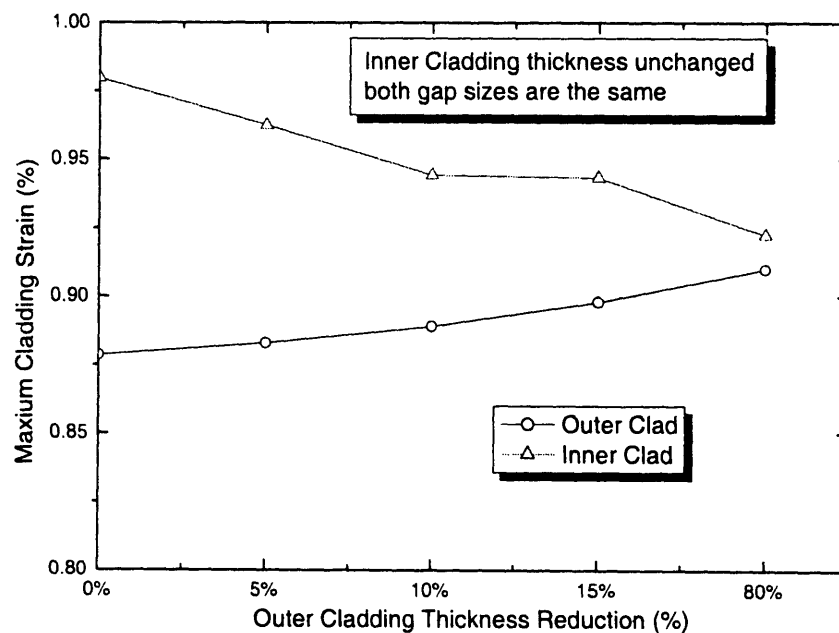


Figure 4.13 The Impact of Outer Cladding Thickness on Cladding Strains

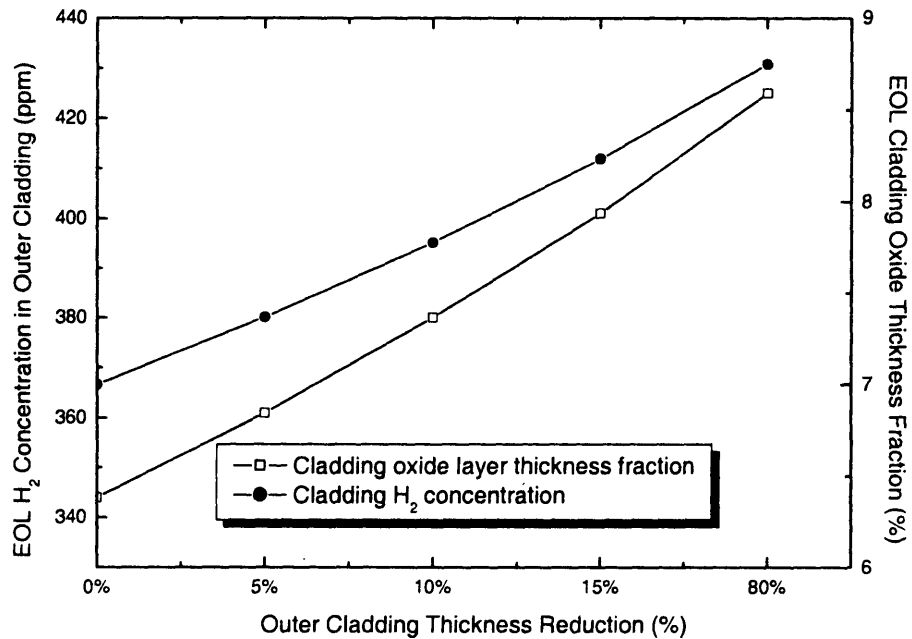


Figure 4.14 The Impact of Outer Cladding Thickness on Cladding Corrosion

The sensitivity to the inner cladding thickness was also analyzed in a similar approach as to outer cladding. The outer cladding thickness was maintained as well as both initial gap sizes. As seen in Figure 4.15, the inner cladding strain is increased while the outer cladding strain is reduced. The inner cladding strain increase is due to the reduced inner cladding thickness and the outer cladding strain reduction is due to swelling-induced strain shifted towards the inner cladding.

The reduction of inner cladding thickness allows cladding creep-down towards the fuel more quickly, therefore, reducing the gap conductance asymmetry. However, reduction of the gap conductance asymmetry is very small, and the reduction of cladding thickness could not eliminate conductance asymmetry occurrence due to the existence of one open gap and one closed gap. (Figure 4.16)

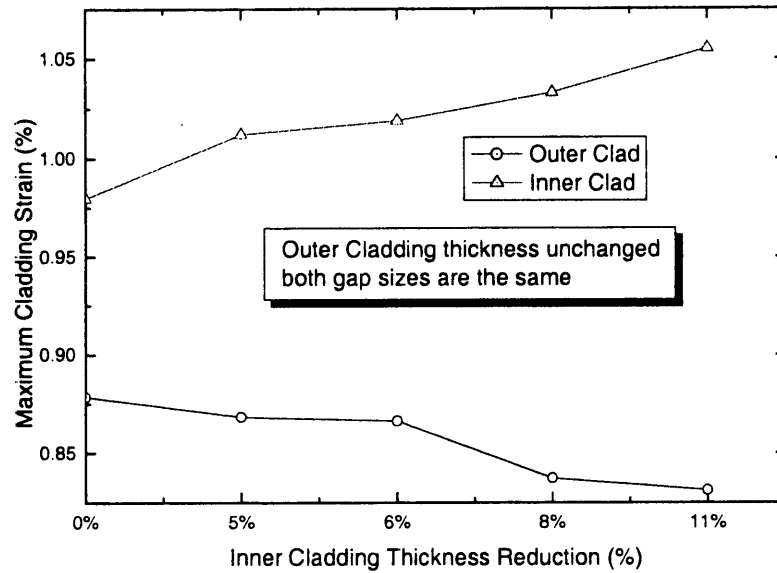


Figure 4.15 The Impact of Inner Cladding Thickness on Cladding Strains

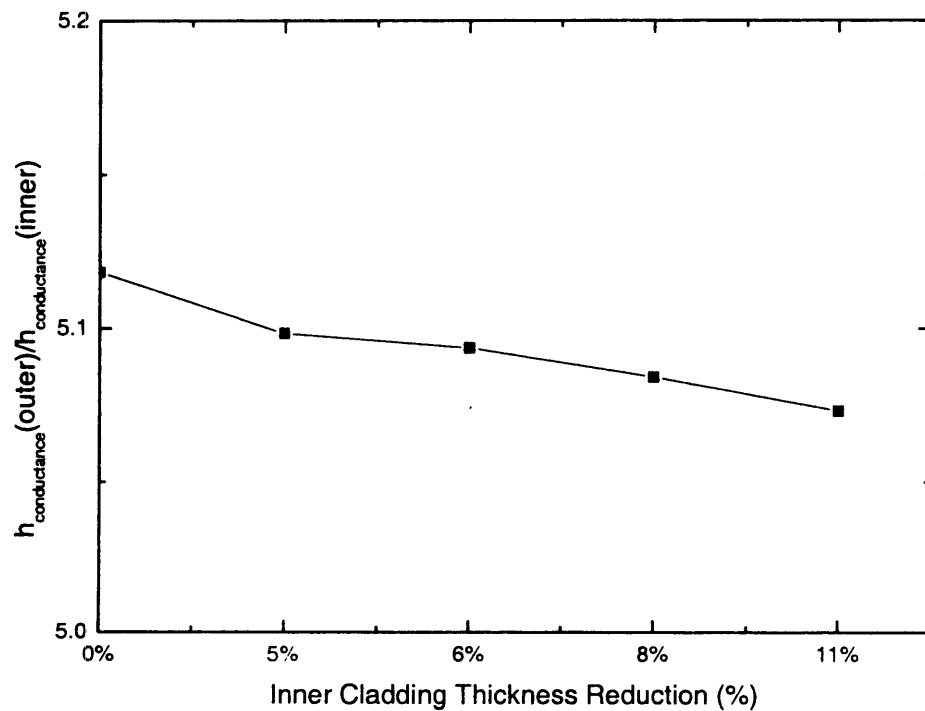


Figure 4.16 The Impact of Inner Cladding Thickness on Gap Conductance Asymmetry

4.5 I&E Cooled Annular Fuel Design Optimization

4.5.1 Reduction of Gap Conductance Asymmetry

As discussed in Section 4.4, the most effective approach to reduce the gap conductance asymmetry is to allow larger outer gap and smaller inner gap. However, it is difficult to achieve small enough inner gap size so that gap conductance symmetry can be obtained. Therefore, other approaches have to be attempted to resolve the problem.

The fuel-clad gap conductance includes three components: (1) conductance of the bonding gas which exists in the fuel-cladding interface, (2) conductance due to radiation and (3) conductance through the contacts of particles and cladding. Contributions from radiation and solid contact conductance are rather small and the gas conduction dominates the thermal conductance of the gap when solid contact pressure is small. Hence, in order to reduce the gap conductance asymmetry, it is desirable to reduce the gas conductance in the outer gap and increase the gas conductance in the inner gap. The gas conductance can be calculated with following correlation:

$$h_{gas} = \frac{K_{gas}}{\delta + \Delta} \quad (4-1)$$

Where h_{gas} = the gas conductance (W/K-m²),

K_{gas} = the gas conductivity in (W/K-m)

δ = the jump distance (m)

Δ = gap size (m)

(1) Fuel Roughness Adjustment

The fuel roughness is one of the important components of the jump distance, the rough surface of the fuel could form additional effective gap and as a result, reduce the gap conductance. By applying different roughnesses to the inner and outer surface, the outer gap conductance is reduced due to large fuel roughness, while with smaller surface roughness in the inner fuel

surface; the inner gap conductance could match the outer gap conductance during the initial period.

To evaluate the applicability of the roughness adjustment, a 150% power annular fuel case with a small inner gap size (minimum achievable by a commercial facility) and large outer gap size has been calculated with different roughness in the inner and outer fuel surface.

Table 4.5 Parameters for 150% Power Annular Rod

Cladding outside diameter	Outer:	15.4mm
	Inner:	8.6mm
Cladding thickness	Outer:	0.57mm
	Inner:	0.57mm
Fuel diameter	Outer:	14.1mm
	Inner:	9.88mm
Diametral gap size	Outer:	0.14mm
	Inner:	0.1mm

A fuel roughness of 3 microns has been used in the reference case calculations for both the inner and outer surfaces. The cladding roughnesses for both the inner and the outer cladding are taken as 3 microns as well. Since the inner fuel surface roughness is difficult to adjust, it will stay fixed while adjusting the outer fuel roughness. The results are shown in Figure 4.17.

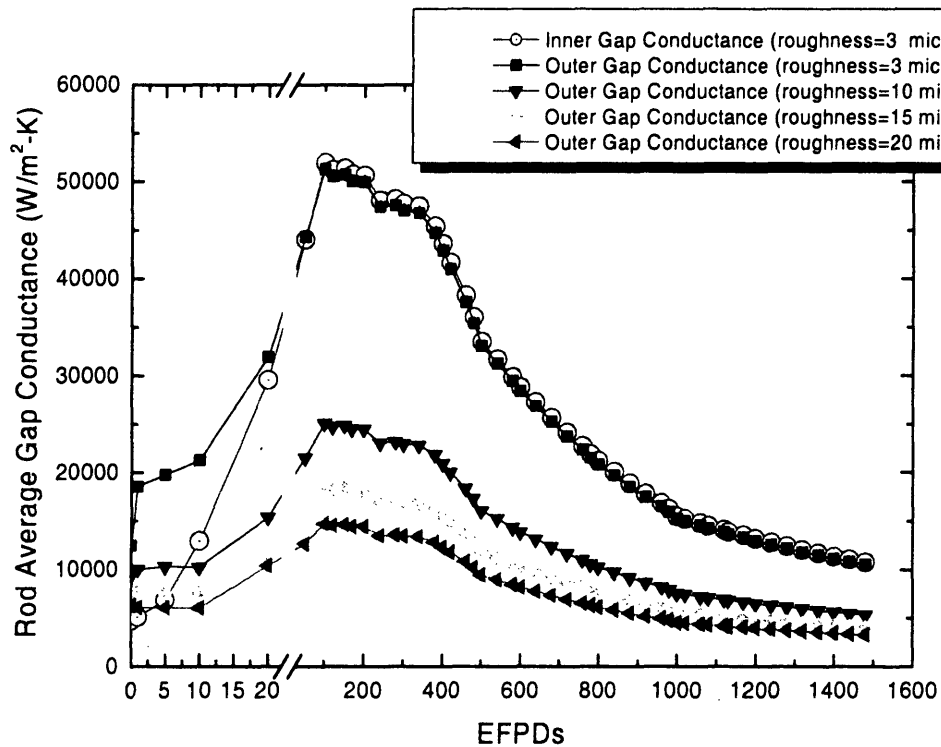


Figure 4.17 The Impact of Fuel Roughness on Gap Conductance Asymmetry

As shown in Figure 4.17, with larger roughness on the fuel outer surface, the gap conductance asymmetry at the initial stage of irradiation can be reduced or eliminated. This is because the large fuel roughness forms an effective outer gap size that matches the inner gap size. Hence, reducing the gap conductance imbalance, which results from the instantaneous outward thermal expansion of the fuel, is possible. However, after fuel cladding contact is made, the inner gap conductance becomes much larger than the outer gap conductance due to the fact that the large fuel roughness at the outer gap presents a large thermal resistance barrier. Therefore, using different fuel roughnesses at the inner and outer surfaces eliminates the gap imbalance problem when one gap is open while the other is closed, but it creates gap conductance imbalance after both gaps are closed.

(2) Cladding Roughness Adjustment

As previously analyzed, it is not practical to achieve gap conductance symmetry by adjusting the outer fuel surface roughness alone. Similar fuel roughnesses should be used to avoid gap conductance imbalance after gap closure. While the inner surface roughness of the fuel is difficult to control, it is much easier to control the inner cladding roughness at the inner surface before it is welded. The cladding internal roughness has the same function as the fuel roughness and can form an effective gap to reduce the outer gap conductance.

Using the case indicated in Table 4.5, both the cladding and the fuel roughnesses are adjusted to yield the same combined surface roughness (the cladding roughness + fuel roughness). The fuel and cladding roughness used for these evaluation cases are listed in Table 4.6.

Table 4.6 Cladding and Fuel Roughness for Evaluation Cases

	Cladding Internal Roughness (μm)		Fuel Roughness (μm)		Combined Roughness (Cladding+Fuel)(μm)		Gap thickness (radial) (μm)	
	Outer	Inner	Outer	Inner	Outer	Inner	Outer	Inner
Case 1	3	27	27	3	30	30	70	50
Case 2	3	32	32	3	35	35	70	50

The results are shown in Figure 4.18. It shows that with large combined cladding and fuel roughness, the gap conductance asymmetry problem can be resolved. Nevertheless, fabrication of such a large roughness poses a challenge to the fuel manufacturers. The fuel and cladding may be machined with grooves to achieve similar effects. [Hao, 2004] However, machining the fuel surface causes loss of fuel and adds to the costs of fuel manufacturing.

Although cladding strains can be reduced by increasing the cladding roughness, it causes stress concentrations in the cladding and may cause excessive local strains in the cladding

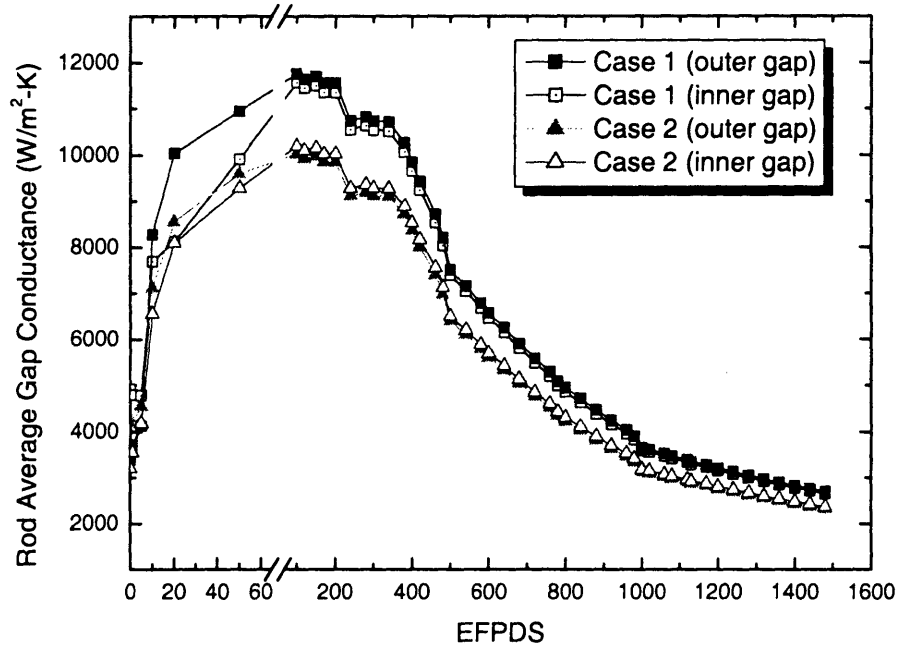


Figure 4.18 Impacts of Fuel and Cladding Roughness on Gap Conductance Asymmetry

(3) Application of ZrO_2 to Reduce the Gap Conductance Asymmetry

In order to achieve gap conductance asymmetry, ZrO_2 coating was considered due to its low thermal conductivity (~ 0.98 W/m-K at 700°C compared with UO_2 conductivity of ~ 4 W/m-K at 700°C) and its abundant availability at the fuel manufacturing facility. ZrO_2 coating turned out to be an unrealistic option because: (1) if only the outer fuel surface is coated, the gap conductance of the inner gap will be much larger than the outer gap after both gaps are closed, although gap conductance balance can be achieved during the initial irradiation period; (2) it is very difficult to coat the inner fuel surface; (3) A thick ZrO_2 layer ($\sim 1/3$ of the gap size) has to be applied to achieve gap conductance symmetry.

Another approach being considered is spattering of the fuel surface with ZrO_2 particles. By applying ZrO_2 particles to the outer fuel surface, the outer gap conductance is reduced due to the formation of an effective gap and a smaller thermal conductivity of ZrO_2 . After the fuel-

cladding gap closure, the ZrO_2 will be crashed due to the large contact pressure and the spattered ZrO_2 particles form a thin layer in the gap and the thermal conductance can be controlled, which avoid conductance imbalance caused. The ZrO_2 spattering option is evaluated with case presented in Table 4.5, the spattering particles are assumed to cover 50% of the fuel surface, and the cladding and fuel roughnesses are taken as $3\text{ }\mu\text{m}$. The results are shown in Figure 4.19.

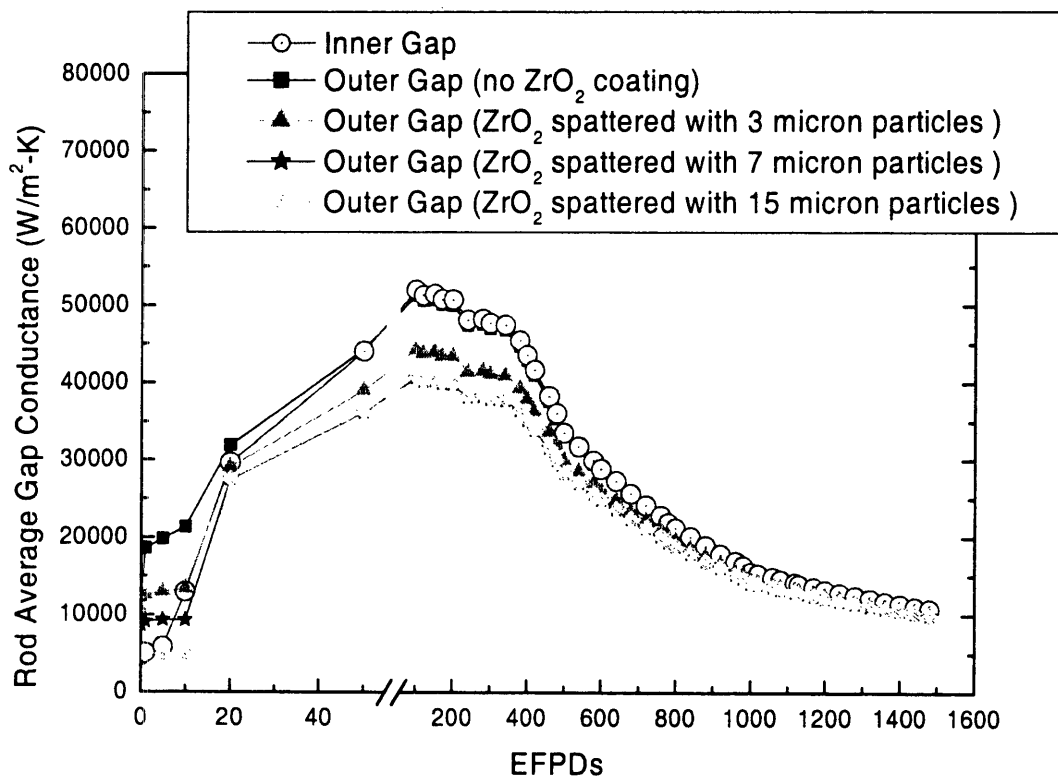


Figure 4.19 Impact of ZrO_2 Spattering on Gap Conductance Asymmetry

It can be seen in Figure 4.19 that with ZrO_2 spattering, the gap conductance asymmetry has been significantly reduced. Although there is a gap conductance imbalance during a period after both gap closure. The gap conductances are very large (larger than $35,000\text{W/m}^2\text{-K}$), the MDNBR problem is not a concern. However, manufacturing the fuel spattering with ZrO_2 particle presents a challenge and the ZrO_2 mechanical behavior might not be as desirable.

(4) Other Approaches to Eliminate the MDNBR Problem

As mentioned in Section 4.3, if both the inner and outer gap conductances are very large, gap conductance asymmetry can be tolerated. Therefore, the MDNBR problem can also be circumvented by improving both gap conductances. A metallic bond can be installed into fuel-cladding gap to significantly increase the thermal conductance of both gaps. However, the metallic bond compatibility with the cladding and the fuel is a constraint in fuel operation. A lead-bismuth-tin mixture (33wt% Pb-33wt% Bi-33wt% Sn) was found to have excellent compatibility with the zircaloy and uranium oxide at both normal and accident conditions [Wright et al, 1994]. In addition, it is also possible to implement fine wires in the fuel-clad gaps to improve both gap conductances. Both approaches are very expensive and could increase the fabrication cost significantly.

Collapsible cladding has been used in the Canadian CANDU reactor and the Indian Pressurized Heavy-Water Reactor (PHWR) [Das, et al, 1977]. This may be applied to the annular fuel design to eliminate the gap conductance asymmetry, since both the inner and outer claddings will collapse down to the fuel due to external coolant pressures. Therefore, when both gaps are closed for the entire life of the fuel the conductances become comparable for the inner and outer gaps. This option is not evaluated at the present stage but is recommended for future investigation.

4.5.2 Reduction of Cladding Strains

The reference design comparison in Section 4.2 has shown that for the annular fuel, the maximum strains occur during the initial stage of operation as a result of cladding creep-down to the fuel due to large gap sizes. The parametric study in Section 4.4 has also demonstrated that the maximum cladding strain can be reduced by reducing the total gap size. However, the reduction of the gap size is limited by concerns about gap conductance asymmetry and the capability of the fuel manufacturing facilities. Fortunately, cladding strains can be reduced by adjusting the fuel and cladding roughness or by spattering ZrO_2 particles, which are also able to reduce the gap conductance imbalance. With large cladding and fuel roughnesses or spattered particles, the cladding creep-down strain will be retarded during initial period of

irradiation, At high burnup, the swelling-induced strains are expected to be larger but since the roughness and ZrO_2 particle volumes are small, the strains due to swelling will not increase significantly.

The annular fuel cases with fuel cladding roughness optimization or ZrO_2 spattering have been compared for the 150% power reference case. The fuel dimensions of former cases are also optimized such that the outer gap is larger than the inner gap, along with the roughness adjustment or ZrO_2 spattering to achieve gap conductance symmetry. These cases are also compared with 100% power solid fuel. The main parameters are listed in Table 4.7.

Table 4.7 Parameters for Different Cases for Strain Comparison

	Annular rod with Roughness adjustment		Annular rod with Spattering ZrO_2 particles	Reference annular rod	Reference solid rod
Cladding outside diameter (mm)	Outer	15.4	15.4	15.4	9.5
	Inner	8.6	8.6	8.6	
Cladding thickness(mm)	Outer	0.57	0.57	0.57	0.57
	Inner	0.57	0.57	0.57	
Diametral gap size (mm)	Outer	0.14	0.14	0.12	0.16
	Inner	0.1	0.1	0.12	
Core Power	150%		150%	150%	100%
Annotation:	Fuel and cladding total internal roughness: 35 μm at both surfaces		Spattered particle size: 15 μm at the outer fuel surface	Fuel /cladding roughness: 3 μm	Fuel and cladding roughness: 3 μm

For the annular case with large fuel cladding roughness, the cladding strains due to initial cladding creepdown are significantly reduced due to the constraining effects of the fuel cladding roughness. (Figure 4.20) At high burnup, the strains are larger for the fuel with large roughness, which is expected due to the reduction of the gap volume and swelling-induced strains are larger.

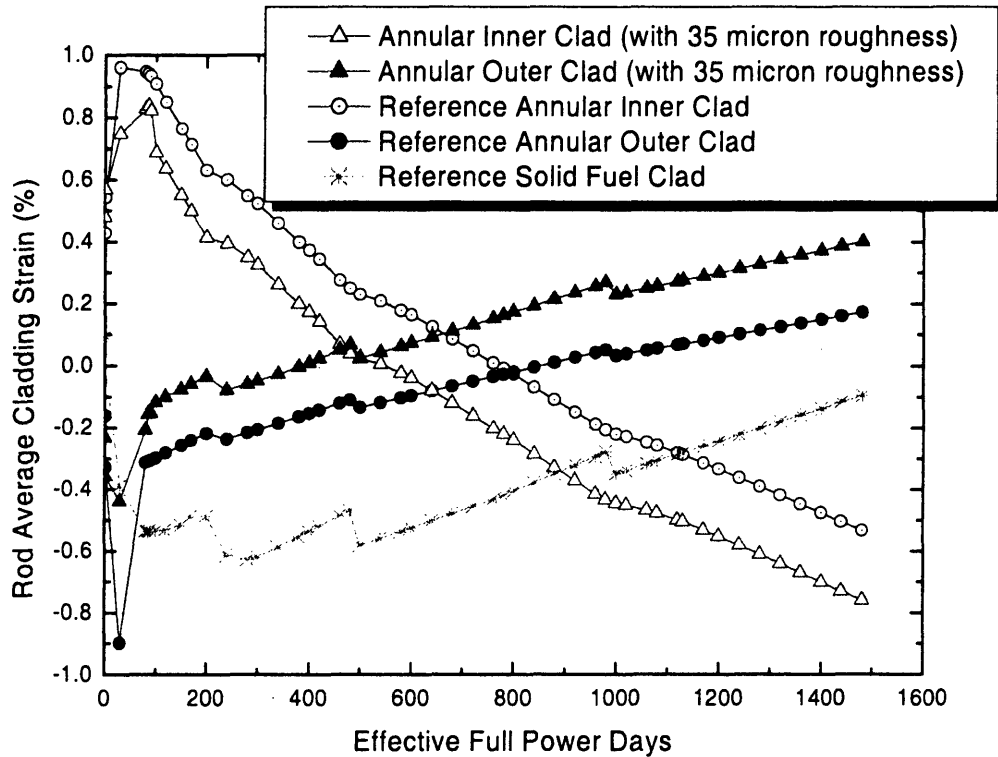


Figure 4.20 Impact of the Fuel Clad Roughness on Cladding Strain

The effect of the spattered ZrO_2 particles on the cladding strain is shown in Figure 4.21. It is obvious that with ZrO_2 , the cladding strains due to creepdown at initial period of irradiation are smaller compared with the reference annular fuel case, but are larger than the annular case (with $35\mu m$ fuel cladding roughness) as shown in Figure 4.20. This is because the spattered particle size is only $15\mu m$ while the roughness for the annular case shown in Figure 4.20 is $35\mu m$, the latter is more effective in resisting the cladding creepdown. Nevertheless, at high burnup, the swelling induced strains in the annular fuel case with spattered ZrO_2 particles are smaller than the annular case with large roughness.

Therefore, increasing the fuel cladding roughness and spattering the fuel particles can improve the gap conductance symmetry and reduce the cladding strains.

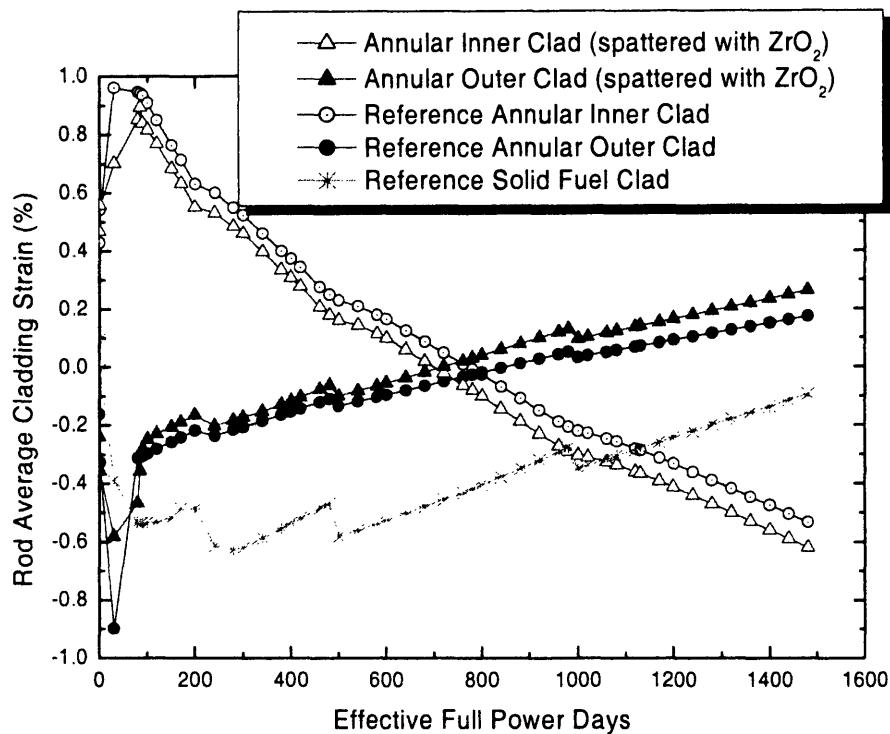


Figure 4.21 Impact of Spattered ZrO₂ on Cladding Strain

4.5.3 Annular Fuel Performance at Different Power Levels

The target of the annular fuel design is to achieve 150% power density of that of today's PWRs. However, to illustrate lower power level effects, the sensitivity of the annular fuel performance at different power levels (ranging from 100% power to 150% power) is analyzed with optimized fuel design. The annular fuel design with spattered ZrO₂ particles on the fuel surface, as presented in Table 4.7, is evaluated for different power levels.

The results are shown in Figure 4.22 and Figure 2.3. The strains are not much affected by the power level and the fission gas release is higher for higher power level due to the higher burnup.

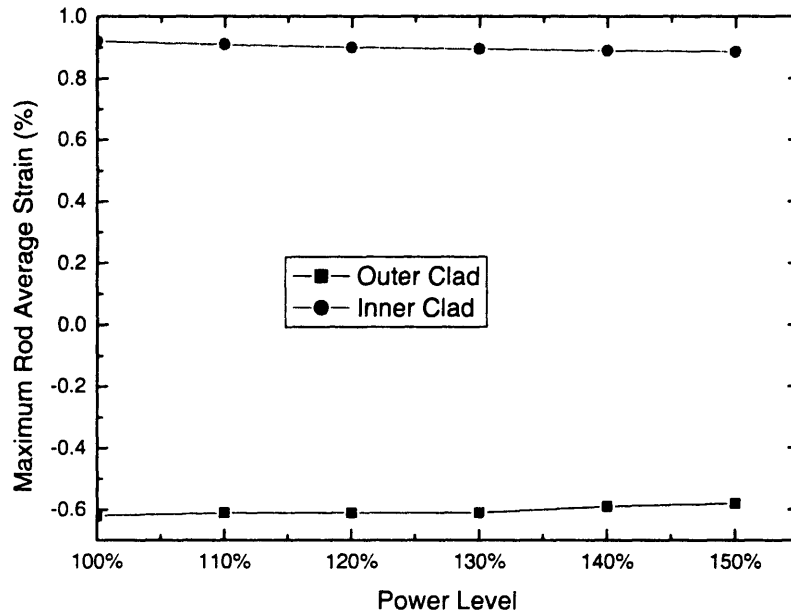


Figure 4.22 Maximum Rod Average Strain for Different Power Levels

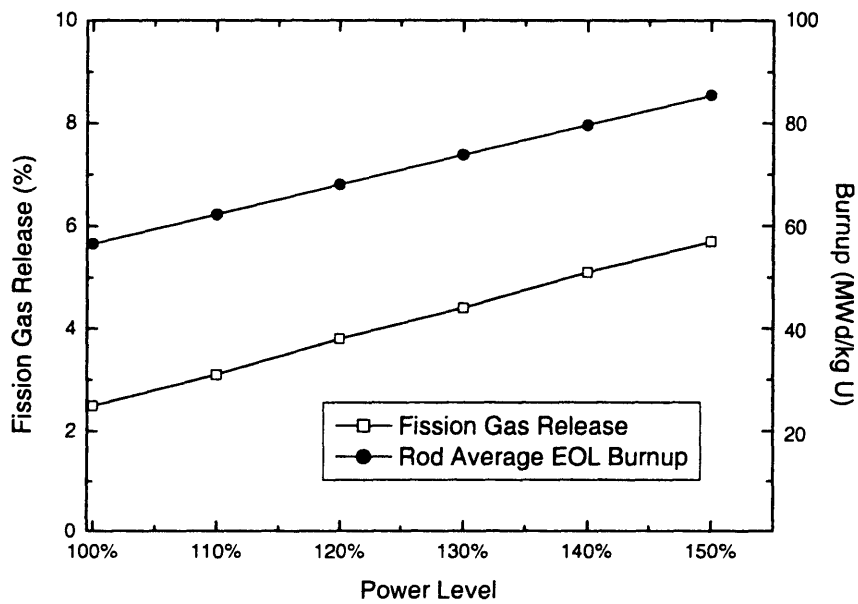


Figure 4.23 Fission Gas Releases for Different Power Levels

4.6 Summary

The rod design criteria have been described for I&E cooled annular fuel. The 100% power and 150% power reference annular fuel cases have been compared with the solid PWR fuel and the rod Ocone 15309, which is a real fuel rod. It is found that the reference annular fuel rods have lower fission gas release at 100% power and higher fission gas release at 150% power than the solid fuel. However, the fission gas release from 150% power is still small (~6%). The maximum cladding strains for the annular fuels are higher than the solid fuel due to the larger diameter and gap sizes. The hydrogen concentration and the oxide accumulation of the annular fuel are comparable to those of the solid fuel. However, the gap conductance asymmetry caused by outward thermal expansion has been identified as a major concern for the annular fuel. The asymmetry can result in a small MDNBR.

A sensitivity study of major parameters for I&E cooled annular fuel has been performed. It has been shown that smaller as-fabricated inner gap size and larger outer gap could reduce the gap conductance asymmetry. The total gap size should be reduced and fuel volume should be enlarged to reduce the cladding strains due to cladding creepdown. Increasing the initial filled helium gas pressure produces little improvement for gap conductance imbalance; whereas higher initial gas pressure is detrimental to fuel operation since high rod internal pressure could reopen the fuel-cladding gaps with the addition of fission gases. The adjustment of inner and outer cladding thicknesses could allow allocation of swelling-induced cladding strains but higher H₂ concentration and higher fraction of oxide layer poses concerns for cladding failure. The reduction of inner cladding thickness has little improvement for gap conductance asymmetry.

The gap asymmetry problem can be circumvented by adjusting the gap sizes to allow larger outer gap and smaller inner gap, combined with enlarging the fuel and cladding roughnesses or spattering the fuel surface with ZrO₂ particles. Large fuel cladding roughness and the spattered ZrO₂ particles are also found to be effective in reducing the strains due to cladding creepdown to the fuel. The optimized annular fuel are analyzed at different power levels, no significant impact on cladding strain is found and fission gas release increases with power level due to higher burnup.

Chapter 5 Performance Models for the Annular Fuel

5.1 VIPAC fuel thermal conductivity model

5.1.1 Background

The thermal conductivity of powder beds has been investigated extensively. Various theoretical models have been developed and experiments performed to investigate the bulk thermal conductivity of a powder bed in a gaseous environment. The thermal conductivity was presented as a function of many variables, such as the gaseous phase thermal conductivity, solid phase thermal conductivity, smear density, packing pattern, particle size and shape, gas pressure, oxide layer, and contact surface area [Bauer and Schlunder, 1978]. The Vibration Compacted (VIPAC) fuel thermal conductivity was measured and a theoretical model was developed for fast reactor fuel [Hall and Martin, 1981]. The Hall and Martin theoretical model was developed assuming uniform packing and linear flow of heat. However, complexities of the theoretical models did not improve the fit between models and experimental data. Therefore, empirical models to provide a simple description of the VIPAC thermal conductivity are often applied. Calza-Bini et al measured the VIPAC $\text{UO}_2\text{-PuO}_2$ fuel back in early 1970's and developed a simple fit of the VIPAC thermal conductivity [Calza-Bini, et al, 1974]. VIPAC thermal conductivity was also measured at PNL and a simple model was provided as a function of temperature and fission content. [Cunnolingham, et al, 1981]

5.1.2 Model Development

Both the theoretical models and empirical models of previous studies are somewhat inadequate for implementation in our VIPAC fuel performance code for the following reasons: (1) Theoretical models are difficult to implement yet provide a poor agreement with experiments.

(2) Empirical models found in the literature generally lack important parameters that impact the VIPAC thermal conductivity, such as burnup, rod pressure, porosity, etc.

The Cunnllingham model described the VIPAC thermal conductivity as a function of temperature and fission gas content for 87% TD VIPAC fuel after first few days of irradiation.

$$K(T) = 3.506 - 1.37 \times 10^{-3} (T - 273) + 4.23 \times 10^{-7} (T - 273)^2 \quad (5-1)$$

where, T = fuel temperature in K,

K(T)= the thermal conductivity in (W/m-K)

With the addition of fission gas, the thermal conductivity will be reduced and the degradation factor is described as:

$$F_{fgr} = 1.0 \text{ for fission gas mole fraction less than 11\%}$$

$$F_{fgr} = 1.0 - 0.01 \times [10.05 \times (fgr - 11)]^{1/2} \text{ for fission gas mole fraction larger than 11\%}$$

Where fgr is the mole fraction of the fission gases including Kr, Xe in fill gas.

The Cunnllingham model tends to over predict the VIPAC thermal conductivity data of Hall and Martin. Therefore, the parameters were adjusted in our model to yield a better fit between data and model:

$$K(T) = 3.22 - 1.82 \times 10^{-3} (T - 273) + 4.23 \times 10^{-7} (T - 273)^2 \quad (5-2)$$

This model provides a reasonable agreement with experimental data, but it lacks important parameters that affect the thermal conductivity, such as gas pressure and burnup degradation.. Therefore, a modified VIPAC conductivity model was developed to incorporate the pressure, burnup, porosity effects as extension of the Cunnllingham model.

It is assumed that the VIPAC fuel conductivity burnup degradation effects be the same as in the pelletized fuel. The burnup factor in FRAPCON-3 has been applied to VIPAC fuel. In order to apply the FRAPCON-3 burnup factor to the VIPAC fuel model, a simple fit was derived depending on the temperature as shown in Figure 5.1.

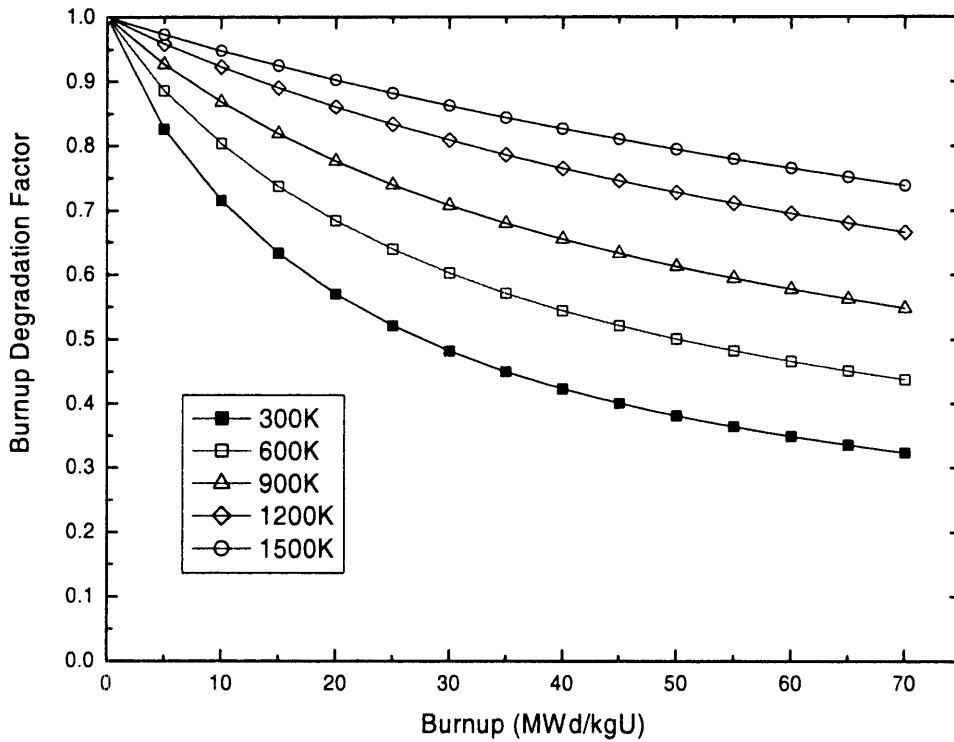


Figure 5.1 Burnup Degradation Factor as a Function of Burnup and temperature

The burnup degradation factor can be represented with the following correlations:

$$F_{Bu} = a(4) \cdot bu^4 + a(3) \cdot bu^3 + a(2) \cdot bu^2 + a(1) \cdot bu + a(0) \quad (5-3)$$

where

F_{Bu} = burnup degradation factor

bu = burnup in MWd/kgU

$$a(4) = -3.46 \times 10^{-17} T^3 + 1.58 \times 10^{-13} T^2 - 2.45 \times 10^{-10} T + 1.30 \times 10^{-7}$$

$$a(3) = 4.97 \times 10^{-15} T^3 - 2.46 \times 10^{-11} T^2 + 4.13 \times 10^{-8} T - 2.35 \times 10^{-5}$$

$$a(2) = -1.95 \times 10^{-13} T^3 + 1.21 \times 10^{-9} T^2 - 2.41 \times 10^{-6} T + 1.58 \times 10^{-3}$$

$$a(1) = -1.23 \times 10^{-12} T^3 - 1.25 \times 10^{-8} T^2 + 5.12 \times 10^{-5} T - 5.0 \times 10^{-2}$$

$$a(0) = 5.25 \times 10^{-12} T^3 - 2.07 \times 10^{-8} T^2 + 2.80 \times 10^{-5} T + 1.0$$

and T is the fuel temperature in K.

The VIPAC fuel thermal conductivity also changes with internal gas pressure, with higher pressure, the convective heat transfer is enhanced, resulting in higher bulk thermal conductivity. An empirical correlation is used to express the pressure effect based on Hall and Martin data. [Hall and Martin, 1981]. This pressure factor is to be multiplied by K(T) as predicted by the Cunnligham model.

$$F_p = 1.1986 \times P^{0.23} \quad (5-4)$$

where P is pressure in MPa, the correlation is illustrated in Figure 5.2.

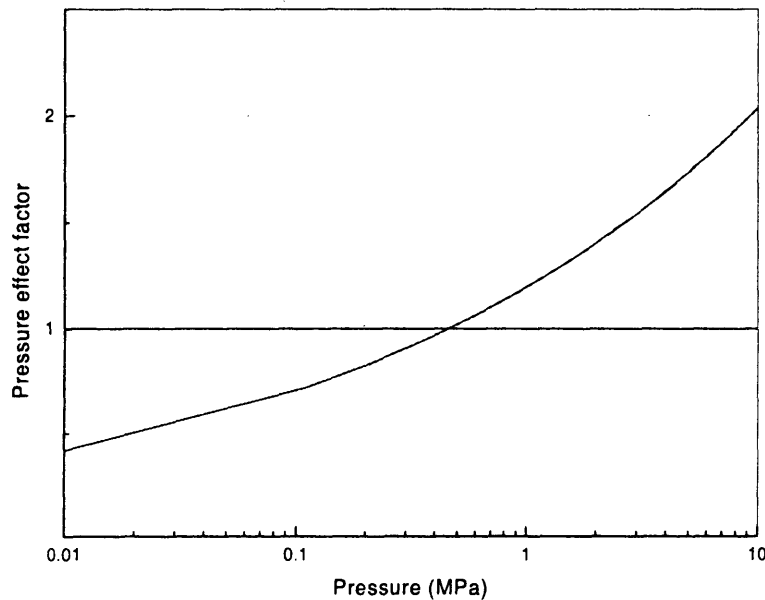


Figure 5.2 The Pressure Effect Factor

For thermal conductivity of the sintered fuel, the porosity effect can be approximated by a linear correlation. When VIPAC fuel is packed at a high smear density, the thermal conductivity of VIPAC behaves very much similar to that of the sintered fuel. Therefore, a simple linear correlation is applied to VIPAC fuel thermal conductivity with various porosities, which is valid for the VIPAC fuel of our interest (smear density between ~87% and 92%). The linear correlation is derived by extrapolating between NFI model in FRAPCON-3 (95% TD) and the Cunnolingham model (87% smear density). However, for VIPAC fuel with smear density lower than 87% TD, a simple relation is derived to describe the degradation of the VIPAC fuel thermal conductivity, which is expressed as

$$F_{por} = e^{-0.125 \cdot (porosity - 13)} \quad (5-5)$$

where, F_{por} is the porosity factor, porosity is VIPAC fuel porosity in percent.

Hence, the complete model can be described by the following equations:

$$K_{base} = 3.22 - 1.82 \times 10^{-3} (T - 273) + 4.23 \times 10^{-7} (T - 273)^2 \quad (5-6)$$

where K_{base} is the base thermal conductivity correlation by Martin and Hall, T is temperature in K.

$$K_{NFI} = \frac{1}{(0.0452 + 0.000246 \cdot T + 0.00187 \cdot Bu + (1.0 - 0.9 \cdot e^{-0.04 \cdot Bu}) \times 0.038 \cdot Bu^{0.28} \cdot \frac{1}{1 + 396 \times e^{-6380/T}})} + \frac{3.5 \times 10^9 \cdot e^{-16361/T}}{T^2} \quad (5-7)$$

K_{NFI} is the NFI thermal conductivity model used in the latest version of FRAPCON-3, Bu is burnup in MWd/kgU. [Lanning et al, 1997]

For VIPAC fuel with smear density lower than 87%,

$$K_{VIPAC}(T, Bu, P, fgr, \rho) = K_{Base} \cdot F_{fgr} \cdot F_P \cdot F_{Bu} \cdot F_{por} \quad (5-8)$$

For VIPAC fuel with smear density between 87% and 95%

$$K_{VIPAC}(T, Bu, P, fgr, \rho) = K_{Base} \cdot F_{fgr} \cdot F_P \cdot F_{Bu} + 0.125 \times (K_{NFI} - K_{Base} \cdot F_{fgr} \cdot F_P \cdot F_{Bu}) \times (\rho - 0.87) \quad (5-9)$$

For VIPAC fuel with smear density higher than 95%

$$K_{VIPAC}(T, Bu, P, fgr, \rho) = K_{NFI} \times 1.079 \times \frac{\rho}{1 + 0.5 \times (1 - \rho)} \quad (5-10)$$

Where,

T = Temperature in K

Bu = Burnup in MWd/kgU

P = pressure in MPa

fgr = fission gas mole fraction in the fill gas

ρ = Fuel smear density as a fraction of theoretical UO₂ density

F_{fgr} = Fission gas degradation factor

F_p = Pressure effect factor

F_{bu} = Burnup degradation factor

Calculated thermal conductivities for different smear density VIPAC fuels are illustrated in Figure 5.3.

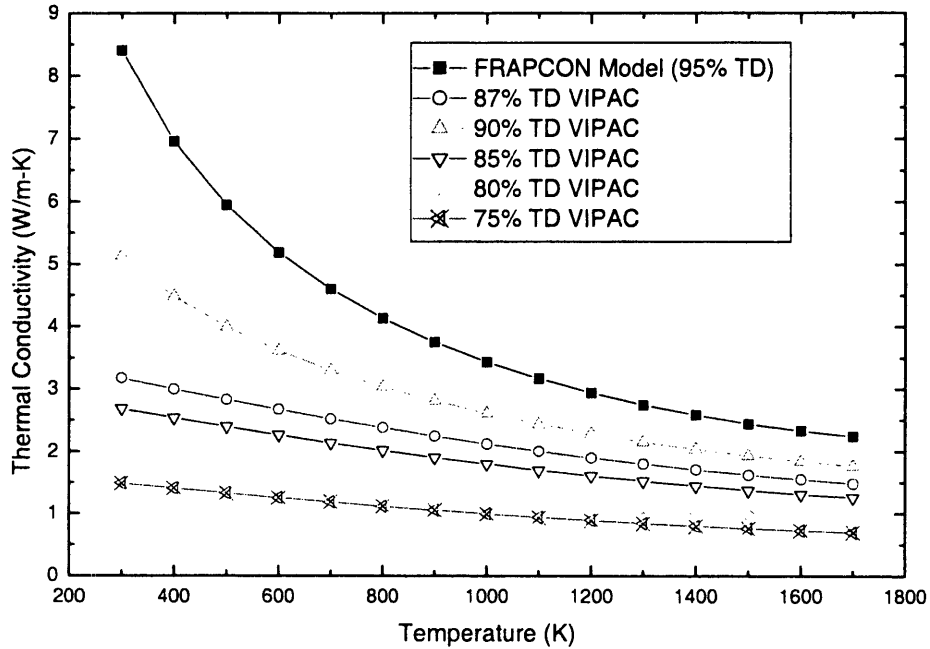


**Figure 5.3 Effect of Smea Densities on VIPAC Fuel Thermal Conductivity
(VIPAC fuels are at 0.445MPa helium environment)**

The effect of porosity on VIPAC fuel thermal conductivity is more significant at lower temperatures than at higher temperatures, as shown in Figure 5.3. At higher sinter density, the correlation of VIPAC thermal conductivity draws closer to the sintered fuel thermal conductivity. This can be explained by the occurrence of a fuel sintering effect at higher temperatures, which causes the fuel to behave like sintered fuel.

5.1.3 Comparison of the model with data

The VIPAC fuel thermal conductivity was extrapolated from empirical correlations of Cunningham and Barner [1981], with addition of porosity, gas pressure and burnup terms. Moreover, the data from Hall and Martin was also used to tune the model predications. A comparison is presented to verify the validity of the models.

Hall and Martin presented the thermal conductivity data measured at different gas pressures, temperatures, gas compositions and smear densities [Hall and Martin, 1981]. Samples with three fraction particles packed at 86.6% smear density and two fraction particles with 79.9% smear densities were measured.

The data obtained for different gas pressures are presented in Figure 5.4 and compared to model predictions. The VIPAC fuel sample is composed of three-fraction particles with 86.6% smear density in helium gas environment, and the thermal conductivities are measured at different temperatures. The thermal conductivity is quite sensitive to the gas pressure. At higher gas pressure, the gaseous phase conduction contribution to the bulk thermal conductivity is improved hence the bulk thermal conductivity is increased. The model predictions are consistent with the experimental data, as shown in Figure 5.4.

Another sample which contained two fraction particles with smear density of 79.9% is also compared with the three-fraction sample (Figure 5.5). Thermal conductivities of both fuel samples were measured at 0.1MPa helium pressure. It can be seen that with decreased smear density, the thermal conductivity dropped significantly from $\sim 2.0 \text{ W/m-k}$ to $\sim 1.0 \text{ W/m-k}$ at 580K. The model predictions and data have reasonable agreement.

It is expected that fission gas release will significantly deteriorate the bulk thermal conductivity of the VIPAC fuel. The data presented in Figure 5.6 is derived from a simulated environment with 100% krypton. Due to the lower thermal conductivity of krypton, the bulk thermal conductivity is reduced by more than 50% compared with the case with 100% helium content.

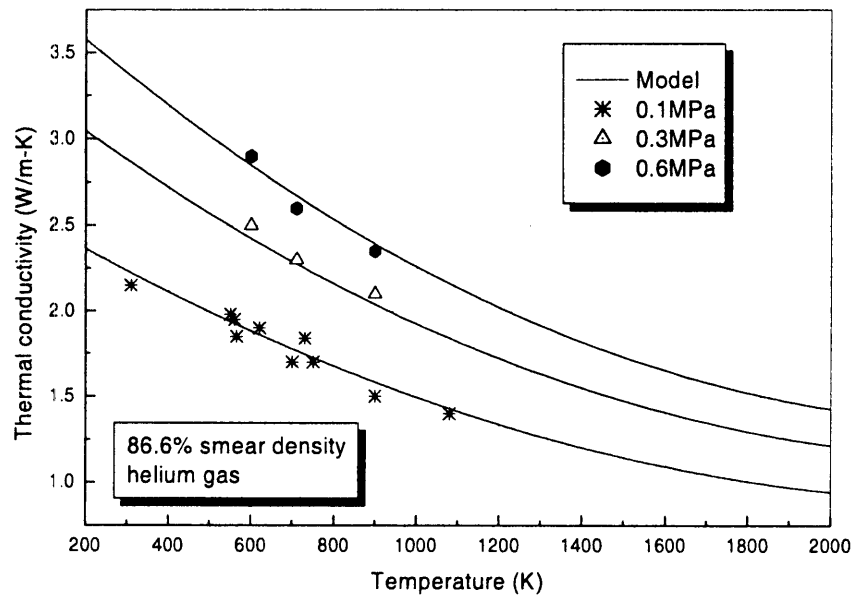


Figure 5.4 Comparison of Data and Model at Different Gas Pressures

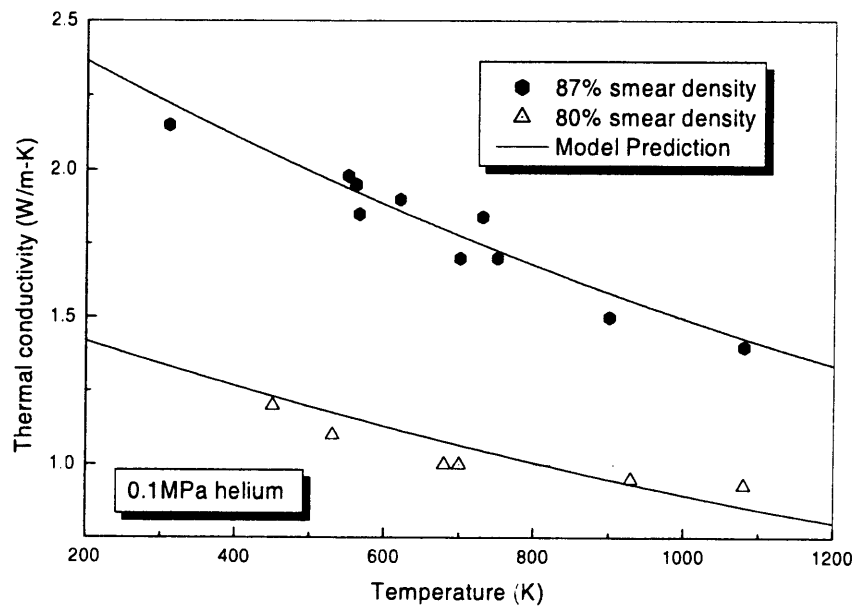


Figure 5.5 Comparison of Data and Model at Different Smear Densities

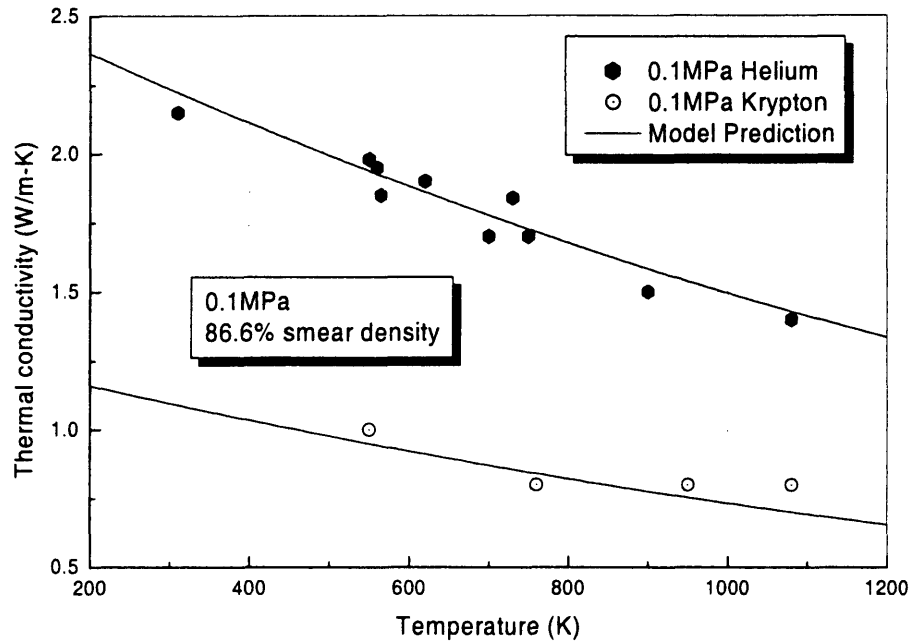


Figure 5.6 Comparison of Data and Model at Different Ambient Gas Conditions

5.2 VIPAC fuel-cladding conductance model

5.2.1 Model development

Three components contribute to the VIPAC fuel-cladding conductance: (1) conductance of the bonding gas which exists at the fuel-cladding interface, (2) conductance due to radiation and (3) conductance through the contacts of particles and cladding.

$$h = h_{gas} + h_{radiation} + h_{contact} \quad (5-11)$$

The gas conductance term is often expressed as:

$$h_{gas} = \frac{K_{gas}}{\delta} \quad (5-12)$$

where K_{gas} is the gas conductivity in (W/K-m) and δ is the jump distance, which is an equivalent gap between the VIPAC fuel particles and cladding. It was observed that the VIPAC fuel thermal conductivity is very sensitive to the internal rod pressure [Cunnlingham, et al, 1981]. A gas pressure term is also added to the gas conductance equation:

$$h_{gas} = \frac{K_{gas} F_{pres}}{\delta} \quad (5-13)$$

The jump distance δ can be calculated using the following correlation [Berna et al, 1997]:

$$\delta = \max(R_f \cdot e^{-0.00125P}, 3.15 \times 10^{-5}) \quad (5-14)$$

where:

P = fuel-cladding interfacial pressure (kg/cm²)

R_f = the mean particle radius the VIPAC fuel (m)

The pressure effect term assumes the same shape as that in the VIPAC fuel thermal conductivity but the constants are corrected to fit the data [Calza-Bini et al, 1974]:

$$F_{Pres} = 12.45 \times P^{0.23} \quad (5-15)$$

The conductance through the points of contacts, taken from the FRAPCON-3 conductance model, depends on the fuel-cladding interfacial pressure and the microscopic roughness:

$$\begin{aligned} h_{contact} &= \frac{5.0 K_m P_{rel} R_{mult}}{R E}, \text{ if } P_{rel} > 0.003 \\ &= \frac{0.015 K_m}{R E}, \text{ if } 9.10^{-6} < P_{rel} < 0.003 \\ &= \frac{5.0 K_m P_{rel}^{0.5}}{R E}, \text{ if } P_{rel} < 9.10^{-6} \end{aligned} \quad (5-16)$$

where:

P_{rel} = ratio of interfacial pressure to cladding Meyer hardness (taken as 680 MPa)

$K_m = \frac{2K_f K_c}{(K_f + K_c)}$, where K_m is mean conductivity (W/m-K) and the subscripts f and c stand for fuel and cladding respectively.

$R = R_f$ mean particle radius the VIPAC fuel (m)

$R_{mult} = 333.3 P_{rel}$, if $P_{rel} \leq 0.0087$ or $R_{mult} = 2.9$, if $P_{rel} > 0.0087$

$E = [5.738 - 0.528 \ln(R_2)]$

R_2 is the roughness of the rougher surface (m) of the fuel and cladding.

The radiation contribution is usually small at low temperatures but can be significant at very high temperatures. The same correlation has been used as in the FRAPCON-3 model:

$$h_{radiation} = \sigma \cdot F \cdot [T_{fs}^2 + T_{ci}^2][T_{fs} + T_{ci}] \quad (5-17)$$

T_{fs} is fuel surface temp. (K),

T_{ci} is cladding inner surface temp. (K).

σ is Stefan-Boltzman constant, $\sigma = 5.6697 \times 10^{-8} (W/m^2 - K^4)$,

$$F = \frac{1}{1/e_f + (r_{fs}/r_{ci})(1/e_c - 1)}$$

where, e_f , e_c are fuel emissivity and cladding emissivity, respectively. r_{fs} , r_{ci} are fuel outer surface radius and cladding inner surface radius, respectively, which are approximately the same in the VIPAC fuel case.

5.2.2 Comparison with data and sensitivity analysis

Calza-Bini presented a set of measurements of the VIPAC fuel interfacial conductance for different linear powers [Calza-Bini et al, 1974]. It was found during the first 12 hours of irradiation that the interfacial conductance dropped significantly due to formation of oxide layer in the inner cladding surface. The conductance stabilized afterwards. However, the conductance tended to be still very low compared with data presented by Fitts and Miller [Fitts and Miller, 1973]. This is due to the fact that data was obtained from VIPAC fuel with very large mean particle size ($\sim 1800\mu\text{m}$) and low gas pressure (0.09MPa). The Calza-Bini data is compared with the model prediction and the agreement is satisfactory (Figure 5.7).

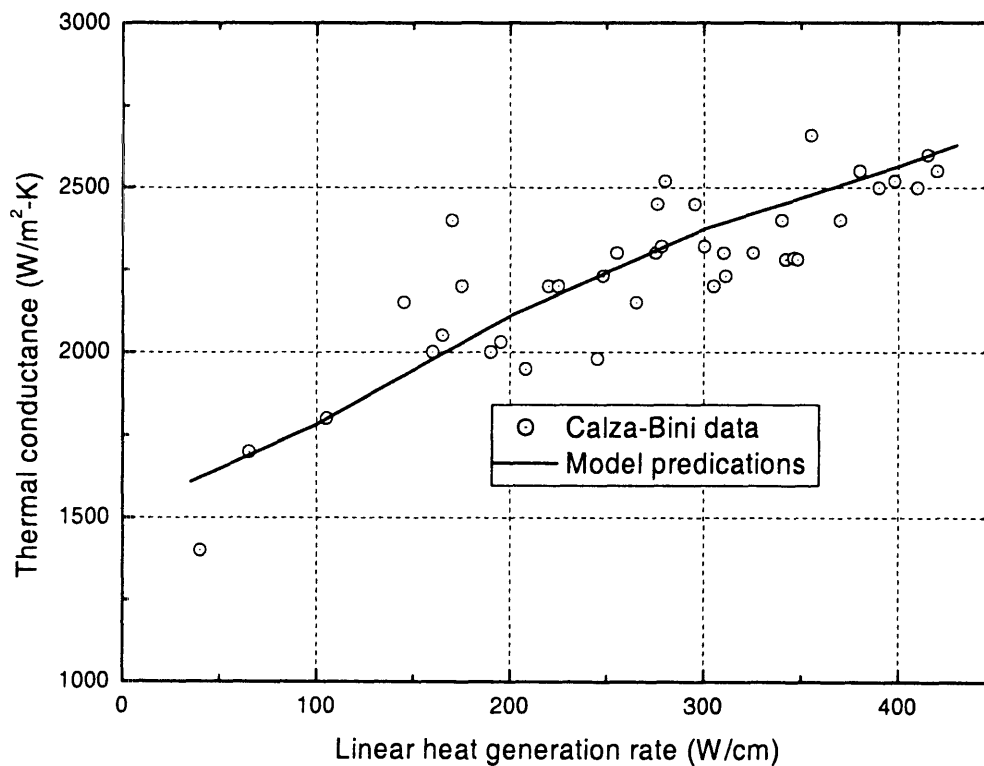


Figure 5.7 Thermal Conductance Data of Calza-Bini Compared to Model Prediction

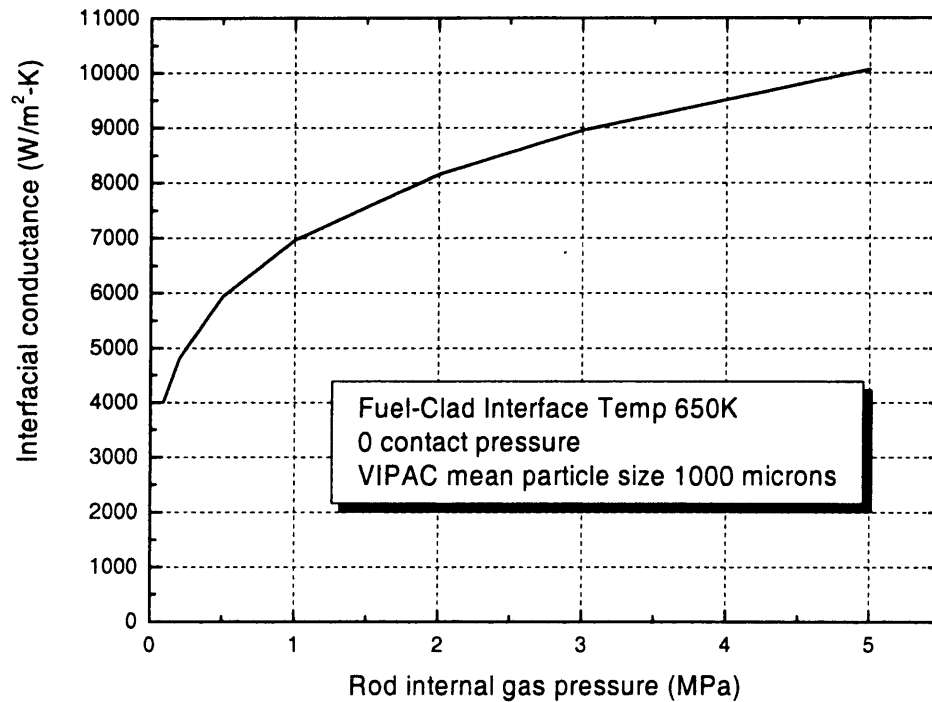


Figure 5.8 Effect of Gas Pressure on Interfacial Thermal Conductance

Similar to the VIPAC thermal conductivity, the gas pressure serves to enhance the gas conductivity due to enhanced convective heat transfer and as result, increase the conductance. As can be seen in Figure 5.8, the VIPAC fuel cladding conductance increases from ~4,000W/m²-K at 0.1 MPa to ~10,000 W/m²-K at 5.0 MPa. For large particle size VIPAC fuel, it would be desirable to pressurize the rod with higher helium pressure. However, increasing the rod internal pressure may pose adverse effect on the cladding and offset the contact pressure contribution to the interfacial thermal conductance.

The VIPAC fuel-clad conductance is very sensitive to VIPAC fuel particle size (Figure 5.9). The particle size dictates the jump distance and the larger the particle size, the lower the conductance. This is reasonable since the larger particle size limits the contacts between fuel and cladding and the equivalent “gap” between the fuel and cladding becomes larger.

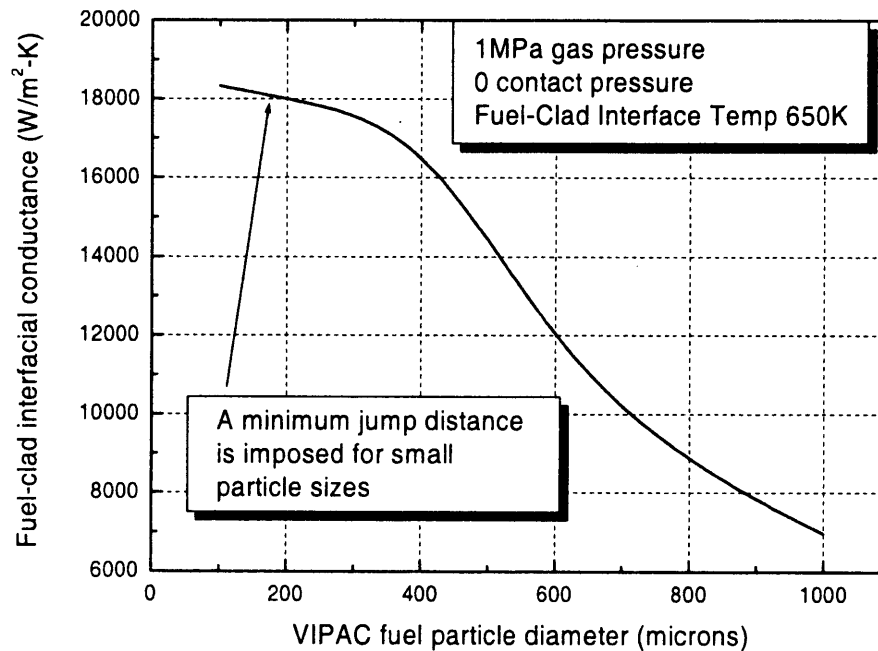


Figure 5.9 Effect VIPAC Fuel Particle Size on Fuel-clad Conductance

It is shown in Figure 5.9 that the particle size effect is significant for particle sizes larger than 400 μm and relatively mild for particle sizes smaller than 400 μm . This is due to the fact that, with a smaller particle size, the effect of conduction by contacts will saturate and a minimum equivalent “gap” size is reached.

The build-up of the contact pressure between the VIPAC fuel and cladding will also improve the fuel-clad conductance because the contact pressure will enhance heat conduction through contacts as it will crash some of the particles existing at the fuel cladding interface and extend the contacts between fuel and cladding. The contact pressure effect is illustrated in Figure 5.10.

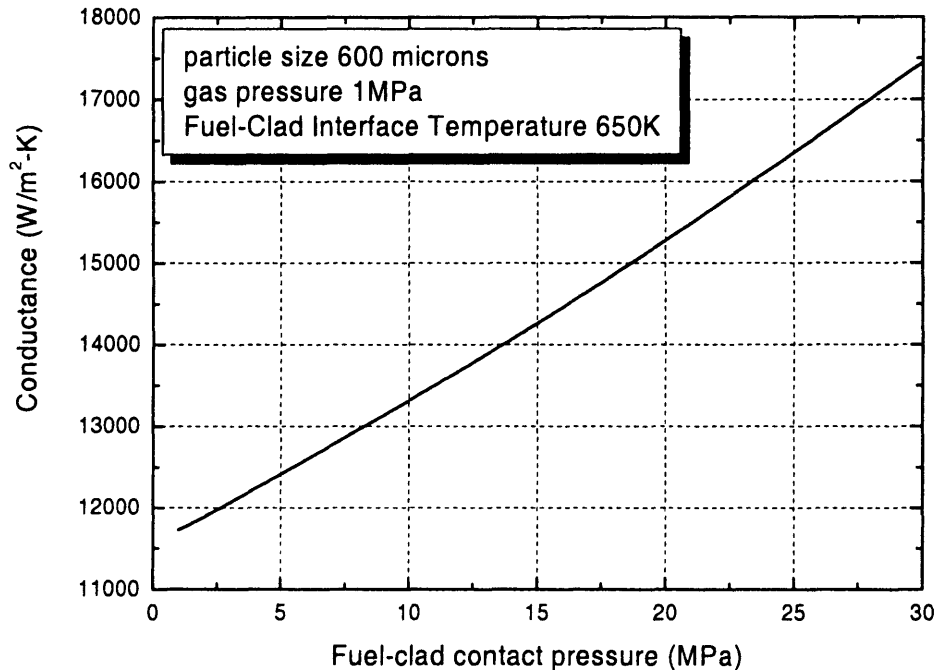


Figure 5.10 Effect of VIPAC Fuel Contact Pressure on Fuel-clad Thermal Conductance

5.3 VIPAC Annular Fuel Dimensional Changes

5.3.1 Overview

The dimensional changes of the I&E cooled VIPAC annular fuel are affected by the following mechanisms: (1) thermal expansion due to temperature effects, (2) densification of the fuel due to the disappearance of small porosities by irradiation, (3) solid fission product swelling. The VIPAC fuel is different from the pelletized fuel in that it is composed of small particles which are readily adjusted to the cladding geometry. Therefore, the models of the VIPAC annular fuel are different from those of the pelletized annular fuel.

Although cracks may develop within the fuel particles, the relocation model is not applicable to VIPAC annular fuel since no fuel cladding gaps exist and the fuel particles can be easily moved to fill the entire volume enclosed by the claddings. Therefore, the relocation model of

the pelletized fuel, which calculates the effects of the fuel relocation on fuel-cladding gap as a function of linear power and burnup, is eliminated from the VIPAC annular fuel modeling code.

The thermal expansion of the VIPAC fuel is also different from the pelletized annular fuel. The thermal results in larger particle diameters. Unlike the pelletized fuel where thermal expansion causes the outward fuel expansion, VIPAC annular fuel thermal expansion will allow the fuel particles to move in both directions within the concentric claddings. Therefore, the bulk effect is that the fuel outer radius is enlarged and the inner radius is reduced due to thermal expansion.

The VIPAC fuel has large porosity which can accommodate the fission products. Therefore, swelling of the VIPAC fuel is much less than the pelletized fuel. The dimensional change caused by swelling is not seen until reaching high burnup.

5.3.2 A Simple Model for VIPAC Thermal Expansion, Swelling and Densification

The bulk dimensional changes of a particle bed are complicated and are dictated by many factors including the distribution of the particle shapes, particle sizes and packing ratio. However, a simplified model for the particle bed can be derived by assuming all particles are rigid spheres with the same thermal mechanical properties and are closely packed. The bulk thermal expansion of a closely packed particle bed is about the same as that of the solid body. Therefore, the VIPAC annular fuel dimensional changes can be calculated considering the thermal expansion, swelling and densification using the bulk properties of thermal expansion, densification and swelling of the sintered fuel. Hence, the VIPAC fuel dimensional change model is based on following assumptions:

- The particles are rigid spheres and the bulk thermal expansion rate is the same as that of the solid fuel.

- Densification causes shrinkage of the fuel particles due to disappearance of the small porosities.
- Solid fission product will be accommodated by the spaces between the fuel particles. The bulk swelling rate is assumed to be zero at low burnup and the swelling contribution leads to a reduction of the VIPAC fuel porosity. At high burnup, when the fuel porosity is reduced to about 4-6%, the swelling contribution causes expansion of the fuel.
- The fuel particles can be moved due to cladding pressures. Hence, the bulk dimension is also dictated by fuel-cladding interaction.

Similar to the sintered fuel, the VIPAC fuel is divided into multiple radial rings as shown in Figure 5.11.

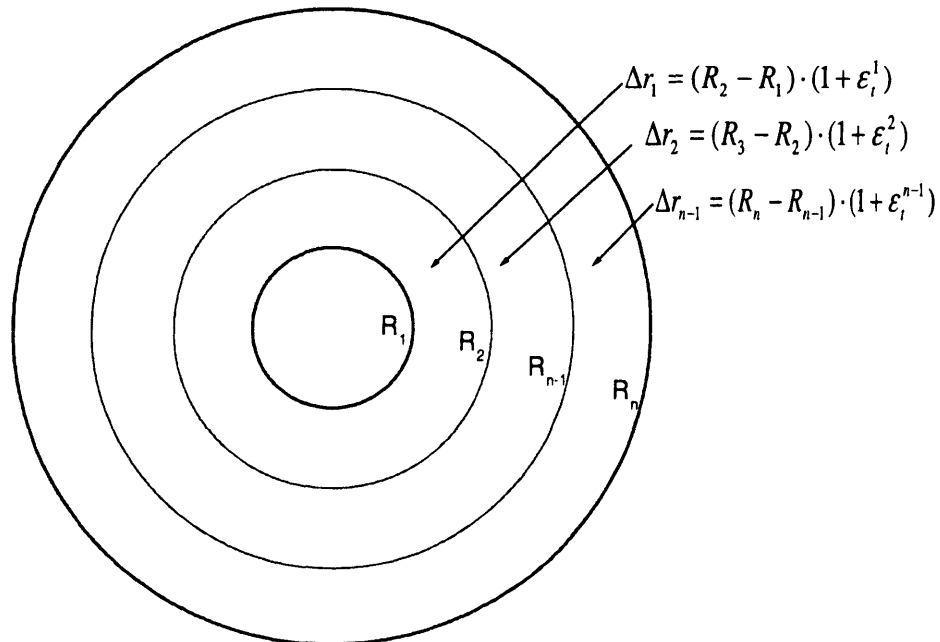


Figure 5.11 Illustration of the VIPAC Fuel Radial Rings

The dimensional increment for each ring can be calculated with the following equations:

$$\Delta r_1 = (R_2 - R_1) \cdot (1 + \varepsilon_1^1) \quad (5-18)$$

$$\Delta r_2 = (R_3 - R_2) \cdot (1 + \varepsilon_2^2) \quad (5-19)$$

...

$$\Delta r_{n-1} = (R_n - R_{n-1}) \cdot (1 + \varepsilon_{n-1}^{n-1}) \quad (5-20)$$

$\Delta r_1, \Delta r_2, \dots, \Delta r_{n-1}$ denote the dimensional changes at each ring and $R_1, R_2, \dots, R_n$ denote the cold state ring dimensions of the fuel. And ε_i^i is the total strain at the i^{th} ring.

$$\varepsilon_i^i = \varepsilon_{thermal} + \varepsilon_{swelling} + \varepsilon_{densification} \quad (5-21)$$

$\varepsilon_{thermal}$ and $\varepsilon_{densification}$ are the thermal strain and the densification strain, respectively. They are calculated using the existing thermal expansion and densification models in the FRAPCON-3. Fuel thermal expansion takes place immediately as fuel temperature rises and fuel densification occurs when small porosities (1-2 μ m) within fuel particles migrate to the particle surface, causing shrinkage of the fuel particles. The swelling strain, however, is calculated differently:

For porosity larger than ~6%, the solid swelling can be accommodated by the porosities and the bulk swelling strain $\varepsilon_{swelling}=0$, while the porosity of the fuel is reduced by fission products swelling.

For porosity less than or equal to ~6%, the VIPAC fuel is very much like the sintered fuel, therefore, $\varepsilon_{swelling}$ is calculated with the same model as used in for solid fuel.

Lanning and Berna [1995] developed a swelling model which calculates the volumetric swelling as a function of burnup and fuel density:

$$\text{If burnup is less than 6 MWd/kgU, } Soldsw = 0. \quad (5-22)$$

$$\text{If burnup is larger than 6 MWd/kgU } Soldsw = 0.081 \times fdens \times (burnup - 6) \quad (5-23)$$

Where:

burnup = fuel burnup (MWd/kgU)

fdens = fuel density (%)

Soldsw = fuel volumetric swelling due to solid fission products (fraction in percent)

For VIPAC fuel, the swelling can be assumed as follows:

If porosity is larger than 6%, $VIPACSoldsw = 0$ and $Porosity = Porosity - Soldsw$

If porosity is less than 6%, $VIPACSoldsw = \sum \Delta Soldsw$

$VIPACSoldsw$ is the volumetric swelling rate (%) of VIPAC fuel ; $Soldsw$ is calculated by the original FRAPCON-3 swelling model using the VIPAC fuel particle density, burnup; $\sum \Delta Soldsw$ is the accumulated fission product swelling fraction after the porosity is reduced to ~6%.

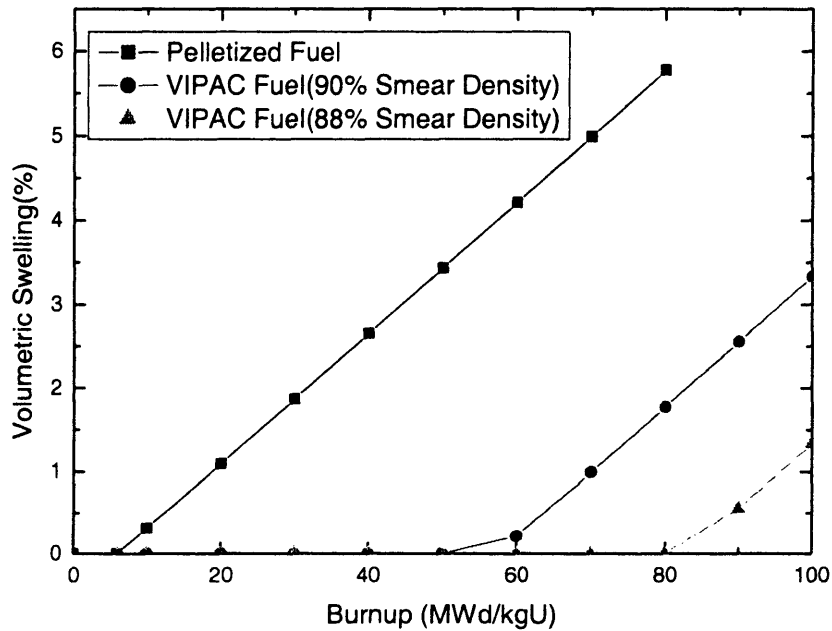


Figure 5.12 The VIPAC Fuel Swelling as a Function of Burnup

The VIPAC fuel swelling is compared with the sintered fuel swelling in Figure 5.12, it can be seen that VIPAC fuel swelling rate is much smaller than the sintered fuel, since its larger porosity accommodates most of the fission products. It is also shown in Figure 5.12 that the larger the VIPAC fuel porosity, the smaller the swelling rate.

5.4 VIPAC Annular Fuel Cladding Interaction Model

For I&E Cooled VIPAC Annular Fuel, the fuel and cladding interaction is very similar to the fuel cladding full contact regime of the sintered annular fuel as described in Section 3.3.4.

Again, equal fuel cladding interfacial pressures are assumed for both the inner and outer interfaces. However, the “lock-up” mechanism is no longer valid for the VIPAC annular case since fuel particles are more mobile and a “slippage” to some extent is expected. Nevertheless, since the fuel particles are still closely packed, the cladding strain can be related to the fuel strain assuming a certain “slipping ratio”. The equations to solve the VIPAC annular fuel interfacial pressure and cladding stresses and strains are as follows:

$$\sigma_{\theta}^i(t^i/r_i^i) + \sigma_{\theta}^o(t^o/r_i^o) = ((r_o^i/r_i^i) - (r_o^o/r_i^o))P_{coolant} \quad (3-34)^*$$

Where superscript o, i, refer to outer and inner cladding respectively; subscript o, i, refer to outer and inner surface of annular fuel respectively. $P_{coolant}$ is coolant pressure, r is radius and t is the cladding thickness, σ_{θ} is the hoop stress.

For ε_{θ} and ε_r , the hoop and radial strains respectively, the aggregated displacement can be written as:

$$\bar{r}_o \varepsilon_{\theta}^o - \frac{t_o}{2} \varepsilon_r^o - \bar{r}_i \varepsilon_{\theta}^i - \frac{t_i}{2} \varepsilon_r^i = u(r) \quad (3-38)$$

$u(r)$ is the aggregated radial displacement of the fuel by thermal expansion, densification and swelling, which is calculated by equations (5-18) to (5-21) and equals $\sum_1^{n-1} \Delta r_n$.

*Equation number same as in Chapter 3.

The equations relating cladding stress and strain are as follows:

$$\varepsilon_{\theta}^o = \frac{1}{E}(\sigma_{\theta}^o - \nu\sigma_z^o) + \varepsilon_{\theta}^{P(o)} + d\varepsilon_{\theta}^{P(o)} + \int_{T_0}^T \alpha_{\theta}^o dT \quad (3-39)$$

$$\varepsilon_z^o = \frac{1}{E}(\sigma_z^o - \nu\sigma_{\theta}^o) + \varepsilon_z^{P(o)} + d\varepsilon_z^{P(o)} + \int_{T_0}^T \alpha_z^o dT \quad (3-40)$$

$$\varepsilon_r^o = -\frac{\nu}{E}(\sigma_{\theta}^o + \sigma_z^o) + \varepsilon_r^{P(o)} + d\varepsilon_r^{P(o)} + \int_{T_0}^T \alpha_r^o dT \quad (3-41)$$

$$\varepsilon_{\theta}^i = \frac{1}{E}(\sigma_{\theta}^i - \nu\sigma_z^i) + \varepsilon_{\theta}^{P(i)} + d\varepsilon_{\theta}^{P(i)} + \int_{T_0}^T \alpha_{\theta}^i dT \quad (3-42)$$

$$\varepsilon_z^i = \frac{1}{E}(\sigma_z^i - \nu\sigma_{\theta}^i) + \varepsilon_z^{P(i)} + d\varepsilon_z^{P(i)} + \int_{T_0}^T \alpha_z^i dT \quad (3-43)$$

$$\varepsilon_r^i = -\frac{\nu}{E}(\sigma_{\theta}^i + \sigma_z^i) + \varepsilon_r^{P(i)} + d\varepsilon_r^{P(i)} + \int_{T_0}^T \alpha_r^i dT \quad (3-44)$$

Where

$\varepsilon_{\theta}, \varepsilon_r, \varepsilon_z$ = cladding hoop, radial and axial strains respectively,

$\sigma_{\theta}, \sigma_r, \sigma_z$ = cladding hoop, radial and axial stresses respectively,

$\alpha_{\theta}, \alpha_r, \alpha_z$ = thermal expansion coefficient in the hoop, radial and axial direction

E is Young's modulus and ν is Poison's ratio and superscript p denotes plastic

$\varepsilon_z^o, \varepsilon_z^i$ are treated as known parameters. Since the lock-up mechanism no longer applies,

$\varepsilon_z^o, \varepsilon_z^i$ cannot be assumed equal to the fuel strains. It is then assumed that these cladding strains are equal to the average of the fuel strains and free-standing cladding strains.

$$\varepsilon_z^o = \varepsilon_z^i = \frac{\varepsilon_z^{fuel} + \varepsilon_z^{free_standing_cladding}}{2} \quad (5-24)$$

free standing cladding strain $\varepsilon_z^{free_standing_cladding}$ can be calculated using equations (3-20) to (3-22) and (3-30).

Again, the equations from (3-38) to (3-44) form the following matrix:

$$\begin{pmatrix} A(1) & 0 & C(1) & 0 & 0 & 0 & 0 & 0 \\ 0 & 0 & 0 & 0 & E(2) & F(2) & G(2) & H(2) \\ 0 & 0 & C(3) & D(3) & E(3) & 0 & 0 & 0 \\ 0 & 0 & C(4) & D(4) & 0 & 0 & 0 & 0 \\ 0 & 0 & C(5) & D(5) & 0 & F(5) & 0 & 0 \\ A(6) & B(6) & 0 & 0 & 0 & 0 & G(6) & 0 \\ A(7) & B(7) & 0 & 0 & 0 & 0 & 0 & 0 \\ A(8) & B(8) & 0 & 0 & 0 & 0 & 0 & H(8) \end{pmatrix} \begin{pmatrix} \sigma_{\theta}^i \\ \sigma_z^i \\ \sigma_{\theta}^o \\ \sigma_z^o \\ \varepsilon_{\theta}^o \\ \varepsilon_r^o \\ \varepsilon_{\theta}^i \\ \varepsilon_r^i \end{pmatrix} = \begin{pmatrix} I(1) \\ I(2) \\ I(3) \\ I(4) \\ I(5) \\ I(6) \\ I(7) \\ I(8) \end{pmatrix} \quad (3-45)$$

where,

$$A(1) = t_i; \quad C(1) = t_o;$$

$$E(2) = \bar{r}_o; F(2) = -\frac{t_o}{2}; G(2) = -\bar{r}_i; H(2) = -\frac{t_i}{2}; I(2) = u(r)$$

$$C(3) = -\frac{1}{E}; D(3) = \frac{\nu}{E}; E(3) = 1; I(3) = \varepsilon_{\theta}^{P(o)} + d\varepsilon_{\theta}^{P(o)} + \int_{T_0}^T \alpha_{\theta}^o dT$$

$$C(4) = \frac{\nu}{E}; D(4) = \frac{-1}{E}; I(4) = -\varepsilon_z^o + \varepsilon_z^{P(o)} + d\varepsilon_z^{P(o)} + \int_{T_0}^T \alpha_z^o dT$$

$$C(5) = \frac{\nu}{E}; D(5) = \frac{\nu}{E}; F(5) = 1; I(5) = \varepsilon_r^{P(o)} + d\varepsilon_r^{P(o)} + \int_{T_0}^T \alpha_r^o dT$$

$$A(6) = -\frac{1}{E}; B(6) = \frac{\nu}{E}; G(6) = 1; I(6) = \varepsilon_{\theta}^{P(i)} + d\varepsilon_{\theta}^{P(i)} + \int_{T_0}^T \alpha_{\theta}^i dT$$

$$A(7) = \frac{\nu}{E}; B(7) = \frac{-1}{E}; I(7) = -\varepsilon_z^i + \varepsilon_z^{P(o)} + d\varepsilon_z^{P(o)} + \int_{T_0}^T \alpha_z^o dT$$

$$A(8) = \frac{\nu}{E}; B(8) = \frac{\nu}{E}; H(8) = 1; I(8) = \varepsilon_r^{P(i)} + d\varepsilon_r^{P(i)} + \int_{T_0}^T \alpha_r^i dT$$

These equations are solved using Gaussian elimination.

5.5 VIPAC Annular Fuel Fission Gas Release Model

5.5.1 Overview

It was observed in experiments that sphere-pac fuel released larger percent of fission gas than pelletized fuel at comparable power level [Delbrassine and Smith, 1980]. For pins irradiated at 65-70kW/m up to 7,7% fima, ~13% of the fission gas releases were observed in both pellets and VIPAC fuel. For pins with 90-95 kW/m linear power, the sphere-pac fuel had higher fission gas release (33%-45%) than the pelletized fuel (22%-37%). A fuel assembly consisting of 4 pins filled with a blend of 3 sizes of UO₂ spheres were irradiated at Halden reactor. The fuel had ~87-88% smear density and achieved burnup of 13.7 MW-days/kg of UO₂ and the percentage fission gas release was 24%. [Van der Linde, 1982]

The VIPAC fuel fission gas release was higher due to the larger fuel surface exposed to the free volume and to interconnected channels for fission gas migration and release.

Although the VIPAC fuel has lower thermal conductivity, the I&E cooled VIPAC annular fuel temperatures are still significantly lower than the solid fuel (Figure 1.4) and temperatures still fall below the ~800 °C when fission gas diffusion through the temperature gradient takes place. Therefore, recoil and knockout are the dominating mechanisms for VIPAC annular fuel fission gas release.

The fission gas release from the I&E cooled VIPAC annular fuel includes the following processes: (1) fission gas atoms are generated during the fission process; (2) fission gas atoms are either directly ejected out of the fuel particles or knocked out by other fission fragments; (3) since the fuel particles are closely packed, some fission gas atoms are absorbed by the fuel matrix and resolute in the matrix; (4) the remaining fission gas atoms are released to the free volume.

Therefore, it is important to develop a simplified low temperature high burnup fission gas release model for the I&E cooled annular fuel to capture the surface effect and resolution effect.

5.5.2 High Burnup VIPAC Fuel Athermal Fission Gas Release Model

Again, the athermal fission gas release model used in FRAPCON-3 has been expressed as a function of burnup:

$$F = 7 \times 10^{-5} \cdot B + C \quad (3-55)$$

where, F=the fission gas release fraction

B=burnup in MWd/kgU

C=0 for B<40 MWd/kgU

C=0.001*(B-40) for B>40MWd/kgU

The above model was derived for a typical LWR fuel. However, for VIPAC annular fuel, the surface effect should be taken into account. The fuel surface to volume ratio β for the solid and the VIPAC annular fuel are expressed as:

Solid fuel:

$$\beta_{solid} = \frac{2\pi r^2 + 2\pi r l}{\pi r^2 l} = 2\left(\frac{1}{l} + \frac{1}{r}\right) \quad (3-56)$$

where l =pellet length(m)

r =pellet radius(m)

VIPAC Annular fuel:

$$\beta_{VIPAC} = \frac{\# \text{ of particles} \times \text{area of each particle}}{\text{volume of fuel}} = \frac{V_{fuel} / V_{particle} \times A_{particle}}{V_{fuel}} = \frac{4\pi R^2}{4/3\pi R^3} = \frac{3}{R} \quad (5-25)$$

where R =VIPAC fuel particle Radius (m)

The solid fuel values are taken from the reference solid fuel design APPENDIX A, where

$$l=13.4\text{mm}, \quad r=4.095\text{mm}, \quad \beta_{solid}=637.7 \text{ m}^{-1}.$$

Let ξ be the athermal surface release enhancement factor,

$$\xi = \beta_{VIPAC_annular} / \beta_{solid} = \frac{1}{R} \times 4.7 \times 10^{-3} \quad (5-26)$$

Where

ξ = athermal surface release enhancement factor

R = VIPAC fuel particle Radius (m)

Since the fission gas atoms ejected from the fission process have a travel distance of ~10-15 μm and the fuel particles are closely packed, it is conceivable that not all the fission gas atoms leaving the fuel surface are released to the free volume and some of fission gas atoms can be trapped or resolute in the fuel matrix. Although annular fuel has rim effects at both the inner and outer surfaces, the effect is insignificant at low burnup. Hence, rim effect is not considered at low burnup. The ratio of fission gas release to resolution can be estimated by the ratio of fuel porosity volume to fuel volume. Therefore the fission gas release enhancement factor is approximated by

$$f = \xi \times P \quad (5-27)$$

Where

f = fission gas release enhancement factor

ξ = athermal surface release enhancement factor

P = VIPAC fuel porosity

At low burnups, the fission gas release from the VIPAC annular fuel rod can be calculated by scaling the fission gas release from the solid fuel rod with the fission gas release enhancement factor f , which combines the effects of surface release and resolution. At high burnups, the solid fuel experiences microstructure change and surface to volume ratio becomes much larger and surface effects of the VIPAC fuel are diminished. Therefore, the fission gas release

enhancement factor is reduced. As described in Section 5.5.1, the fission gas release from the particle fuel is about 1.5-2.0 times that from the solid fuel. Therefore, it is reasonable to assume that high burnup fission release enhancement factor is about 2.

Then the low temperature athermal fission gas release model for the VIPAC fuel can be expressed as:

For Burnup $\leq 40 \text{ MWd/kgU}$

$$F = 7 \times 10^{-5} \cdot f \cdot \text{Burnup} \quad (5-28)$$

For Burnup $> 40 \text{ MWd/kgU}$

$$F = 7 \times 10^{-5} \cdot f \cdot 40 + 0.001 \cdot (\text{Burnup} - 40) \cdot 2 \quad (5-29)$$

where,

F = the fission gas release fraction

f = fission gas release enhancement factor

Burnup = burnup in MWd/kgU

The athermal fission gas release from pelletized fuel and VIPAC fuel are compared in Figure 5.13 and Figure 5.14. The VIPAC fuel fission gas release is generally higher than pelletized fuel.

At the same smear density and with smaller particle size, the fission gas release is higher because the total fuel surface exposed to free volume is larger, which allows more fission gas atoms be released to the free volume by recoil and knock-out. (Figure 5.13)

At the same particle size, the VIPAC fuel with higher smear density tends to release smaller amount of fission gas than fuels with lower smear density because the resolution of fission gas is more prominent for fuels with higher smear densities and the resolution effect offset some of the surface fission gas release contribution. (Figure 5.14)

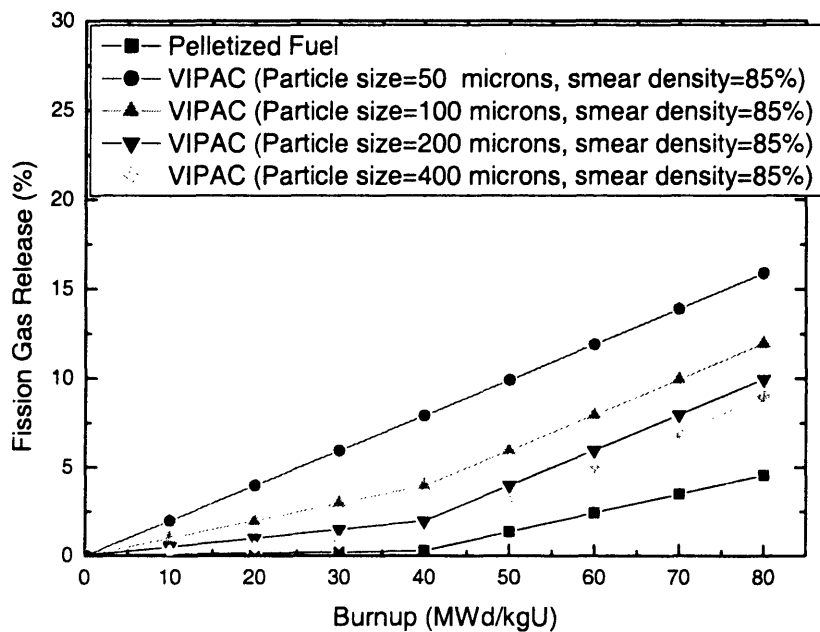


Figure 5.13 Comparison of Athermal FGR from Pelletized Fuel and VIPAC Fuels with Various Particle Sizes

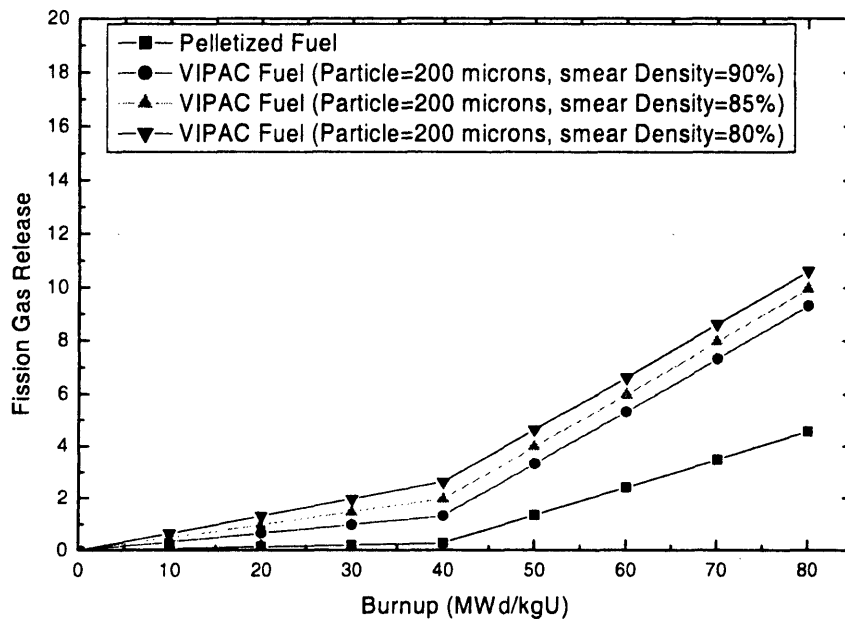


Figure 5.14 Comparison of Athermal FGR from Pelletized Fuel and VIPAC Fuels with Various Smear Densities

5.6 Summary

A VIPAC fuel thermal conductivity model has been developed and implemented in the fuel performance code. The new empirical VIPAC thermal conductivity model is a function of temperature, burnup, porosity and gas pressures. The model has been validated with experimental data.

The VIPAC fuel and cladding thermal conductance model has also been developed by modifying the original FRAPCON thermal conductance model. The model incorporates the effects of fuel particle size, gas pressure and interfacial pressure, etc. The model has been compared with experimental data and a sensitivity analysis has been performed on each variable in the model.

The VIPAC fuel dimensional changes resulting from thermal expansion, densification and swelling have been calculated based on appropriate assumptions. It is assumed that bulk thermal expansion and densification rate for the VIPAC fuel is the same as that of the pelletized fuel and the fission product swelling is accommodated by the large porosity of the VIPAC fuel until the VIPAC fuel porosity is reduced to about 6%, which is comparable with the sintered fuel.

The VIPAC fuel-cladding mechanical interaction mechanism is similar to the full gap closure regime of the pelletized annular fuel. Therefore, a model for VIPAC annular fuel cladding interaction has been derived by modifying the sintered annular fuel cladding interaction model. Fuel cladding “slippage” is allowed in the VIPAC annular fuel model.

An athermal fission gas release model for VIPAC annular fuel has been developed based on FRAPCON-3 low temperature fission gas release model. The surface effect and the resolution effect have been incorporated in the model.

Chapter 6 The VIPAC Annular LWR Fuel Performance and Design

6.1 Comparison of the VIPAC Fuel with the Sintered Fuel

6.1.1 Input Parameters of Different Cases

The VIPAC fuel performance models as presented in Chapter 5 have been implemented in the FRAPCON-ANNULAR (VIPAC) code and a representative VIPAC annular fuel 150% power case has been analyzed with the code. The 150% power cases are associated with 50% higher coolant velocity. The results are compared with the sintered annular fuel 150% power case and the reference Westinghouse solid LWR fuel 100% power case. It is notable that the 50% increase in power density cannot be attained using the solid fuel.

The input parameters for the reference Westinghouse 100% solid LWR fuel case and 150% power sintered annular fuel case are the same as presented in Section 4.2.1. The VIPAC annular fuel is assumed to be composed of UO_2 sphere-shaped particles which have a particles size (diametral) of 400 μm . Since the fuel particles are closely packed within the concentric cladding, the as-fabricated gaps between the fuel and the claddings are non-existent. The VIPAC fuel has a smear density of 85%. At the top section of the fuel rod, a rod plenum with ~7% of the rod length is included to accommodate the fission gas release. The VIPAC fuel is boned by helium gas with a pressure of 1.4 MPa to enhance the fuel thermal conductivity and fuel cladding conductance. For the 150% power VIPAC annular case, the power history is assumed to be the same as the 150% power sintered annular fuel case, which starts with a rod average linear power of ~78kW/m and follows a decreasing trend (see Figure 4.4). The axial power shape for the VIPAC annular fuel is assumed to be chopped cosine with peak to average ratio of 1.3. The VIPAC fuel is to be irradiated for ~ 1500 effective full power days (EFPDs) and achieve a burnup of 97MWd/kgU, which is 13% higher than that of the 150% power sintered fuel case due to the smaller fuel content. The external coolant conditions of the

VIPAC annular case are the same as the sintered annular fuel. The parameters of the 150% VIPAC and sintered annular fuels and the solid fuel are listed in Table 6.1.

Table 6.1 Key Design & Operating Parameters for Annular and Solid Fuel Pins

	Sintered annular rod	VIPAC annular rod	Reference solid rod
Cladding outside diameter (mm)	Outer: 15.4	Outer: 15.4	9.5
	Inner: 8.6	Inner: 8.6	
Cladding thickness (mm)	Outer: 0.6	Outer: 0.6	0.6
	Inner: 0.6	Inner: 0.6	
Cladding material	Zircaloy-4	Zircaloy-4	Zircaloy-4
Fuel pellet diameter (mm)	Outer: 14.1	Outer: 14.2	8.2
	Inner: 9.5	Inner: 9.8	
Diametral gap size (mm)	Outer: 0.12	Outer: 0.0	0.16
	Inner: 0.12	Inner: 0.0	
Fuel pellet height(mm)	13.4	–	13.4
Fuel stack height(m)	3.66	3.66	3.66
Fuel volume(mm ³) at100% theoretical density	2.9×10 ⁵	2.6×10 ⁵	1.9×10 ⁵
Plenum length(mm)	250	250	250
Plenum volume(mm ³)	2.1×10 ⁴	2.1×10 ⁴	1.4×10 ⁴
External coolant pressure (MPa)	15.5	15.5	15.5
Initial fuel density	95%TD	85%Smear	95%TD
Fuel particle size(μm)	-	400	-
Initial helium pressure (MPa)	1.4	1.4	1.4
Initial rod average linear power(kW/m)	78 (150% Power)	78 (150% Power)	30 (100% Power)
Peak to average ratio	1.3	1.3	1.3
EOL burnup(MWd/kgU)	85.8 (150% Power)	97 (150% Power)	50.7 (100% Power)

6.1.2 Fuel Temperatures Comparisons

The 150% power VIPAC annular fuel average temperatures are compared with the sintered annular fuel case of the same power level and the reference solid fuel with 100% power in Figure 6.1.

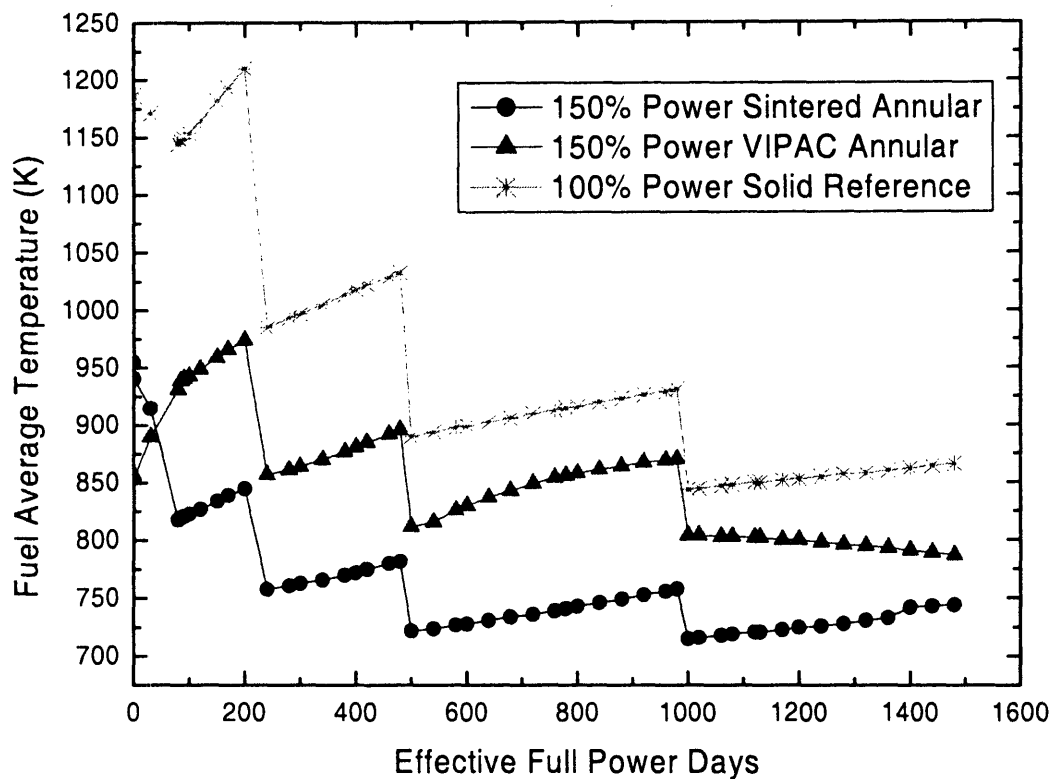


Figure 6.1 Comparisons of Average Temperatures of the VIPAC Annular, the Sintered Annular and the Reference Solid Fuel Rods

The VIPAC and the sintered annular fuel average temperatures are lower than those of the solid fuel although the annular cases have 50% higher power. During the first ~50 EFPDs, the VIPAC annular fuel average temperatures are lower than those of the sintered annular fuel. The VIPAC fuel has no fuel cladding gaps and the temperature drops at the fuel cladding interfaces are much lower, resulting in lower average fuel temperature although the thermal conductivity of the VIPAC fuel is relatively lower than that of the sintered fuel. However, the sintered

annular fuel cladding gaps close after 50 effective full power days, and the sintered annular fuel average temperature drops below the VIPAC fuel since the sintered fuel has higher thermal conductivity. Near the end of life, after about 1000 effective full power days, the difference between the VIPAC and the sintered annular fuel temperatures becomes much smaller because at high burnup, the build-up of the solid fission products reduces the VIPAC fuel porosity and increases its thermal conductivity.

6.1.3 Fission Gas Release Comparisons

For the 150% power VIPAC annular fuel, although the linear power is very high, the fuel average temperatures are still low (Figure 6.1) and fission gas release is mainly from the knock-out and recoil of fission gas atoms. The athermal fission gas release from the VIPAC fuel is calculated by the model described in Section 5.5. The fission gas releases of the VIPAC, the sintered annular fuel and the reference solid fuels are compared in Figure 6.2.

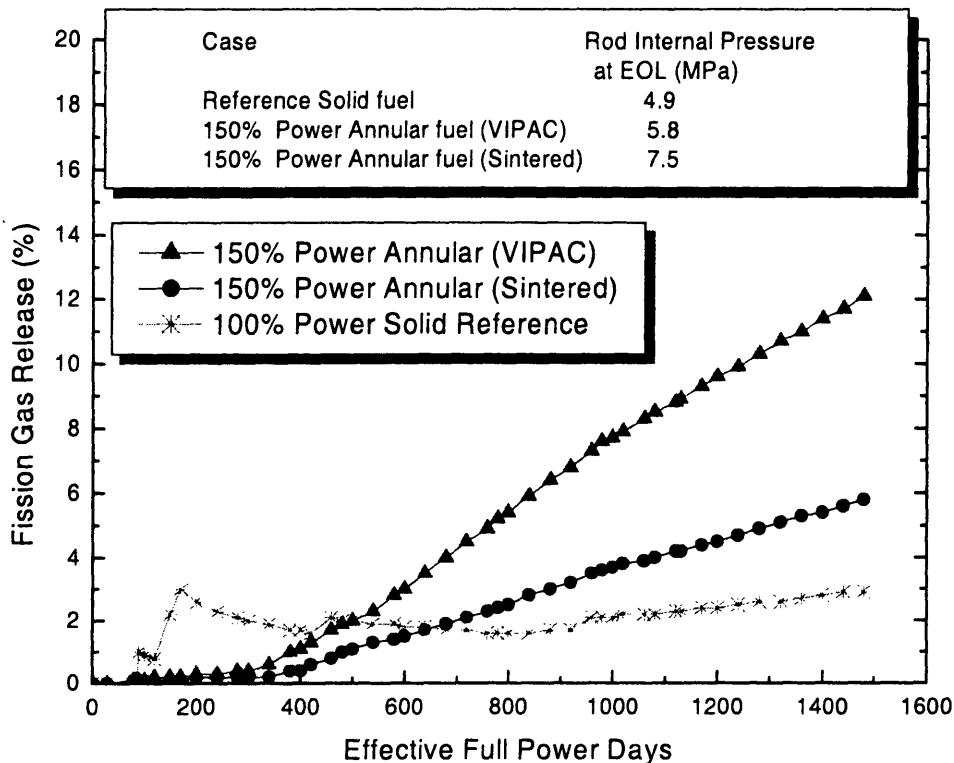


Figure 6.2 Comparison of Fission Gas Releases (and Rod Pressure at EOL).

As shown in Figure 6.2, the VIPAC fuel fission gas is high compared with the sintered fuel (~12% at EOL for VIPAC annular, ~6% at EOL for 150% power sintered annular and 3% for 100% reference solid fuel). The VIPAC fuel fission gas release in our case can be explained by the larger surface to volume ratio which facilitates fission atoms escaping the fuel by recoil and knock-out. In addition, operating at the same power history as the sintered fuel, the VIPAC fuel end of life burnup is higher due to lower heavy metal content (at 85% smear density). Hence the fission gas release is also enhanced by higher burnup.

Although VIPAC fuel fission gas release turns out to be larger than the sintered annular fuel, the EOL VIPAC fuel rod internal pressure at the end of life is lower than that of the sintered annular fuel. The VIPAC fuel rod has larger free volume due to larger porosity ($1.04 \times 10^{-4} \text{ m}^3$ total free volume for VIPAC annular fuel case and $3.94 \times 10^{-5} \text{ m}^3$ for sintered annular fuel). The fission gas release of VIPAC fuel can be reduced by enlarging the fuel particle size (reducing the surface to volume ratio) or reducing the fuel porosity (allowing higher resolution rate). However, to attain high smear density, the particle size has to be smaller than $500 \text{ }\mu\text{m}$, at least for a portion of the fuel.

6.1.4 Comparison of Cladding Performances of the VIPAC and Sintered Fuel

The cladding oxide thickness is dictated mainly by the cladding temperature, cladding heat flux and fast neutron flux, while the cladding hydrogen accumulation is dictated by the cladding oxide thickness and cladding temperature. The VIPAC annular fuel has the same operating power history as the sintered annular fuel. The cladding average temperatures are compared in Figure 6.3 for the VIPAC, sintered annular and solid reference cases. The cladding temperatures for the VIPAC annular fuel and the sintered annular fuel are comparable. The inner cladding temperatures are higher than the solid reference rod cladding temperatures due to higher heat fluxes. Whereas, the outer cladding temperatures for both annular cases are lower than the reference solid fuel. Since the external coolant temperatures are assumed the same for all three cases and operating periods are equal, it is expected that the VIPAC cladding oxide thickness and hydrogen concentration would be comparable to those of the sintered annular fuel, which can be seen in Figure 6.4 and Figure 6.5.

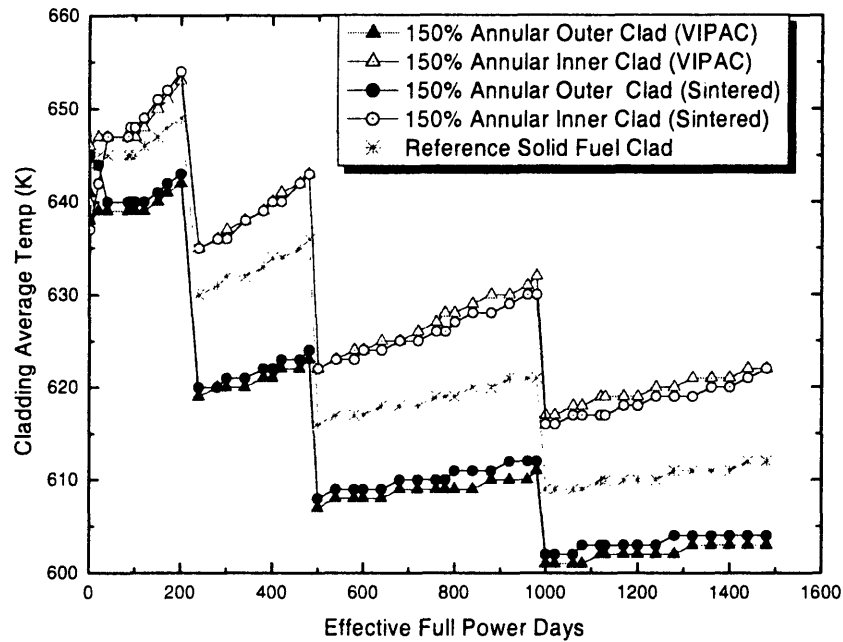


Figure 6.3 Comparisons of the Average Cladding Temperatures

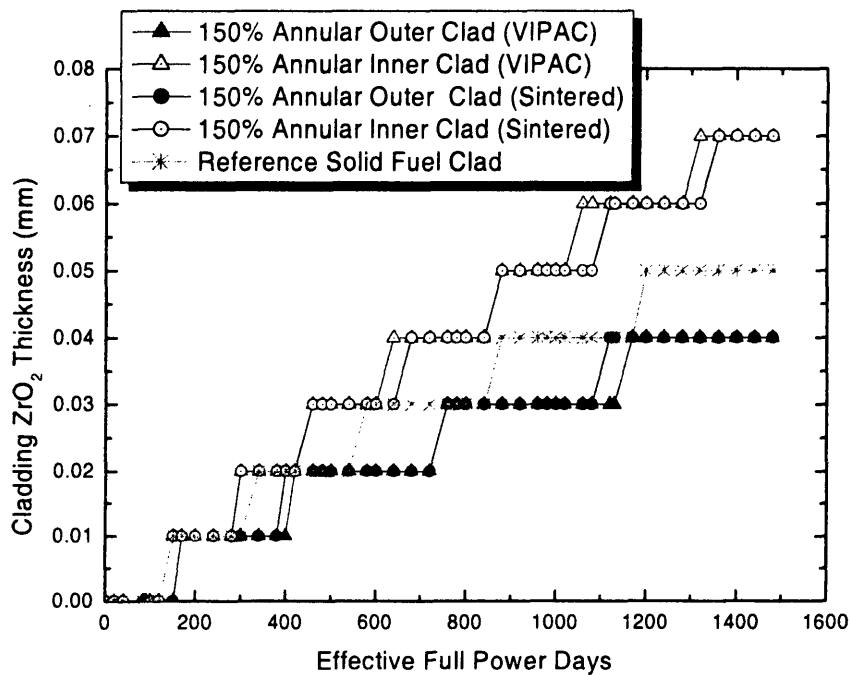


Figure 6.4 Comparisons of Cladding Oxide Layer Thickness

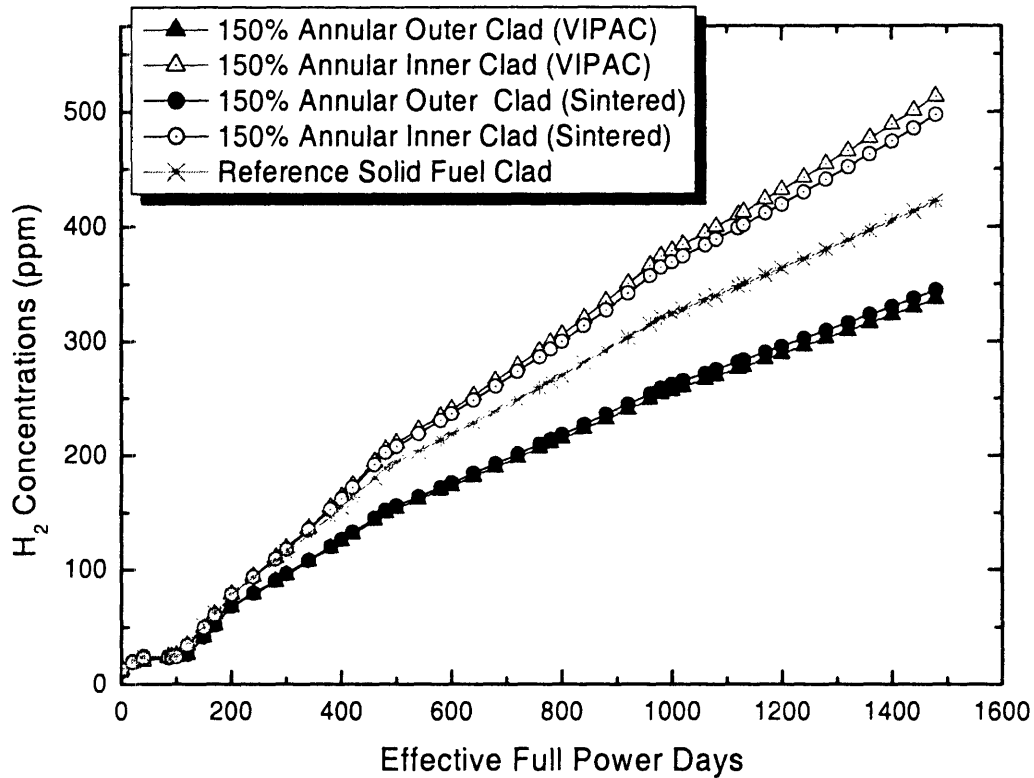


Figure 6.5 Comparisons of Cladding H₂ Concentrations

Since in a VIPAC annular fuel rod, there are no fuel-cladding gaps, the cladding creep-down during the initial operating period due to external pressure could be alleviated. At high burnup, the presence of larger porosity in the VIPAC fuel can accommodate the fuel solid fission products. Therefore, the effect of the VIPAC fuel-cladding mechanical interactions is expected to be less severe than the sintered fuel. The rod average cladding strains for different cases as a function of EFPDs are shown in Figure 6.6.

At beginning of life, the sintered annular fuel experiences larger strains, at both the inner and outer claddings, due to creep-down, while for VIPAC annular fuel, the initial cladding strains are much less. Thermal expansion of VIPAC fuel causes positive outer cladding strains and negative inner cladding strains, while the effect of densification causes reduction of cladding strain at both claddings. The gradient of cladding strain increment over operation period is

much smaller for VIPAC annular fuel than for sintered annular since the swelling effect of the VIPAC fuel is reduced by its larger porosity.

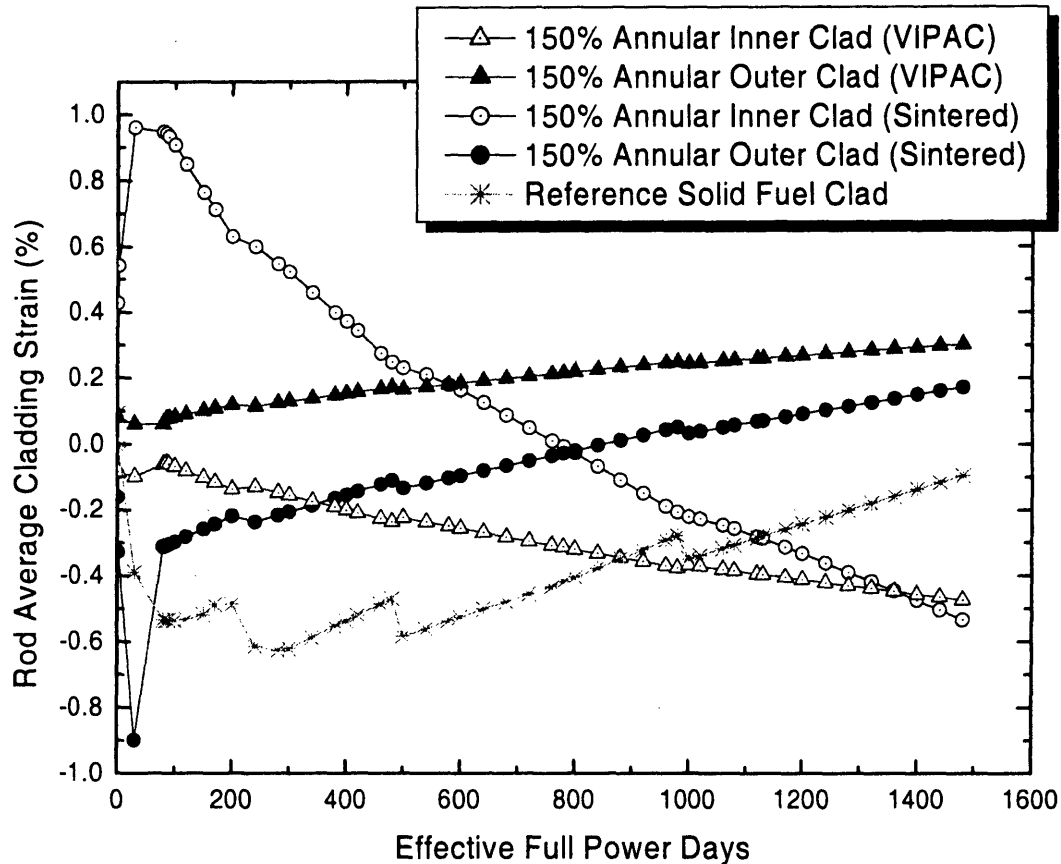


Figure 6.6 Comparisons of Cladding Strains

Compared with sintered fuel, the VIPAC fuel also provides a “softer” contact between the fuel and cladding, which effectively reduces the intensity of fuel-cladding mechanical interactions and avoids stress concentrations. Therefore, the VIPAC rod cladding performance is relatively better than the sintered annular fuel.

6.1.5 The VIPAC Annular Fuel Gap Conductance Symmetry

The gap conductance asymmetry problem for the sintered annular, which was discussed in Section 4.3, has posed serious concern for fuel rod safety. The gap conductance asymmetry is generated by the fuel thermal expansion which results in gap differentials. However, for the VIPAC annular fuel, this is no longer a problem since the fuel particles are adequately mobile to readjust themselves to fill the space between the concentric claddings. Therefore, the gaps for both the inner and outer fuel cladding interfaces are always kept closed. The gap conductance symmetry is well maintained. (Figure 6.7) The large gap conductance degradation during irradiation (12,000-14,000W/m-K at the BOL compared with ~4,000W/m-K at EOL) can be explained by deterioration of the gas conductivity due to high fission gas release (~12% at EOL for this case).

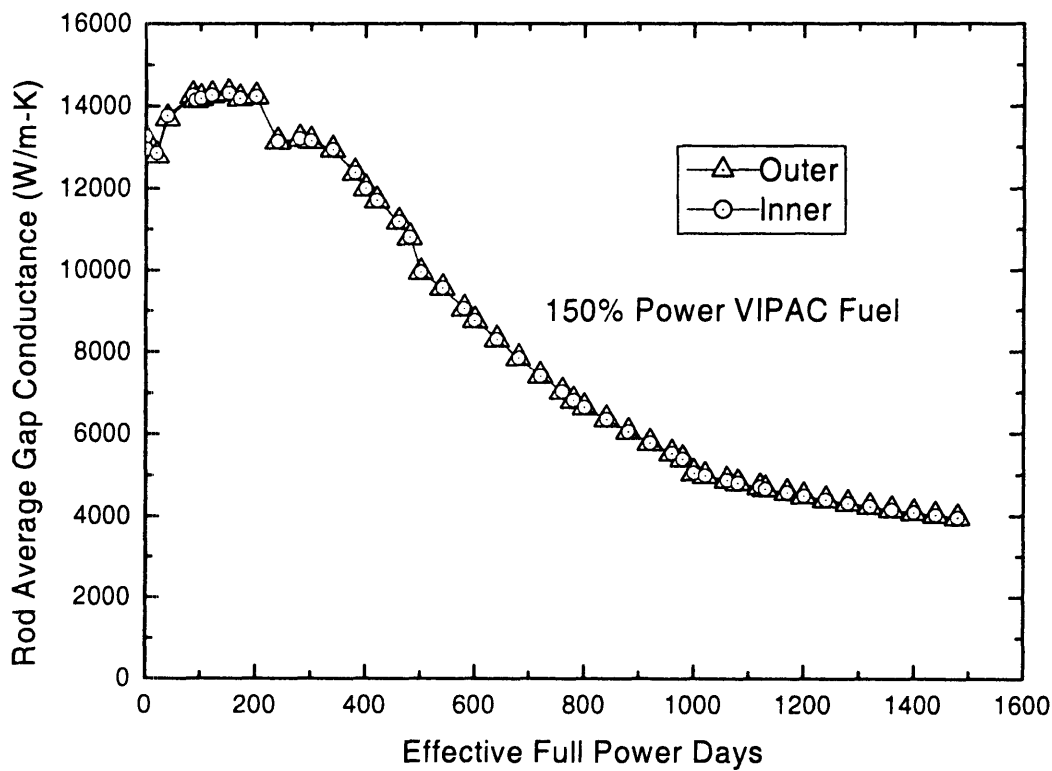


Figure 6.7 Gap Conductance Symmetry of VIPAC Annular Fuel

6.2 Parametric Study and Design Optimization of VIPAC Annular Fuel

6.2.1 Optimization of Initial Helium Gas Pressure

The VIPAC annular rod should be filled with helium gas to enhance the thermal conductivity of the VIPAC fuel and the conductance of the fuel-cladding interface. As discussed in Section 5.1 and 5.2, the VIPAC fuel thermal conductivity is sensitive to the gas pressure since the gas serves as a major medium of heat transfer between fuel particles. The heat transfer between the fuel particles and cladding at the fuel-cladding interface is also much affected by helium gas pressure. Hence, it is imperative to optimize the initial helium pressure in the VIPAC fuel design. In Figure 6.8, the end of life rod internal pressures and the fission gas releases at different initial gas pressure is illustrated.

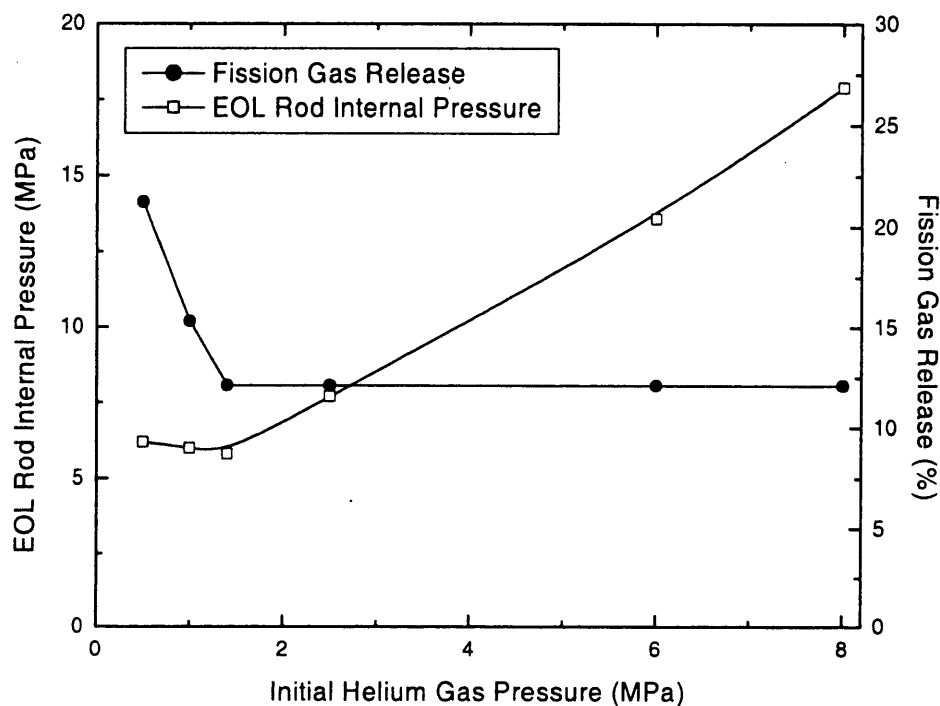


Figure 6.8 Impact of Initial Helium Pressure on EOL FGR and Rod Pressure

All the VIPAC fuel parameters in this case are the same as those presented in Table 6.1 except for the initial helium gas pressure, which varies from 0.5 MPa to 8 MPa. It can be seen in Figure 6.8 that at initial helium gas pressures ranging from ~1.4 MPa to 8 MPa, the fission gas release at EOL stays consistent while the rod internal pressure at EOL increase proportionally with the initial pressure. This can be explained by the fact that although increasing the helium gas pressure increases the VIPAC fuel thermal conductivity and reduces the fuel temperatures, the fission gas release does not increase since the athermal release mechanisms dominate and fuel temperatures do not matter because they all fall below the regime where the diffusion release process becomes significant. Therefore, increasing the helium gas pressure at this range does not reduce the fission gas release, yet it is important for EOL rod pressures.

However, for initial gas pressures lower than 1.4 MPa, lower helium gas pressure causes the fuel temperatures to reach the diffusion release regime due degraded fuel thermal conductivity, and the fission gas release is enhanced (from ~12% at 1.4 MPa to ~23% at 0.5MPa). Although fission gas release increases, the EOL rod pressure only sees a modest increase since the EOL rod pressure is collectively affected by high fission gas release and reduced initial internal pressure.

With the addition of fission gas, the gas conductivity will deteriorate and total gas pressure will increase: the former decreases the VIPAC thermal conductivity fuel while the latter enhances the conductivity. To avoid high fission gas release and high end of life rod pressure, the optimum initial helium gas pressure should fall between ~1.4 to 2.0 MPa as shown in Figure 6.8.

6.2.2 Optimization of VIPAC Fuel Particle Size

The VIPAC fuel particle size has several impacts on fuel performance in several ways : (1) the fuel cladding interface thermal conductance is a function of the particle size since the effective jump distance between fuel and cladding is affected by particle size; (2) particles of different sizes have different surface to volume ratio, affecting the fission gas release, especially for low temperature fission gas release; (3) the particle size affects the maximum achievable fuel smear

density, which in turn has an impact on fuel performance

A sensitivity analysis has been performed for different particle sizes (diametral) using the 150% power VIPAC annular fuel case as presented in Section 6.1. All other parameters remain unchanged. The fuel particles are assumed to be UO_2 spheres which are closely packed. Since the fission gas release is the main concern for VIPAC fuel, the fission gas releases for different particle sizes have been calculated and the results are shown in Figure 6.9.

It is found that for VIPAC fuel with small particle sizes (below $400\text{ }\mu\text{m}$), the fission gas release can be very high ($\sim 23\%$ for VIPAC fuel with $50\text{ }\mu\text{m}$ particle size) due to large surface to volume ratio, which allows more fission atoms to escape the fuel to the free volume by recoil and knock-out. On the other hand, for VIPAC fuel with very large particle sizes (larger than $600\text{-}800\mu\text{m}$), the fission gas release will also increase due to higher fuel temperatures caused by low thermal conductances at fuel cladding interface.

Therefore, the fission gas release is dictated by two competing effects, the fuel surface to volume ratio and the fuel temperature that is affected by fuel cladding thermal conductance, both of which are affected by the fuel particle size. Larger particle size results in smaller surface to volume ratio and large jump distance at the fuel-cladding interface: the former reduces the gas release while the latter increase the fuel temperature and increase the gas release. Conversely, smaller particle size tends to enlarge the surface to volume ratio while reducing the fuel temperature drops at fuel cladding interface. It is found that the optimum particle size is about $300\text{-}600\mu\text{m}$, where fission gas release is the lowest. (Figure 6.9)

It is worth noting that the above calculations are performed assuming the same fuel smear density (85% in this case). However, such a smear density might not be achievable with large particle sizes or certain spherical particle shapes. With a lower smear density, the fission gas release will be higher since the re-solution rate of the fission gas is reduced.

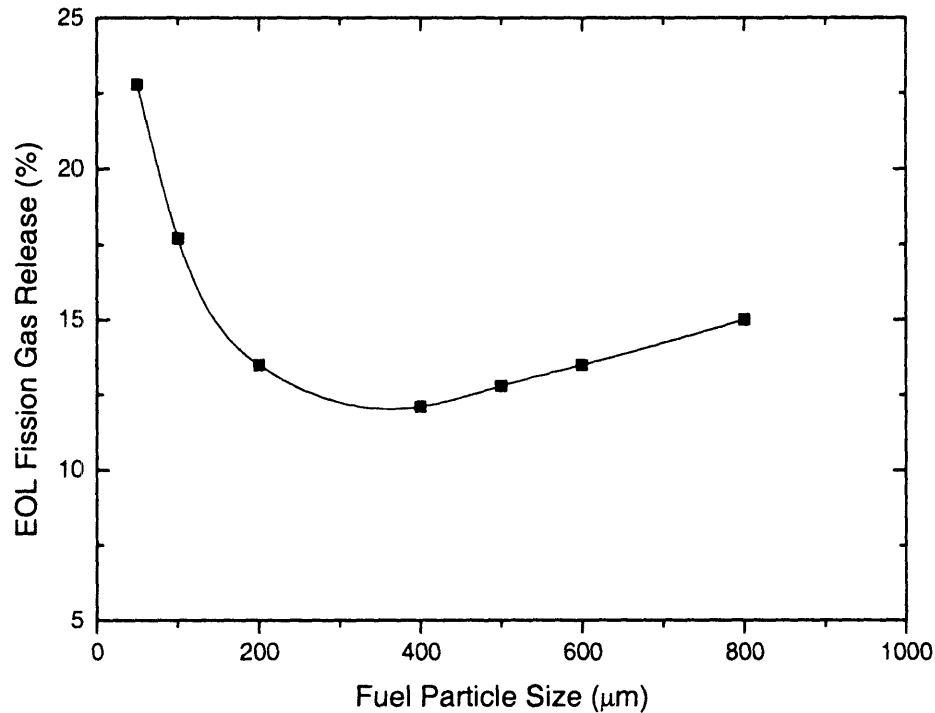


Figure 6.9 The Impact of Fuel Particle Size on FGR

6.2.3 Optimization of VIPAC Fuel Smear Density

In general, it is desirable to achieve high fuel smear density to improve the heavy metal content in a VIPAC fuel rod. However, a higher fuel smear density can increase the rod internal pressure and accommodate less fission products, both of which may pose concerns for fuel performance. In addition, it is technically more difficult to achieve high smear densities.

The 150% power VIPAC case in Table 6.1 is used to evaluate the effect of different smear densities. All the parameters are kept the same except for the fuel smear density. The particle size is set to be 400 μm , assuming it is able to achieve smear densities from 80% to 94%. It is expected that higher smear density will allow smaller porosity, which decreases the fuel temperature by increasing the fuel thermal conductivity and increasing the re-solution of the

fission gas in the fuel, reducing the fission gas release. The impacts of the VIPAC fuel smear density on fission gas release and EOL rod internal pressures are shown in Figure 6.10.

At higher smear density, the fission gas release is reduced. At 80% smear density, the fission gas release is about 15% whereas, at 94% smear density, it is reduced to about 11%. The reduction of fission gas release is very prominent for smear density increases between 80% to 85%. This is because the higher smear density improves the fuel thermal conductivity hence reducing the fuel temperatures, which in effect reduces the fission gas release by diffusion process. While for smear density higher than 85%, the reduction of fission gas release is modest because the fuel temperatures are already in the low temperature release regime and with higher smear densities, the reduction of FGR is only contributed by higher re-solution rate.

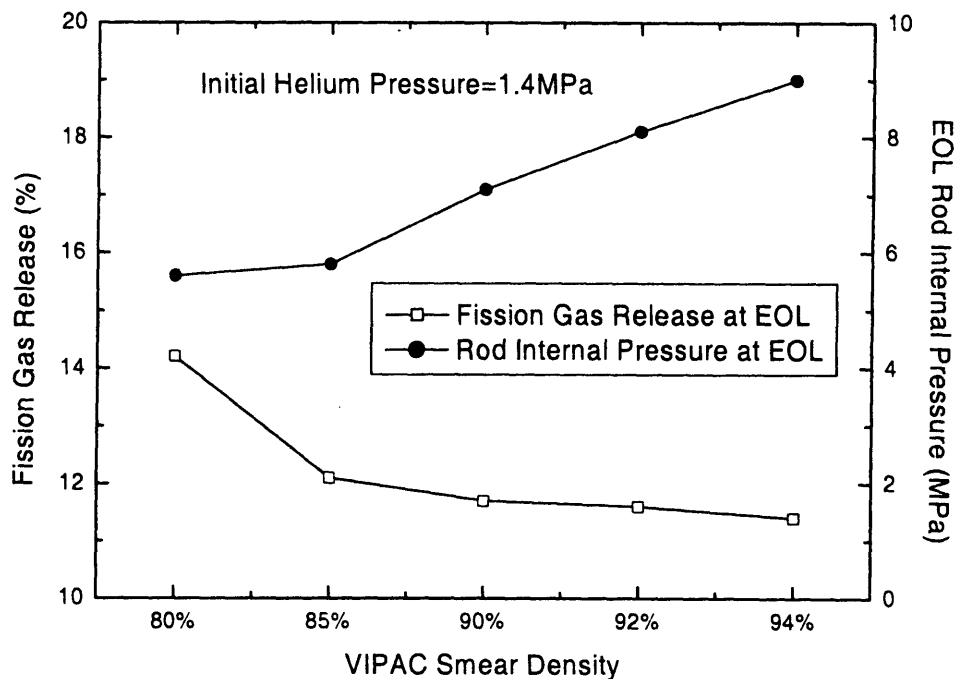


Figure 6.10 Impact of Fuel Smear Density on EOL FGR and Rod Pressure

It is also seen in Figure 6.10 that higher smear density can result in higher EOL rod pressure due to reduced free volume. At 94%, the EOL rod pressure increases to 9 MPa, and this can be

a concern for fuel safe operation. The smear density impacts on the cladding strains are illustrated in Figure 6.11.

It is found that both the inner and outer cladding strains initially decrease with the smear density (from 80% to 90% smear density) and then increase with smear densities higher than 90%. The initial decrease of the cladding strains is explained by improved thermal conductivity which results in lower fuel temperature and less thermal expansion. The increase of the strains at higher smear density is contributed by two competing factors, the reduction of the thermal expansion due to improved thermal conductivity and the increase of the swelling contribution due to reduced fission products accommodation capability.

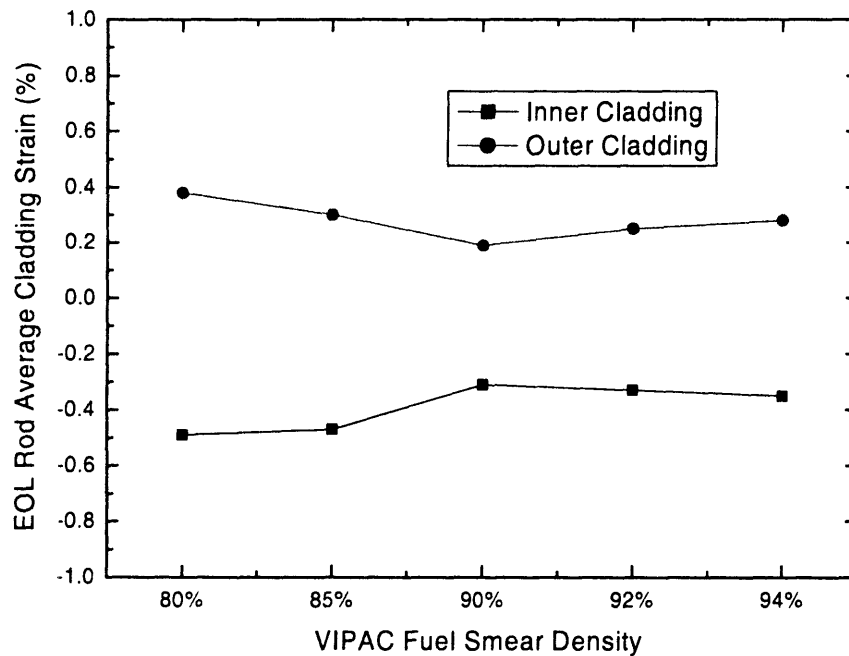


Figure 6.11 Impact of Fuel Smear Density on Cladding Strain

Therefore, the optimum smear density is in the range of 85%-90%, which allows low fission gas release, low EOL rod pressure and small cladding strains.

It is assumed in this analysis that a certain smear density can be achieved with fixed particle size. However, manufacturing test revealed that high smear density can only be achieved with mixed particles sizes [Hamilton, 2003]. The VIPAC fuel was manufactured at AECL with three fractions: UO_2 powders with particle sizes of 250-500 μm , UO_2 powders with particle sizes of 25-53 μm and depleted uranium powders. The achievable smear density is shown in Table 6.2.

Table 6.2 Summary of Fuel Packing Results at AECL [Hamilton, 2003]

Assembly Number	Enriched UO_2 Mass*(g)	Depleted U Metal Mass(g)	Fuel Height (mm)	Packing % (Compared to UO_2 Theoretical Density)
1	115.11	N.A.	70.7	82.2
2	115.24	N.A.	70.9	82.0
3	115.01	N.A.	69.5	81.7
4	102.94	18.17	69.7	87.5
5	103.56	18.28	70.6	87.5
6	103.46	18.26	69.5	87.5

* UO_2 are composed of particle of two range of sizes: 250-500 μm and 25-53 μm

Therefore, to achieve the optimized smear density, a fraction of smaller size fuel powders has to be included. The fission gas release resulting from such a composition is expected to be higher. The performance models of VIPAC fuel with multi-size particles will be developed in the future.

6.2.4 Optimization of Fuel by Addition of the Uranium Metal Powders

It is practically difficult to achieve high smear density with large size particles. A small amount of uranium metal powders is added to the Russian VIPAC fuel which is able to achieve a smear density up to 85%, and a high heavy content. [Mayorshin et al, 2000] In Russian tests, uranium metal powder was also found to be effective in controlling the oxygen to metal ratio and to

reduce the release of cesium, which improves the fuel cladding chemical interaction [Herbig et al, 1993]. Since the metallic uranium has much higher thermal conductivity than the ceramic UO_2 , the bulk thermal conductivity of the VIPAC fuel is improved. The presence of metallic powder at fuel cladding interface can also reduce the effective jump distance at the interface, therefore, improving the gap thermal conductance.

However, at high burnup, the uranium metal powders with less than $10\mu\text{m}$ particle size will release all the fission gas generated by fission process. The uranium metal has larger thermal expansion rate, the uranium powder expansion may cause excessive cladding strains if the fraction of uranium powder is large. However, in Russian test, with 30% of U metallic powder, the PCMI is still smaller compared with pelletized fuel [Mayorshin et al, 2000]. In addition, due to the poor compatibility of uranium metal with water, the uranium metal powders may compromise fuel safety at a LWR core.

Further investigations are needed to define the influence of the uranium metal powder addition on the VIPAC fuel performance. The fraction of the uranium metal powder can be optimized weighing the advantages and disadvantages of uranium powder presence.

6.3 Summary

In this chapter, the VIPAC fuel performance has been evaluated. A 150% power VIPAC annular fuel rod has been compared with a sintered annular fuel of the same power histories and with a reference 100% power solid fuel rod. It was found that VIPAC fuel operated at lower fuel temperatures than the sintered annular fuel at the initial period due to reduced gap temperature drops. After the sintered annular fuel gap closure, the sintered fuel has lower fuel temperature than VIPAC due to higher thermal conductivity. The fission gas release from VIPAC fuel is higher than both the sintered (annular and solid) cases since its particle bed composition provides larger surface area and larger porosity which facilitate fission gas release. However, the end of life internal pressure of the VIPAC fuel turned out to be lower than the sintered annular fuel since the VIPAC fuel has larger free volume.

The VIPAC annular fuel EOL cladding oxide thicknesses and hydrogen concentrations are comparable to those of the sintered annular fuel since the irradiation durations are the same and the cladding temperatures are not much different. The VIPAC annular rod cladding strains are significantly reduced for two reasons: the elimination of the gaps restrains the fuel cladding creep-down due to cladding pressure differential and the large porosity can well accommodate the fuel solid fission products at high burnup. Another obvious advantage for the VIPAC annular fuel is that it eliminates the gap conductance imbalance problem which is a serious concern for the sintered annular fuel.

A sensitivity of the important parameters of the VIPAC fuel has been performed to identify the optimum values of these parameters. The initial helium gas in the fuel rod plays an important role in improving the conductivity of the fuel. At very low gas pressures, the thermal conductivity is low and the fission gas release is rather high. At high pressures, the improvement of the thermal conductivity does not reduce the fission gas release since the gas release is mainly activated by athermal mechanisms, yet the EOL rod pressure is very high. Therefore the optimum initial helium gas pressure is in the range of 1.4-2.0 MPa.

VIPAC fuels of different particle sizes are also evaluated. Larger particle size reduces the fission gas release by reducing the surface to volume ratio, yet it increases the jump distance which leads to larger temperature drop at the fuel cladding interface. The optimum particle size is around 300-600 μ m. In practice, a range of particle sizes should be used with the smaller particles being about 30-60 μ m in diameter. This will improve the smear density and the conductivity.

Larger smear density results in higher thermal conductivity and improves fission the resolution rate, the fission gas release reduction by improving the smear density is significant at lower smear densities and at high smear densities the reduction is modest. However higher smear densities lead to higher cladding strain due to reduction of the fission products accommodation capability. The optimum smear density is in the range of 85%-90%.

The VIPAC fuel performance can also be improved by addition of the uranium metal powders

in the particle bed. Due to high thermal conductivity of the uranium powder, it improves the bulk fuel thermal conductivity and the presence of fine particles at the fuel cladding interface improves the gap conductance. The uranium powder can improve the heavy metal content in the fuel and it serves to control the oxygen potential in the fuel, hence reducing the fuel cladding chemical interaction. However, the uranium powder tends to release all the fission gas at high burnup and its compatibility with water is a concern. It is worth conducting further investigation of this design option.

Chapter 7 A Preliminary Investigation of Annular Fuel Transient Behavior

7.1 Review of Annular Fuel LOCA Performance Study

A preliminary Study of annular fuel LOCA behavior was performed at MIT using RELAP5/MOD 3.2. [Kim et al, 2002 a and b] In this study, a 15×15 internally and externally cooled annular fuel was analyzed in a real Korean Standard Nuclear Power Plant (KSNPP) and was compared with a solid 16×16 rod in the same condition during a Large Break LOCA (LB-LOCA). At steady state, the annular fuel is operated at significantly lower fuel temperatures than the solid fuel, but the pin linear power is higher and the cladding temperature is comparable. LB-LOCA is initiated by a postulated double-ended cold leg break between the coolant pump and reactor.

The cladding temperatures history during a LB-LOCA is illustrated in Figure 7.1. The LOCA transient is characterized by three phases: blow down, refill and reflood. During the blow down phase, a cladding temperature peak occurs due to the initial stored energy and degradation of rod to coolant heat transfer. As shown in Figure 7.1, the Peak Cladding Temperature (PCT) for annular fuel is much lower than that of the solid fuel case (606 °C for annular fuel and 926 °C for solid fuel) and it is far below the 1200 °C limit by NRC for cladding temperature during LOCA. During the refill phase, the emergency coolant injection starts to fill the lower plenum and during the reflood phase, the fuel rods are re-covered with water. During the refill and reflood phase, the cladding temperatures initially experiences a decrease due to the injection of emergency coolant and followed by a temperature increase due to decay heat and low heat transfer between fuel rod and coolant temperature. The cladding temperatures decrease as the water refloods the fuel rods and the decay heat is diminished. It can be seen in Figure 7.1 that the annular fuel cladding is rapidly quenched to coolant temperature while the solid fuel cladding temperature decreases slowly because the annular fuel cladding has larger surface area.

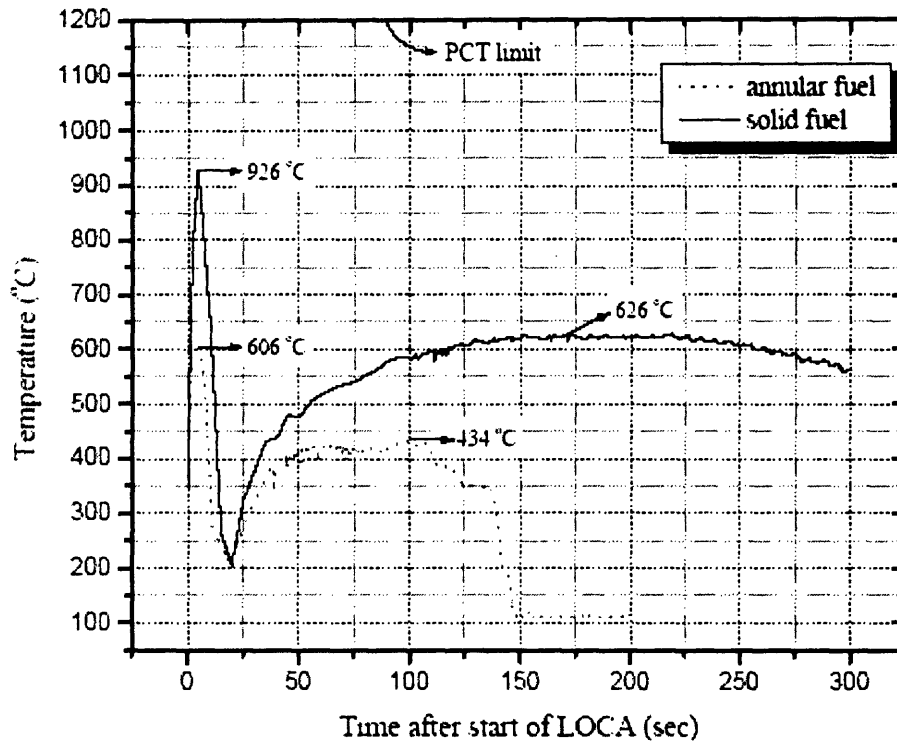


Figure 7.1 Transient Cladding Surface Temperature at Location of Maximum Linear Power of A Solid Fuel Rod and an Annular Fuel Rod [Kim et al, 2002 a]

It is also found that annular fuel PCT increases linearly with linear heat generation rate but even at 150% power, the annular PCT is still lower than the 100% power solid fuel. The degradation of the fuel conductivity and thermal conductance at higher burnup cause the increase of the PCT. [Kim et al, 2002]

For VIPAC annular fuel, the thermal conductivity is about 40%-50% lower but the gap conductance is 1.5-2 times higher than the sintered annular fuel when sintered annular fuel gaps are still open. The resulting PCT for the VIPAC annular fuel is comparable or lower than the sintered annular fuel. When LOCA occurs at a higher burnup, the fission products reduce the porosity of the VIPAC fuel, yielding higher thermal conductivity although the conductivity is somewhat degraded by fission damage, which results in even lower PCT.

7.2 A Preliminary Investigation of Reactivity Initiated Accident

The Reactivity Initiated Accident (RIA) that must be addressed as a design-basis accident is the control rod ejection accident. As the reactivity change leads to an over prompt critical condition, the power could grow very rapidly, which results in a significant energy deposit into the fuel rod in a short time. On the other hand, the temperature rise of the fuel activates Doppler feedbacks which terminate the event. At high burnup, the thermal expansion of fuel pellets together with fission gas induced swelling could cause a severe Pellet-to-Clad Mechanical Interaction (PCMI) in view of a reduced gap size. Depending on the extent of cladding degradation due to oxidation and hydrogen pickup and on the duration of the power pulse, the fuel rod could fail either in a brittle or in a ductile manner.

The RIA behavior of the annular fuel has been assessed using NRC licensed FRAPTRAN (Fuel Rod Analysis Program Transient) code [Cunningham et al, 2001]. FRAPTRAN has been validated by RIA tests for PWR type fuel rods and a limited number of BWR fuel rods and can calculate the LWR hypothetical accident fuel behavior at burnup level up to 65MWd/kgU. FRAPTRAN allows modeling of annular fuel geometry, but without internal cooling. Although FRAPTRAN does not model any internal cooling of the annular fuel, applying it to the annular fuel could provide some insights into the annular fuel behavior and give a conservative estimation for a number of reasons: (1) The most severe condition occurs with incidence of a large power surge when the heat transfer by coolant is not effective, and the fuel transient behavior can be reasonably captured by imposing the radial power profile from previous analysis with FRAPCON-ANNULAR (sintered and VIPAC); (2) The extrapolated fuel behavior models are generally more conservative as they recognize only one gap instead of two; (3) The outer cladding is much more likely to fail than the inner cladding since the outer cladding is subject to tensile stress and the inner to compressive stress, considering the fact that cladding is more resistant to compressive stresses.

Three hypothetical cases are calculated for comparison: a 150% power sintered annular fuel case, a 150% power VIPAC annular fuel case and a 100% power solid fuel case. The parameters of these cases are taken from the corresponding reference cases in Section 4.2. It is

assumed that the RIA occurs at the end of 1480 EFPDs and the input cladding parameters for RIA analysis such as the oxide layer thickness and hydrogen concentration are also the EOL values from the steady state calculations in Section 6.1.

The radial power distributions are provided by FRAPCON-ANNULAR steady state calculations in Section 6.1. The radial profiles for each axial node are given as input in FRAPTRAN. The axial power shape is assumed to be a chopped cosine with a peak to average ratio as 1.3 for all these cases. The Doppler effects for both the annular fuel and solid fuel are assumed to be the same.

During RIAs, the power density amplitude at the peak of the power pulse could be many times higher than that during normal operation. The resulting power pulse width varies due to different reactivity conditions, Doppler feedback and other factors. A typical power pulse width is around 40ms. To make a conservative estimation, a narrower power pulse width (10ms) is chosen. This is because a more rapid rate of power rise would increase the loading before the cladding becomes ductile enough to accommodate the PCMI loading. Since the peak fuel enthalpy is usually taken as the safety criterion for RIA cases, the amplitude of the solid fuel is so chosen that a conservative value of peak fuel enthalpy is achieved. For 150% power 13x13 annular fuel, the average linear power is ~2.6 times that of a 100% power solid fuel. Therefore, the power pulse height of the annular fuel is assumed to be 2.6 times of that of the sintered fuel. It should be noted that the power pulse is applied in FRAPTRAN, starting from a hot zero condition, which is proportional to the linear power.

Assuming the neutronic kinetics in annular fuel is similar to that of solid fuel, the power pulse for the power sintered annular fuel and the VIPAC fuel is also taken as 10ms. The power shape for these three cases are shown in Figure 7.2 .

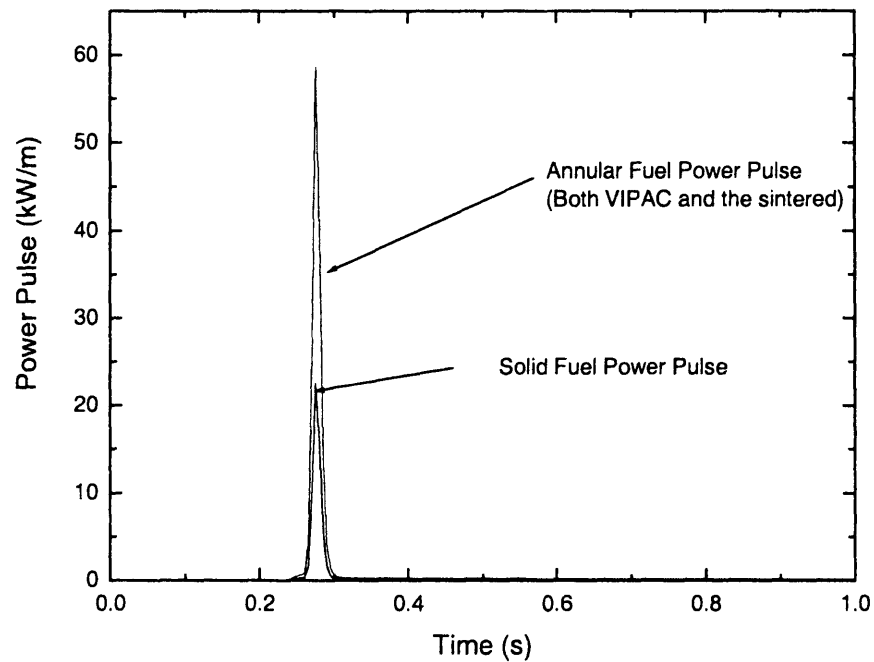


Figure 7.2 The Power Pulses for Different RIA Input Cases

The code calculates the steady state behavior for about 0.2 seconds at hot zero power and then a power surge is initiated which reaches about half of the rod linear power. FRAPTRAN calculates the fuel parameters such as stress and strain, fuel and cladding temperatures, rod internal pressure and so on as a function of time, as well as power and coolant conditions. Other parameters such as burnup, fission gas release and swelling are taken from the steady state calculations. The key parameters for RIA analysis cases are listed in Table 7.1.

Table 7.1 Key Parameters of Three Cases for RIA Analysis*

	Sintered annular rod	VIPAC annular rod	Reference solid rod
Cladding outside diameter (mm)	Outer: 15.4	Outer: 15.4	9.5
Cladding thickness(mm)	Outer: 0.57	Outer: 0.57	0.57
Cladding material	Zircaloy-4	Zircaloy-4	Zircaloy-4
Fuel pellet diameter (mm)	Outer: 14.1	Outer: 14.2	8.2
Diametral gap size (mm)	Outer: 0.12	Outer: 0.0	0.16
Fuel pellet height(mm)	13.4	–	13.4
Fuel stack height(m)	3.66	3.66	3.66
Plenum length(mm)	250	250	250
Plenum volume(mm ³)	2.1×10^4	2.1×10^4	1.4×10^4
External coolant pressure (MPa)	15.5	15.5	15.5
Initial fuel density	95%TD	85%Smear	95%TD
Fuel particle size(μm)	-	400	-
Gas pressure(MPa) (EOL)	7.5	5.8	4.9
Power pulse width (ms)	10	10	10
Peak to average ratio	1.3	1.3	1.3
EOL burnup(MWd/kgU)	85.8	97	50.7
EOL Cladding Oxide Layer Thickness (μ m)**	40	41	51
EOL Hydrogen Concentration (ppm)**	340	350	485

* Only outer cladding of the annular fuel is modeled, with the annular fuel geometry of FRAPTRAN

** Calculated by the FRAPCON code

Table 7.2 The Results of RIA Analysis for Different Cases

	Reference solid	Sintered annular	VIPAC annular
Maximum Fuel Enthalpy Increase (cal/g)	80.9	80.2	82.1
Perm. Hoop Strain (%)	0.85	0.73	1.35
Max. Avg. Cladding Temperature (K) during a Transient	954.6	800.3	707.7

The RIA simulation results are shown in Table 7.2. Although the power pulse height of the annular fuel is significantly larger, the relatively larger pin fuel volume makes the fuel enthalpy rise comparable to that of the solid case.

Permanent hoop strains for VIPAC annular fuel is the highest because without a fuel-clad gap, the fuel-cladding interaction for VIPAC annular fuel due to instantaneous thermal expansion is larger than the other two cases.

The sintered annular fuel has a lower permanent hoop strain than that of the solid fuel case because the power peaking of the sintered annular outer rim is less than that of the reference solid fuel at high burnup, as shown in Figure 2.2 in previous section. Thus, the fuel thermal expansion is less severe than that of the solid cases.

At this level of fuel enthalpy, Departure from Nucleate Boiling (DNB) is not predicted by the model for all the three cases, probably due to the short time of high cladding temperature. The maximum cladding temperature of the solid fuel is significantly higher than for other cases, thus is more susceptible to the burst (ductile) failure at high level of enthalpy deposition.

Since the current version of FRAPTRAN doesn't model the embrittlement of cladding due to hydrogen pickup, which is more dependent on the steady state operation conditions, hydrogen concentration at EOL can be used to compare the margin to PCMI failure. As shown in Table 7.2, the annular fuel is expected to perform better than the solid case.

Chapter 8 Annular Fuel Irradiation Test Planning

8.1 Introduction

The objective of the annular fuel irradiation testing is to obtain useful data about fuel relocation and fission gas release at power levels and temperatures relevant to eventual commercial applications of such type of fuel. It is also imperative to determine the fission gas release rate, fuel dimensional changes and cladding performance of annular fuel. To this end, the annular fuels are to be irradiated to a range of burnups to identify performance benefits and constraints at different burnup. The results can be used to validate the fuel performance code and to optimize the annular fuel design in order to proceed to commercial application.

The irradiation tests are being performed in MITR-II research reactor of the MIT Nuclear Reactor Laboratory. The MITR-II is a 5MW research reactor which is cooled and moderated with light water at atmospheric pressure [Bernard and Hu, 1999]. The core outlet temperature is about 50 °C, the neutron flux levels, where the irradiation samples are located, are close to those in a commercial light water reactor.

Due to low coolant temperature of MITR, the fuel cladding can not be operated in contact with coolant as it will be in the reference commercial design. Additional heat transfer resistances should be added between the cladding and the inner and outer channel to achieve representative fuel temperatures. The fuel sample enrichment is taken as 5%, close to that in commercial fuel design today in order to compensate for lower peak flux in the fuel. The sample fuel diameters are also larger than the commercial design in order to keep the test costs to a minimum. The cladding thicknesses are higher than the commercial design in order to be conservative at large fuel diameter. These differences from the commercial design will have no impact on the test objective, namely is mainly to identify the physical and dimensional changes and fission gas release.

8.2 Thermal Analysis Support for the Irradiation Test

Due to license restrictions, the MITR reactor coolant must be used to remove heat of the fuel specimen and the coolant temperature is significantly lower than that in a power reactor (42°C in MITR compared with $\sim 300^{\circ}\text{C}$ in a power reactor). In order to simulate the power reactor conditions, an aluminum thimble is to be installed to separate the fuel and the coolant, and the space between the thimble and the fuel specimen is filled with Lead Bismuth Eutectic (LBE). The thimble and gaps are designed in such a way that the desirable fuel temperature profile is obtained and the onset of nucleate boiling on the thimble walls is avoided. A schematic of the thimble and gap is shown in Figure 8.1.

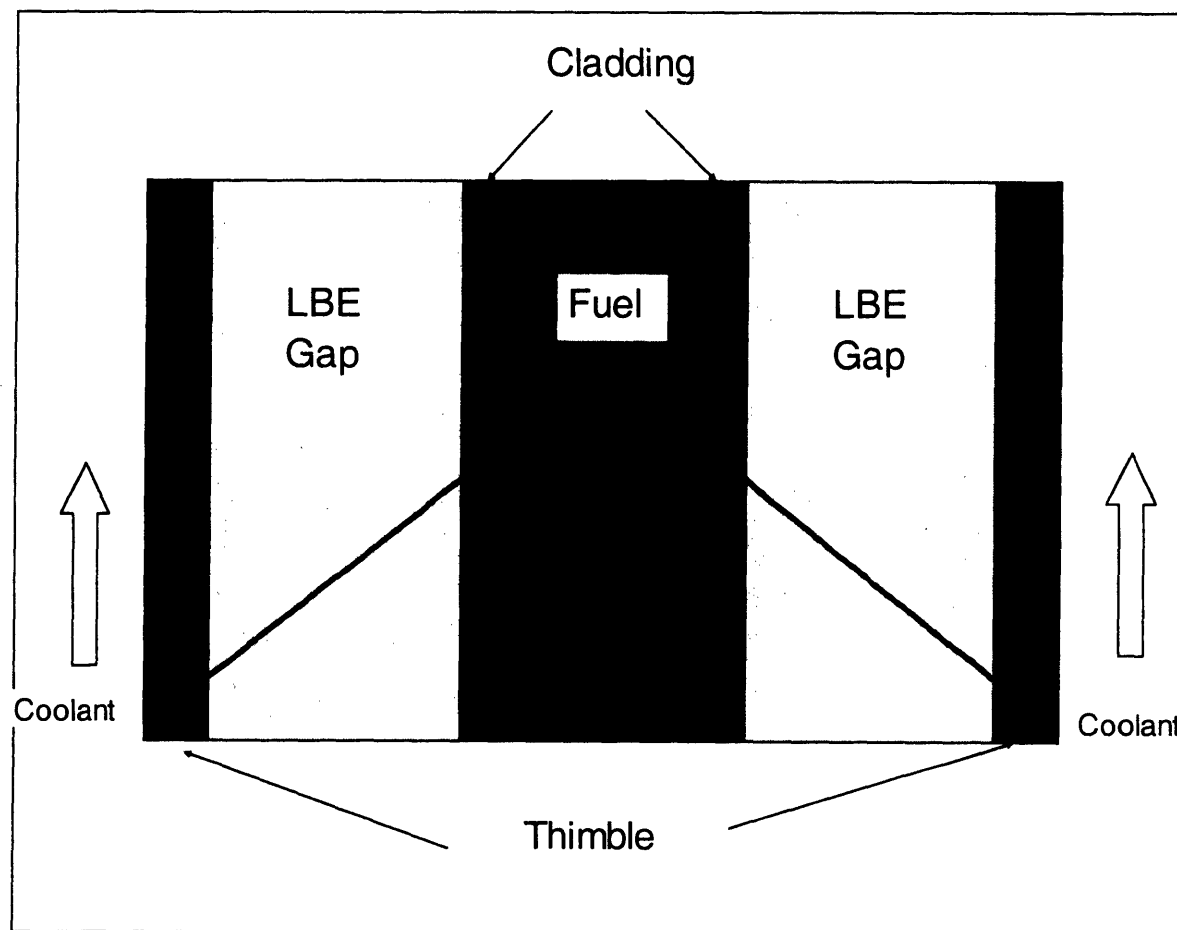


Figure 8.1 The Schematic of the Thimble and LBE Gaps

The thermal hydraulic calculations for the thimble and gap design are performed using the computer code: Thermal-Hydraulic Model of Annular Fuel with Internal and eXternal Cooling (TAFIX). [Kazimi, et al., 2001] The power distribution of the annular fuel segments was calculated by the neutronics code and provided as an input for the thermal analysis.(Figure 8.2) [Hejzlar, 2004]

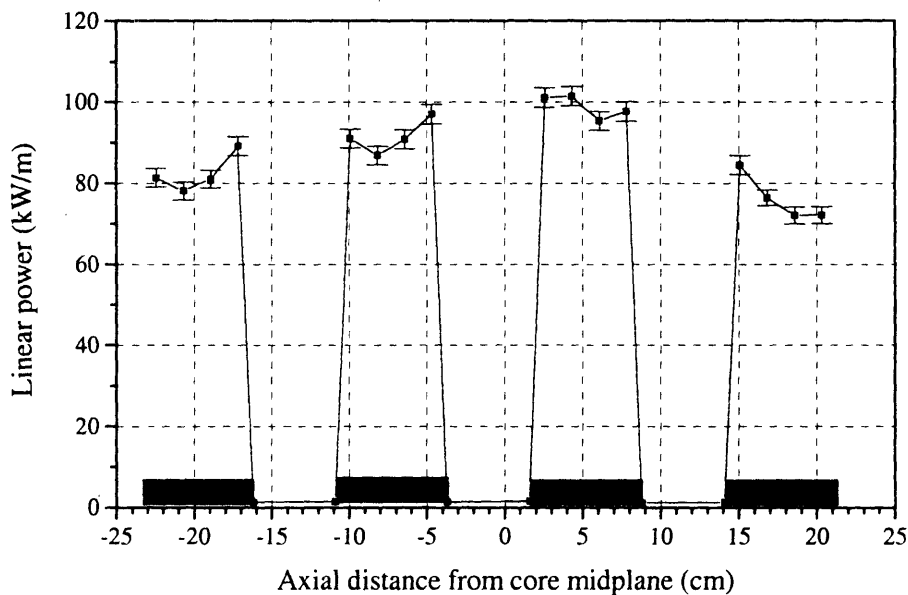


Figure 8.2 Power Distributions in the Annular Fuel Experiment

Four fuel segments are inserted in the thimble, which is to be irradiated in the MITR core. The thimble and gap dimensions were adjusted to match the desired temperature profile. The thimble is made of aluminum and the gap between the aluminum thimble and fuel cladding is filled with LBE with a thermal conductivity of 6 W/K-m. The coolant temperature is 42 °C and coolant flow rate per pin is 5.15 kg/s. The coolant inlet pressure is about 0.12 MPa and the core pressure drop is 0.023 MPa. The fuel meat is composed of VIPAC fuel about ~90% smear density [Hamilton, 2003] and is operated at about 50% higher power than that of the reference Westinghouse LWR core. Using the power distribution shown in Figure 8.2, the dimensions of the annular experiment design are determined and listed in Table 8.1. The radial temperature profiles of the fuel segments are shown in Figure 8.3.

Table 8.1 Radial Dimension of MITR Annular Fuel Experiment

Item	Dimension (cm)
Inner Thimble ID	1.92
Inner Thimble OD	2.12
Inner Cladding ID	2.36
Inner Cladding OD	2.56
Fuel Meat ID	2.56-fuel fill entire gap
Fuel Meat OD	2.98-fuel fill entire gap
Outer Cladding ID	2.98
Outer Cladding OD	3.18
Outer Thimble ID	3.56
Outer Thimble OD	4.36
Outer Channel OD	5.82

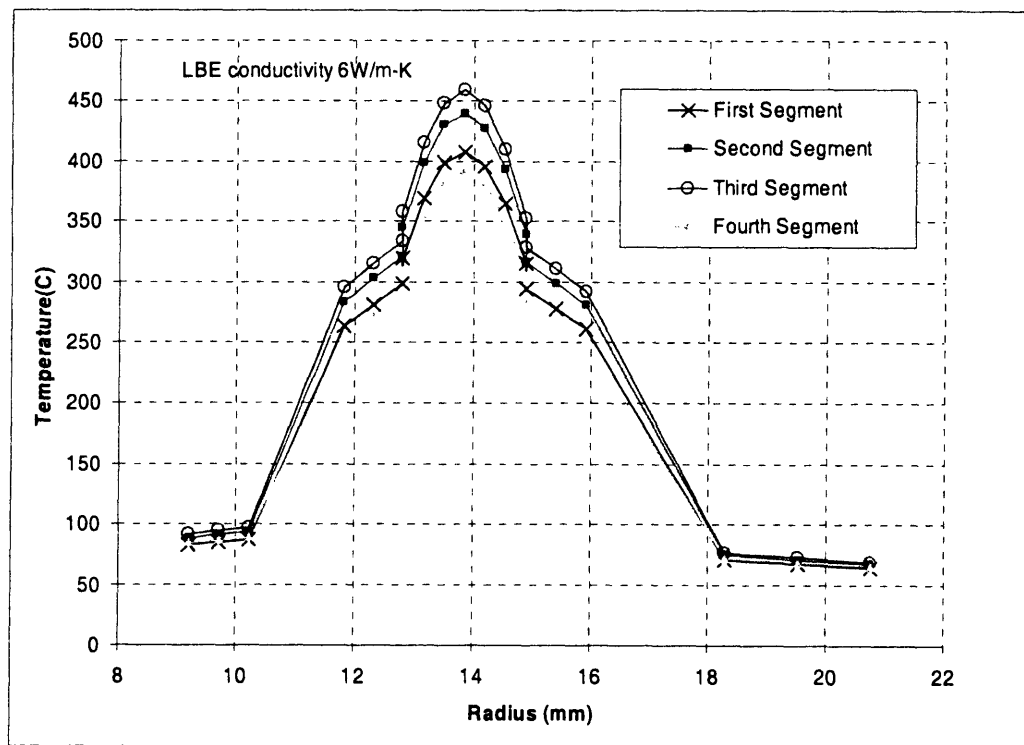


Figure 8.3 Fuel Temperature Profiles for Annular Fuel Experiment

The calculation shows (Figure 8.3) that both fuel and claddings achieve temperatures comparable to those in a PWR core. The calculations are performed assuming that LBE is in good contact with the annular fuel claddings and aluminum thimbles. As can be seen in Figure 8.3, the outer LBE gap is larger than the inner LBE gap. The coolant temperatures and

aluminum thimble temperatures are illustrated in Figure 8.4, while heat flux to coolant is illustrated in Figure 8.5.

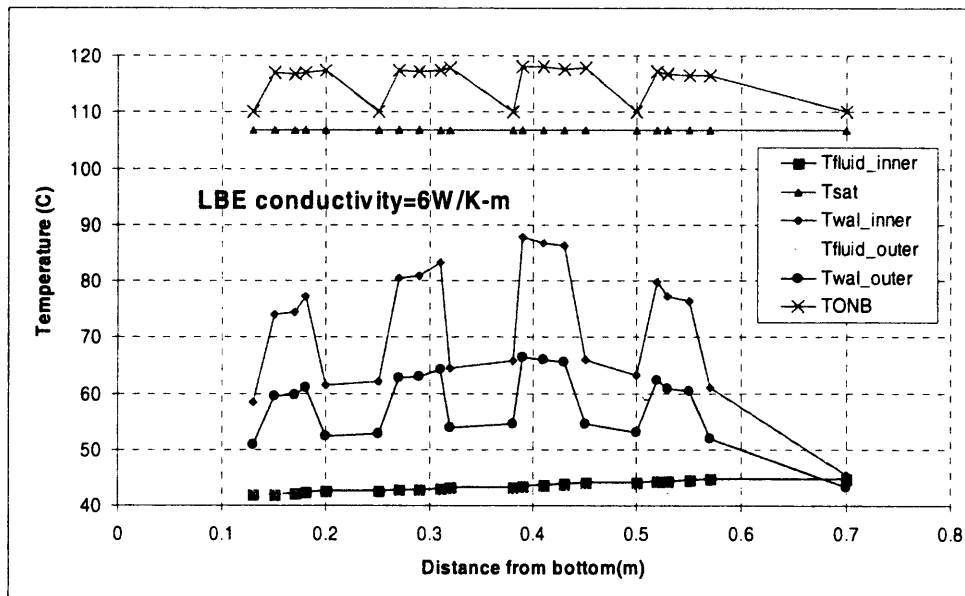


Figure 8.4 Coolant and Thimble Wall Temperatures for Annular Fuel Experiment

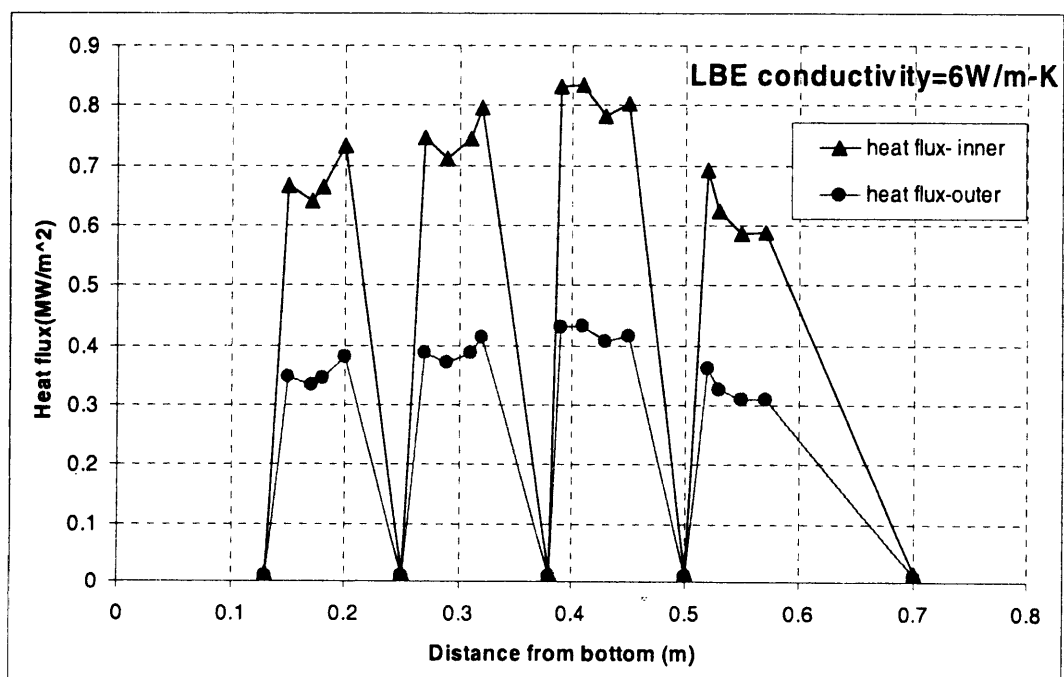


Figure 8.5 Heat Flux to Coolant for Annular Fuel Experiment

As indicated in Figure 8.4, the coolant temperature is deeply subcooled and the thimble wall temperatures are 20-30 K below saturation, leaving large margins for onset of nucleate boiling. The heat flux to inner channel is about twice that to the outer channel (Figure 8.5) due to smaller heated.

However, the initial temperature measurement of the cladding and thimble surface temperatures the top fuel sample indicates a skewed temperature profile. The profile that matches these measurements is shown in Figure 8.6. The reason for the asymmetry is different effective conductance of LBE gaps in the inner and outer channels. [Kazimi, et al, 2004]

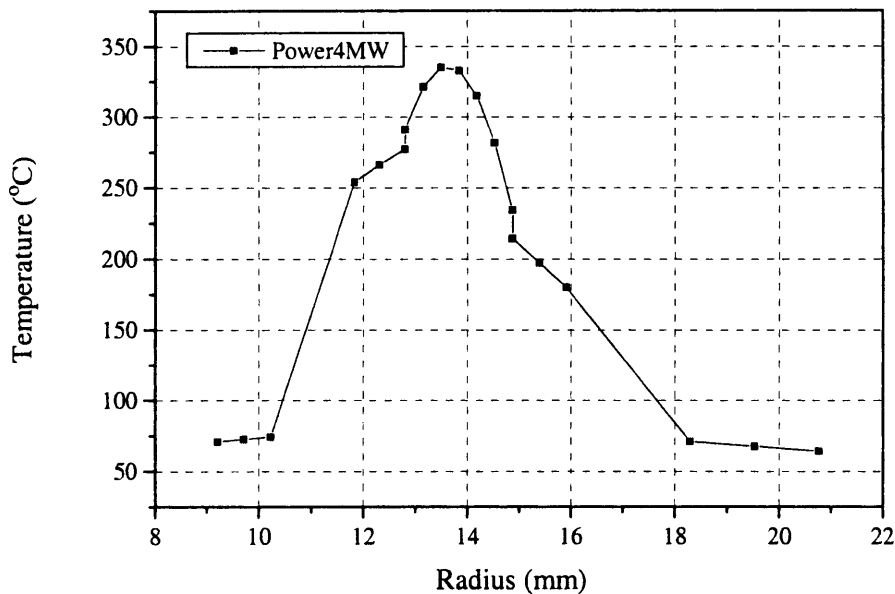


Figure 8.6 Measured Temperature Profile of Fuel Irradiation Test (Top Sample)

This profile could be achieved with inner and outer gap conductances of 4223 and 2430 W/m²-K, which correspond to effective gap thermal conductivities of 10 and 3.9W/m-K. The values of 10 W/m-K was observed in our out of pile experiments [Kazimi, 2003b], but the value of 3.9 is smaller than we have measured earlier. This may be due to the tendency of the inner gap to open as a result of thermal expansion. (Note that part of LBE gap is solid forming contact with Aluminum capsule) On the other hand, the outer gap conductance is larger than expected 6W/m-K, which may be due to tighter gap as a result of thermal expansion of the clad towards

the outer aluminum capsule. In spite of the smaller inner gap conductance, the inner cladding temperature corresponds very closely to the predictions. This is because more heat is transferred towards the outer channel as a result of higher conductance of the outer gap. Thermal couple misplacement could be another explanation for deviation of data from the predictions.

8.3 Post Irradiation Test (PIE) Planning

8.3.1 Initial Conditions of the Fuel Sample

The annular fuel irradiation test capsule contains four segments. The top segment of the annular fuel has been planned to be removed for post irradiation examination after irradiation at MITR for 96 effective full power days and cooling at the reactor core for 30 days before transferring it to the hot cell. The dimensions of the top sample are shown in Table 8.2. Activity of the top sample will be about 1112 curies on transfer to the hot cell from the reactor core tank. The sample is welded shut and has an instrumentation/gas tube for thermocouple leads and sample atmosphere control.

Table 8.2 The Top Sample Dimensions

Fuel Outer Cladding(OD)	2.98 cm
Fuel Inner Cladding(ID)	2.56 cm
Fuel Meat Length	7.07 cm
Fuel Segment Height	10.0 cm

The Curie radioactivity of the top specimen is shown in Table 8.3 and species grams in the top specimen is shown in Table 8.4.

Table 8.3 Curie Radioactivity of the Top Specimen (109gHM) after 96 EFPDs at MITR to Burnup of 4.92MWd/kgHM (Hejzlar, 2004)

Days	Discharge	1.0D	5.0D	10.0D	20.0D	30.0D
Actinides						
U237	1.139E+01	1.028E+01	6.816E+00	4.079E+00	1.461E+00	5.232E-01
U239	1.591E+03	6.050E-16	0.000E+00	0.000E+00	0.000E+00	0.000E+00
NP239	1.585E+03	1.189E+03	3.665E+02	8.414E+01	4.435E+00	2.338E-01
PU239	9.969E-03	1.008E-02	1.030E-02	1.037E-02	1.040E-02	1.040E-02
PU240	1.841E-03	1.841E-03	1.841E-03	1.841E-03	1.841E-03	1.841E-03
PU241	7.678E-02	7.677E-02	7.673E-02	7.668E-02	7.658E-02	7.648E-02
SUMACT	3.190E+03	1.200E+03	3.735E+02	8.833E+01	5.986E+00	8.465E-01
Fission Product						
GE	2.560E+01	3.391E-02	9.291E-05	5.906E-08	2.386E-14	9.642E-21
AS	7.931E+01	2.898E-01	5.456E-02	6.400E-03	8.798E-05	1.209E-06
SE	2.432E+02	7.247E-04	1.421E-04	2.274E-05	7.081E-06	6.865E-06
BR	5.567E+02	7.315E-02	7.489E-03	7.100E-04	6.382E-06	5.737E-08
KR	9.804E+02	2.091E+00	2.073E-01	2.071E-01	2.068E-01	2.064E-01
RB	1.332E+03	5.183E-01	2.090E-02	1.736E-02	1.197E-02	8.256E-03
SR	1.727E+03	2.009E+02	1.484E+02	1.387E+02	1.211E+02	1.058E+02
Y	2.210E+03	2.693E+02	1.707E+02	1.609E+02	1.431E+02	1.273E+02
ZR	1.468E+03	2.852E+02	1.813E+02	1.700E+02	1.525E+02	1.369E+02
NB	2.006E+03	3.123E+02	1.298E+02	1.307E+02	1.362E+02	1.377E+02
MO	1.035E+03	2.109E+02	7.696E+01	2.182E+01	1.755E+00	1.411E-01
TC	1.046E+03	2.020E+02	7.414E+01	2.103E+01	1.691E+00	1.362E-01
RU	2.364E+02	1.303E+02	1.202E+02	1.104E+02	9.319E+01	7.874E+01
RH	2.534E+02	1.551E+02	1.147E+02	1.005E+02	8.445E+01	7.141E+01
AG	2.345E+01	5.021E+00	1.544E+00	9.433E-01	3.732E-01	1.484E-01
IN	2.958E+01	9.068E-01	2.579E-01	5.445E-02	2.431E-03	1.124E-04
SN	1.810E+02	1.691E+00	8.686E-01	6.067E-01	3.522E-01	2.264E-01
SB	5.012E+02	8.104E+00	3.622E+00	1.576E+00	3.990E-01	1.988E-01
TE	1.226E+03	1.851E+02	7.922E+01	3.131E+01	8.542E+00	5.007E+00
I	1.771E+03	4.479E+02	1.633E+02	8.148E+01	2.711E+01	1.058E+01
XE	1.424E+03	3.708E+02	1.902E+02	9.905E+01	2.693E+01	7.515E+00
CS	1.257E+03	2.695E+00	2.556E+00	2.421E+00	2.237E+00	2.127E+00
BA	1.628E+03	2.658E+02	2.143E+02	1.639E+02	9.598E+01	5.650E+01
LA	1.631E+03	2.811E+02	2.395E+02	1.860E+02	1.085E+02	6.312E+01
CE	1.074E+03	4.397E+02	2.831E+02	2.418E+02	2.025E+02	1.719E+02
PR	9.015E+02	3.231E+02	2.756E+02	2.262E+02	1.551E+02	1.119E+02
ND	1.978E+02	9.630E+01	7.494E+01	5.478E+01	2.927E+01	1.564E+01
PM	1.182E+02	6.507E+01	2.299E+01	1.109E+01	7.495E+00	7.048E+00
SM	1.613E+01	8.569E+00	2.031E+00	3.562E-01	2.656E-02	1.721E-02
SUMFPS	2.522E+04	4.277E+03	2.573E+03	1.957E+03	1.400E+03	1.111E+03
CUMULATIVE TABLE TOTALS						
FPS	2.522E+04	4.277E+03	2.573E+03	1.957E+03	1.400E+03	1.111E+03
ACT+FPS	2.841E+04	5.477E+03	2.946E+03	2.045E+03	1.406E+03	1.112E+03

Table 8.4 Species grams in the top specimen (109gHM) after 96 EFPDs at MITR to burnup of 4.92MWd/kgHM (Hejzlar, 2004)

	Discharge	1.0D	5.0D	10.0D	20.0D	30.0D
Actinides						
U235	3.958E+00	3.958E+00	3.958E+00	3.958E+00	3.958E+00	3.958E+00
U236	1.020E-01	1.020E-01	1.020E-01	1.020E-01	1.020E-01	1.020E-01
U238	1.042E+02	1.042E+02	1.042E+02	1.042E+02	1.042E+02	1.042E+02
NP239	6.831E-03	5.125E-03	1.579E-03	3.626E-04	1.911E-05	1.007E-06
PU239	1.603E-01	1.621E-01	1.656E-01	1.668E-01	1.672E-01	1.672E-01
PU240	8.077E-03	8.077E-03	8.077E-03	8.077E-03	8.077E-03	8.077E-03
SUMACT	1.084E+02	1.084E+02	1.084E+02	1.084E+02	1.084E+02	1.084E+02
Fission products						
GE	9.215E-06	9.152E-06	9.146E-06	9.147E-06	9.147E-06	9.147E-06
SE	1.027E-03	1.027E-03	1.027E-03	1.027E-03	1.027E-03	1.027E-03
BR	4.055E-04	4.033E-04	4.033E-04	4.033E-04	4.033E-04	4.033E-04
KR	7.600E-03	7.580E-03	7.580E-03	7.579E-03	7.579E-03	7.578E-03
RB	7.154E-03	7.160E-03	7.161E-03	7.161E-03	7.162E-03	7.163E-03
SR	2.479E-02	2.465E-02	2.435E-02	2.401E-02	2.340E-02	2.286E-02
Y	1.181E-02	1.175E-02	1.170E-02	1.164E-02	1.152E-02	1.140E-02
ZR	6.867E-02	6.869E-02	6.863E-02	6.858E-02	6.851E-02	6.843E-02
NB	3.045E-03	3.074E-03	3.193E-03	3.313E-03	3.456E-03	3.498E-03
MO	4.488E-02	4.490E-02	4.493E-02	4.513E-02	4.576E-02	4.644E-02
TC	1.385E-02	1.398E-02	1.426E-02	1.437E-02	1.441E-02	1.442E-02
RU	3.321E-02	3.314E-02	3.287E-02	3.255E-02	3.200E-02	3.153E-02
RH	4.152E-03	4.203E-03	4.427E-03	4.724E-03	5.254E-03	5.699E-03
PD	4.677E-03	4.703E-03	4.750E-03	4.769E-03	4.794E-03	4.818E-03
AG	2.661E-04	2.667E-04	2.632E-04	2.597E-04	2.561E-04	2.546E-04
CD	4.148E-04	4.163E-04	4.198E-04	4.228E-04	4.259E-04	4.270E-04
IN	5.471E-05	5.523E-05	5.653E-05	5.710E-05	5.760E-05	5.795E-05
SN	7.460E-04	7.435E-04	7.405E-04	7.382E-04	7.351E-04	7.333E-04
SB	3.252E-04	3.127E-04	3.015E-04	2.956E-04	2.931E-04	2.931E-04
TE	6.873E-03	6.722E-03	6.402E-03	6.243E-03	6.152E-03	6.125E-03
I	3.985E-03	3.689E-03	3.301E-03	3.065E-03	2.824E-03	2.731E-03
XE	8.432E-02	8.450E-02	8.452E-02	8.444E-02	8.439E-02	8.441E-02
CS	4.434E-02	4.461E-02	4.531E-02	4.578E-02	4.615E-02	4.623E-02
BA	2.609E-02	2.587E-02	2.518E-02	2.449E-02	2.358E-02	2.305E-02
LA	2.171E-02	2.165E-02	2.158E-02	2.149E-02	2.135E-02	2.127E-02
CE	6.083E-02	6.074E-02	6.049E-02	6.031E-02	5.972E-02	5.893E-02
PR	1.523E-02	1.531E-02	1.544E-02	1.546E-02	1.570E-02	1.611E-02
ND	5.095E-02	5.115E-02	5.179E-02	5.248E-02	5.359E-02	5.442E-02
PM	6.186E-03	6.207E-03	6.350E-03	6.540E-03	6.790E-03	6.904E-03
SM	7.252E-03	7.304E-03	7.413E-03	7.470E-03	7.536E-03	7.592E-03
EU	8.842E-04	8.909E-04	8.997E-04	8.978E-04	8.903E-04	8.849E-04
GD	1.588E-04	1.606E-04	1.664E-04	1.722E-04	1.806E-04	1.862E-04
TB	8.801E-06	8.858E-06	8.842E-06	8.787E-06	8.728E-06	8.703E-06
SUMACT	5.559E-01	5.559E-01	5.559E-01	5.559E-01	5.559E-01	5.559E-01
CUMULATIVE TABLE TOTALS						
FPS	5.559E-01	5.559E-01	5.559E-01	5.559E-01	5.559E-01	5.559E-01
ACT+FPS	1.090E+02	1.090E+02	1.090E+02	1.090E+02	1.090E+02	1.090E+02

8.3.2 Sample Extraction and Transport

The instrumentation tube will be cut off and the capsule will be opened using a tube cutter. This will give access to the plenum end of the fuel sample above the level of the heat transfer medium. The fuel capsule will be subsequently extracted by heating the LBE and removing the measurement instrumentation.

The Sample will be transported to hot cell using the currently available fuel cask at the MITR. A diagram of the cask is shown in Figure 8.7

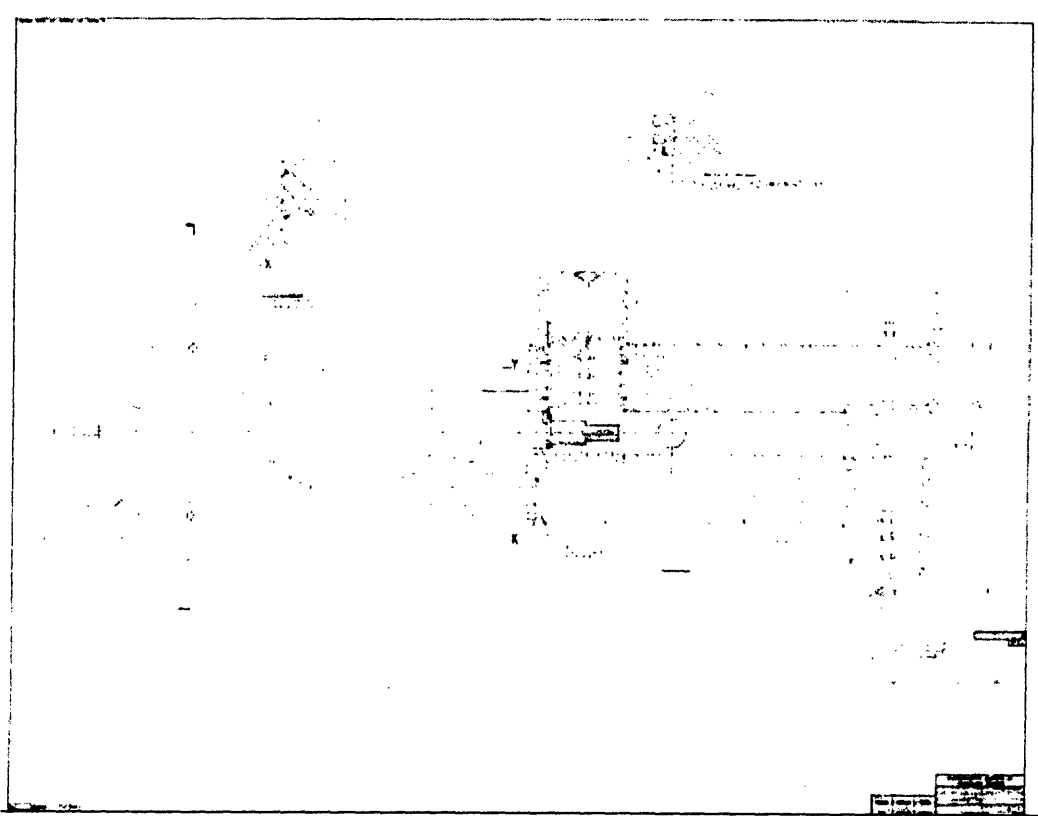


Figure 8.7 Transport Cask for the Experiment

The detailed procedures for the sample extraction and cask operation are as follows: [BNL, 1971]

- 1) remove the reactor top shield
- 2) Attach the fuel transfer basket support structure to the core flow guide
- 3) Place the fuel element transfer basket in the support structure

- 4) Move the fuel element transfer cask to the reactor top and suspend it by the side of the reactor lid at a height ~6 inches above the torus.
- 5) Remove the 12" viewing port plug.
- 6) Insert the fuel element lifting tool and a remote cutting tool through the access plug hole.
- 7) Attach the fuel element lifting tool to the bale of the element and lock.
- 8) Lift the fuel element out of the storage position.
- 9) Use the cutting tool to hold the stretched-out thermal couple wires at the position where cutting is supposed to be performed.
- 10) Cut the thermal couple wires with cutting tool and remove them to the transfer basket.
- 11) Transfer the fuel element to the transfer basket.
- 12) Remove the lifting and cutting tool.
- 13) Open the transfer cask shutter and use a remote handling tool to reel out ~ 20 feet of cable from the transfer cask.
- 14) Attach the cable's lifting hook to the handle to fuel transfer basket and make sure it is securely attached.
- 15) Using the small crane hook to place the view port plug in the view port.
- 16) Move the fuel transfer cask on the reactor top and be centered over the plug hole.
Lower the fuel transfer cask onto the torus and weight of the cask is supported by the crane.
- 17) Reel in the cable and raise the fuel element to the top of the transfer cask.
- 18) Close the cask shutter.
- 19) Raise the cask and close the reactor top.
- 20) Move the cask to the designated place (storage pool/ hotcell)

8.3.3 Burnup Measurement

Fuel burnup can be measured by gauging Cs-137 activity. Cs-137 has a half life of 30.07 years and emits 662keV γ ray. The axial distribution of Cs-137 activity can be determined using a lead collimator which has a thin slit to allow the detection of the γ ray at a specific axial location. The collimator has a detector at one end to capture the γ ray and a slot at the other end to contain the fuel sample. The schematic of burnup measurement setup is shown in Figure 8.8.

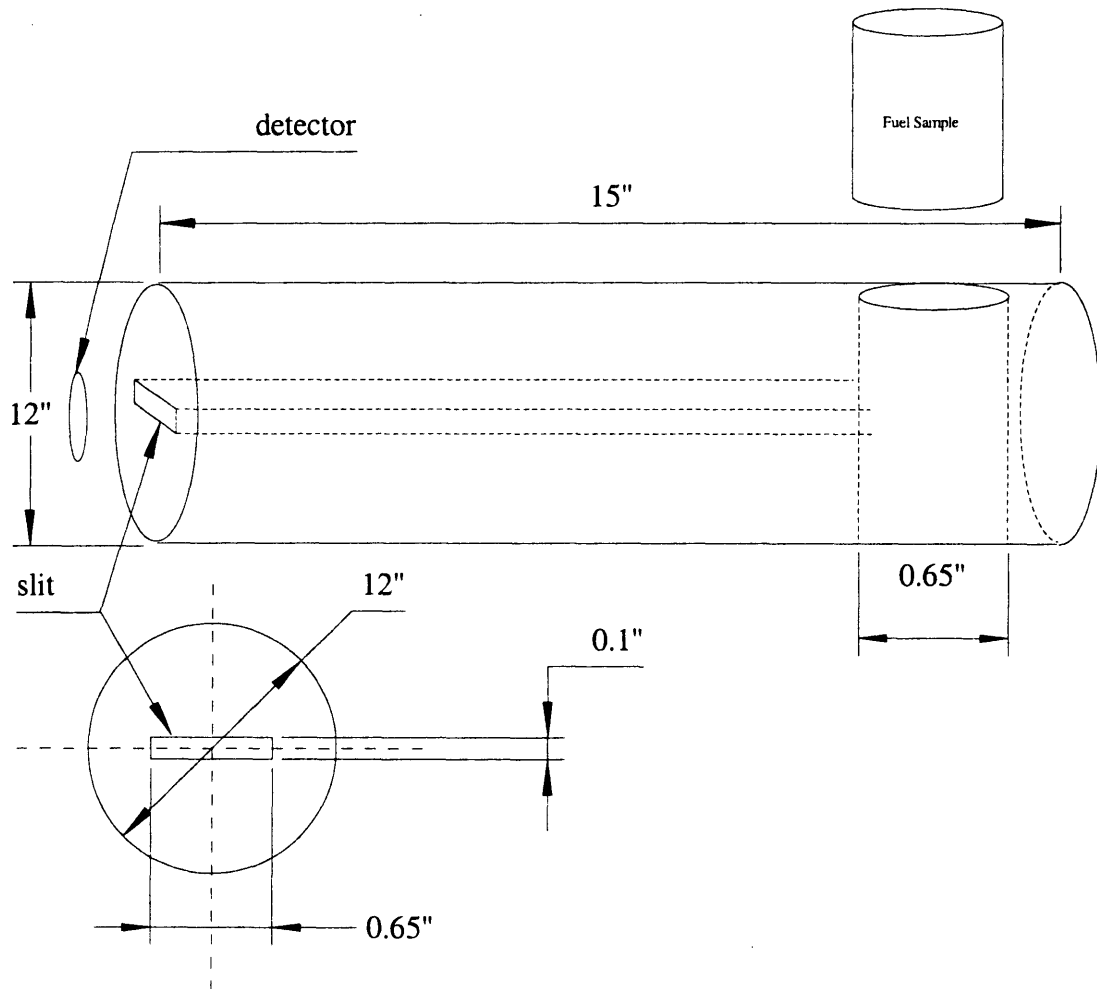


Figure 8.8 Collimator Design for Burnup Measurement

The Cs-137 activity of the top specimen after 96 EFPDs and 30 days decay is 1.723 Curie, which can be converted to 6.38×10^{10} dps. Considering the branching ratio of that mode of decay and branching ratio of that mode γ energy, the count rate can be calculated as [Knoll, 1989]

$$N_{source} = BF \times BR \times A \quad (8-1)$$

where N is the number of γ ray peaks at 662 keV and A is activity of the source in dps. BF is the branching fraction of that mode of decay ($BF=0.9011$) and BR is the branching ratio for that mode of energy ($BR=0.944$) Then.

$$N_{signal} \approx N_{source} \frac{d}{l} \frac{A}{4\pi \cdot t^2} \quad (8-2)$$

where

N_{signal} is the gamma rays that can be detected by detector,

N_{source} is the gamma rays emitted,

t is the lead thickness.

A is the area of the slit, d is the slit width (vertical), l is the specimen length (7cm) and t is collimator length (cm)

Therefore,

$$N_{signal} = N_{source} \epsilon_{int} \frac{dA}{4l\pi^2} = BF \times BR \times N \times \epsilon_{int} \frac{dA}{4l\pi \cdot t^2} \quad (8-3)$$

where ϵ_{int} is the detector efficient, which is assumed to be 40%.

The ratio of background noise to signal can be calculated with the following equation:

$$E_{back} = E_{ls} e^{-\mu} \approx BF \times BR \times N \times \epsilon_{int} \frac{a^3}{16lt^2} e^{-\mu} \quad (8-4)$$

where E_{back} is the background noise energy, E_{ls} is the energy before attenuation and μ is gamma ray attenuation coefficient for lead (which is 1.066/cm)), t is the lead thickness, a is the diameter of detector. It can be further derived from equations (8-3) to (8-4) as:

$$E_{back} / E_{sig} = \frac{a^3 \pi / 4}{dA} e^{-\mu} \quad (8-5)$$

Assuming ϵ_{int} as 40%, $a=2$ cm, $d=0.254$ cm, t from 20-100cm, we derived the following result as shown in Figure 8.9.

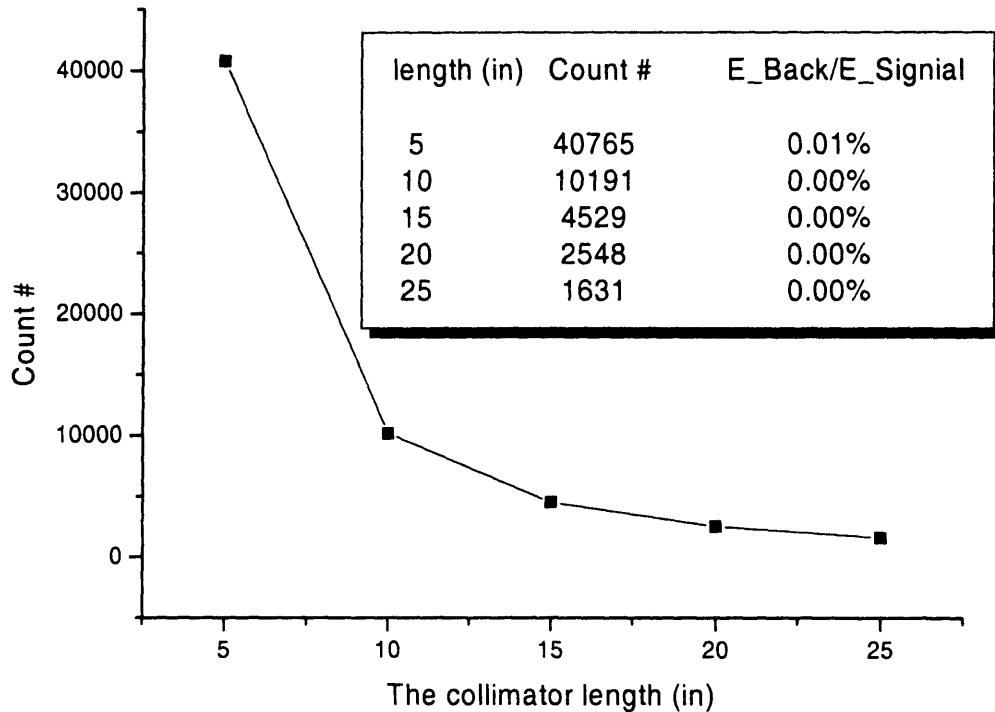


Figure 8.9 Count Rate and Background Noise of Burnup Measurement

It can be seen by using 15 inch long lead collimator (weighing ~42kg), the count rate is large enough and the background noise is negligible.

8.3.4 Fission Gas Release Measurement

Fission Gas Release Prediction

The fission gas release for the fuel samples is predicted with FRAPCON-ANNULAR (VIPAC), the fuel sample is composed of three fractions with smear density of 87.5%. Instead of using operating power, the outer cladding temperatures measured in Figure 8.6 are used as the cladding boundary conditions. The predicted fission gas release for different burnups is shown in Figure 8.10. The prediction is obtained with FRAPCON-ANNULAR (VIPAC version). Since the code was unable to model VIPAC fuel with mixed particle sizes, the FGR prediction is obtained by calculating FGRs for single particle size for 3 different sizes and derive the result with weight fraction.

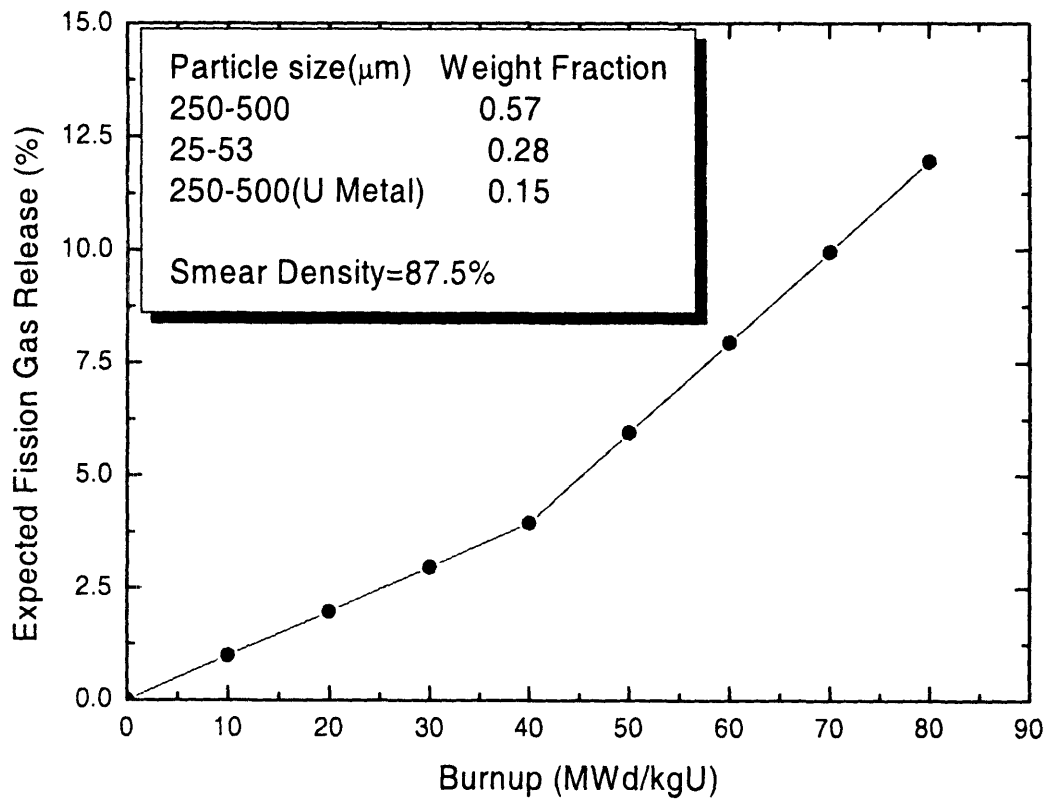


Figure 8.10 Fission Gas Release Predictions for Fuel Irradiation Test

Fission Gas Release Measurement Setup

In order to measure fission gas release, the fuel sample will be inserted in a sealed chamber with provision for sweep gas flow. The sample plenum region will be punctured and the gas atmosphere of the chamber will then be swept by a controlled gas flow through a gamma counting chamber. The fission gas release setup is shown in Figure 8.11.

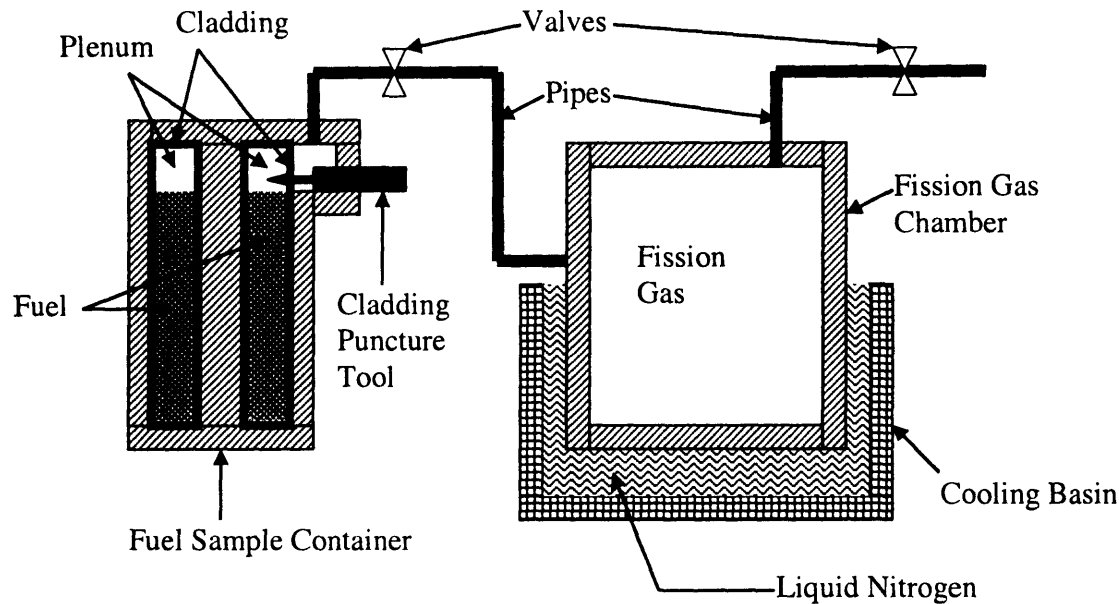


Figure 8.11 Fission Gas Release Measurement Setup

As shown in Figure 8.11, the fuel is placed in a sealed volume where puncture tool is provided. The cladding will be punctured from the side and a pipe is provided to release the fission gas to a fission gas chamber where measurement can be done. The Fission gas chamber is cooled by liquid nitrogen in order to evacuate fission gas out of the fuel sample container.

It is important to minimize the fission gas trapped in the volumes other than the fission gas chamber where measurement is made. However, it is also important to leave large volume in the fuel sample container so that the puncture operation can be performed.

In this setup, fission gas will be dispersed in three volumes: (1). Fuel plenum (2) free volume necessary for FGR measurement, such as volume for clad puncture operation, pipes, etc. (3) fission gas container. Only the fission gas trapped in the fission gas container could be counted. For a rough estimation, we could treat the Kr-85 as ideal gas and calculate the needed free volume with following equations:

$$PV = \frac{m}{M} RT \quad (8-6)$$

$$m_{\text{container}} = \frac{P_{\text{container}} V_{\text{container}} M}{RT_{\text{container}}} \quad (8-7)$$

$$m_{\text{total}} = \frac{P_{\text{container}} V_{\text{container}} M}{RT_{\text{container}}} + \frac{P_{\text{plenum}} V_{\text{plenum}} M}{RT_{\text{plenum}}} + \frac{P_{\text{freeV}} V_{\text{freeV}} M}{RT_{\text{freeV}}} \quad (8-8)$$

where m is mass of the gas and M is atomic mass of gas, P is the pressure and V is volume. R is gas constant and T is gas temperature. The pipe volume is neglected.

Further assume $P_{\text{plenum}} = P_{\text{freeV}} = P_{\text{Container}}$ and $T_{\text{freeV}} = T_{\text{plenum}} = 600K$

$$T_{\text{container}} = 120K$$

The plenum volume is calculated with parameters listed in Table 8.5.

Table 8.5 Top Fuel Sample Parameters and Plenum Volume

ID	OD	Plenum Height	Volume
25.59mm	29.74mm	10.46mm	0.6cm ³

The relation between the fission gas chamber volume and fuel sample container volume to evacuate 90% of fission gas released to the fission gas chamber is shown in Figure 8.12.

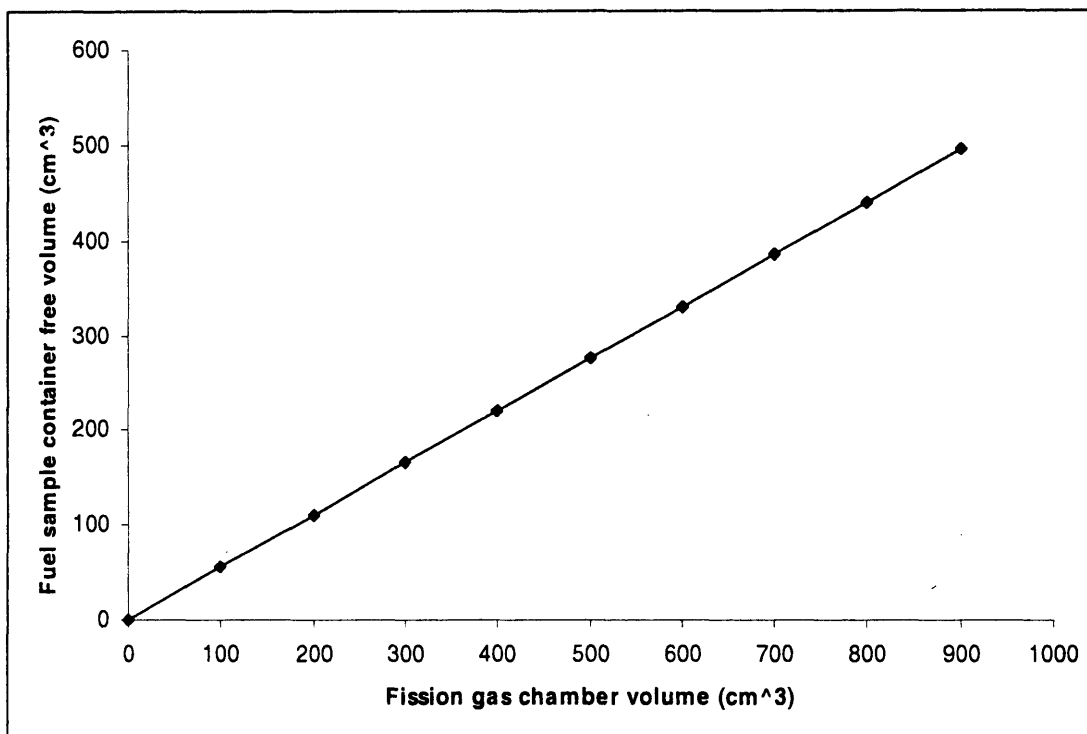


Figure 8.12 The Relation Between the Fission Gas Chamber Volume and Fuel Container Volume in Order to Evacuate 90 % of Fission Gas Released.

It can be seen that in order to evacuate 90% fission gas released and for a fission gas chamber volume of 100 cm³, about 55cm³ free volume is allowed to perform puncture operation.

Estimation of Count Rate at Different FGR Vaules

The gaseous activities are calculated as about 0.206 curies of Kr-85 in 7 cm of fuel at 30 days decay after 96 EFPDs of irradiation. The measurement system and the expected counting rates are estimated based on this value. The half life of Kr-85 is 10.76 years and the γ energy is 514 kev.

Assuming a point source and fission gas is counted by a NaI detector, a rough estimation of the count rate (N) can be given by following equation:

$$N = S \varepsilon_{\text{int}} \frac{Q}{4\pi} \quad (8-9)$$

where ε_{int} is the intrinsic detection efficiency, which depends on the detector and is assumed to be 10% for a NaI detector. [Knoll, 1989] S is the number of photons emitted by the source. Q is the solid angle, which can be approximated as $Q \approx \frac{\pi a^2}{D^2}$, if the distance between the source and the detector (D) is much larger than the detector diameter (a). The air attenuation effect is negligible and the detector diameter is assumed to be 1cm. The count rate for different fission gas release rates is shown in Figure 8.13.

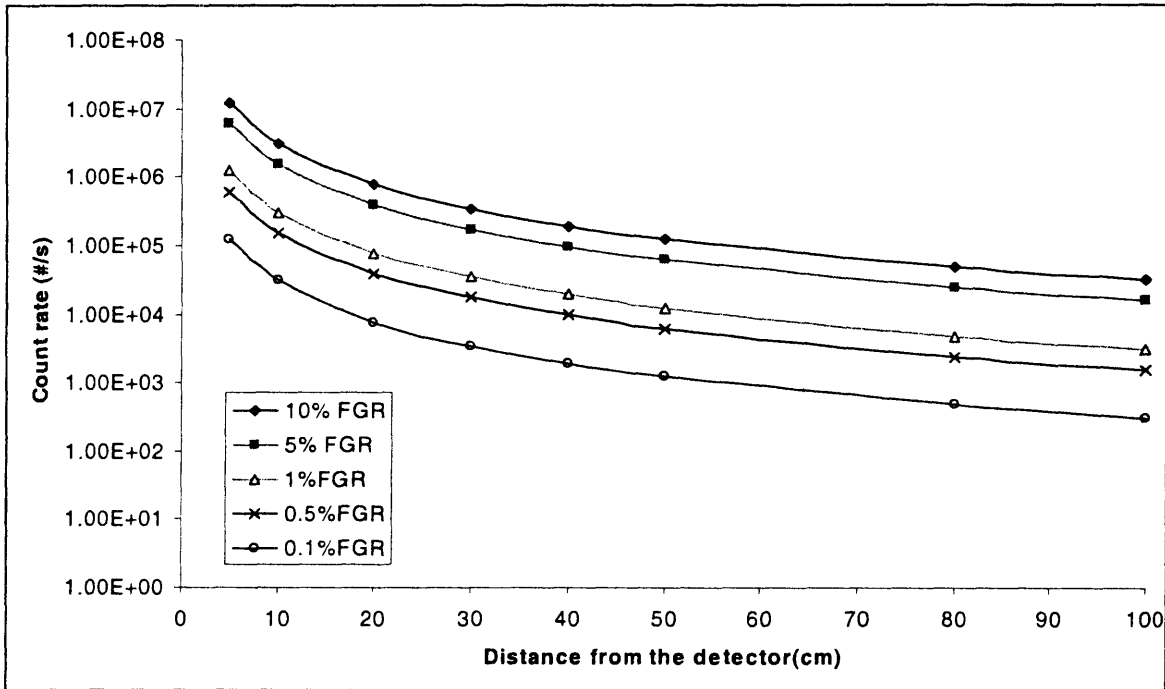


Figure 8.13 NaI Detector Count Rate vs. Distance from Detector at Different FGR

It is seen in the Figure 8.13 that even at 0.1% of fission gas release, there will be enough counts to determine the fission gas release. However, in the real situation, after the fission gas chamber has collected most of the fission gas, it will be heated to room temperature to allow

the fission gas to be evenly dispersed in the volume. The detector will be gauged using simulated gas before measurement.

Chapter 9 Summary and Future Work

9.1 Summary

9.1.1 Steady State Fuel Performance Code Development

The internally and externally cooled annular fuel has been proposed as a new generation of PWR fuels to achieve higher power density, larger safety margin and reduced electricity generation cost. The fuel is designed to be readily capable of being installed in the current LWRs with minimum modifications to the core design. If a large power upgrade is to be implemented, the plant components may have to be altered.

In order to evaluate the annular fuel performance and optimize the annular fuel design, two fuel performance models are developed. The fuel performance models for annular fuel have been developed based on the NRC licensed FRAPCON-3 code. The challenges to be faced for developing a performance code for annular fuel include: (1) installation of additional iteration loops to calculate the heat flux split, (2) performing additional calculations for the inner cladding, (3) calculations of fuel power profile, appropriate for the formation of two rims at the fuel surfaces, and (4) development and implementation of fuel thermal and mechanical models including thermal expansion, thermal conductivity, gap conductance, fuel-cladding interaction and fission gas release models.

Several code structure and performance modeling developments are applicable to both the sintered and VIPAC fuels:

- The solution scheme for the annular fuel performance code has been constructed so that it is able to calculate to high burnup the fuel temperature, heat flux splits, fuel cladding dimensional changes and fuel-clad mechanical interactions at all axial locations.
- The rim effects of the annular fuel have been captured by Monte-Carlo neutronics calculations. These were used to fit a new simple burnup model for annular fuel developed

based on the original FRAPCON-3 burnup model with modifications in pellet radial power shape. The annular fuel burnup model shows good agreement with the neutronics calculations up to high burnup (70MWd/kgU).

- The fuel temperature profiles are calculated by the finite difference method with specific boundary conditions for the internal and external cooling, and the radial mesh scheme is designed so that power peaking and rim effects at the fuel surfaces are captured.

Due to differences between the sintered fuel and VIPAC fuel, specific models for calculations of the fuel dimensional changes, the fuel-cladding interactions and fission gas release have been developed for each type of fuel. For the sintered annular fuel, the following modeling efforts have been made:

- The anchor ring location of fuel thermal expansion is determined and the fuel dimensions are calculated considering the effects of thermal expansion, swelling and densification, etc.
- Fuel relocation is assessed based on which a new empirical relocation model has been developed and implemented in the code.
- A fuel cladding mechanical interaction model has been developed. Three regimes are identified for the fuel-cladding interaction: the free standing cladding regime, the single closure regime and the fuel cladding full contact regime. The interaction mechanisms for each regime are analyzed and solutions are provided.
- A low temperature fission gas release model is implemented for sintered annular fuel by taking into account the double surface effects.

The VIPAC annular fuel performance models are similar to those of the sintered annular fuel except that the fuel exists in a particle form and the gaps between the fuel and cladding no longer exist. In order to simulate the VIPAC fuel behavior, the following models have been developed and implemented:

- An empirical VIPAC fuel thermal conductivity model has been developed based on published data and implemented in the fuel performance code. The empirical VIPAC thermal conductivity model was derived as a function of temperature, burnup, porosity and gas pressure.
- The VIPAC fuel and cladding thermal conductance model has also been developed by modifying the original FRAPCON thermal conductance model. The model incorporates the effects of fuel particle size, gas pressure and interfacial pressure. The model has been compared with experimental data and a sensitivity analysis has been performed on each variable in the model.
- The VIPAC fuel dimensional changes due to thermal expansion, densification and swelling have been calculated based on several assumptions. It is assumed that the bulk thermal expansion and the densification rate for the VIPAC fuel are the same as those of the pelletized fuel. However, the fission product swelling is accommodated by the large porosity of the VIPAC fuel until the VIPAC fuel porosity is reduced to about 4-6%, which is comparable with the sintered fuel.
- The VIPAC fuel cladding mechanical interaction mechanism is similar to the full gap closure regime of the pelletized annular fuel. Therefore, a VIPAC annular fuel cladding interaction has been derived by modifying the sintered annular fuel cladding interaction model. Fuel cladding “slippage” is allowed in the VIPAC annular fuel model.
- An athermal fission gas release model for VIPAC annular fuel has been developed based on FRAPCON-3 low temperature fission gas release model. The surface effect and the resolution effect have been incorporated in the model based on the particular characteristics of the powder fuel particles.

9.1.2 Summary of the Annular Fuel Design and Optimization

For sintered annular fuel:

It is found that the annular fuel rods have lower fission gas release at 100% power and higher fission gas release at 150% power than that of the reference solid fuel (100% power) at the same operation time. The fission gas release from a 150% power annular fuel is about 6%, which is still tolerable with good plenum and cladding design. The hydrogen concentration and the oxide accumulation of the annular fuel are comparable to those of the solid fuel since the cladding heat flux and irradiation time for annular fuel are not much different from the reference solid fuel. The annular fuel gap conductance asymmetry caused by outward thermal expansion has been identified as a major concern due to its potentially effect on MDNBR.

A sensitivity study has shown that a smaller as-fabricated inner gap and a larger outer gap can reduce the gap conductance asymmetry. Meanwhile, the total gap size should be reduced to reduce the cladding strains due to cladding creep-down. The adjustment of inner and outer cladding thicknesses could allow acceptable swelling-induced cladding strains, but higher H₂ concentration and higher fraction of oxide layer pose concerns for cladding failure. The reduction of the inner cladding thickness shows little improvement of gap conductance asymmetry.

The gap asymmetry problem has been analyzed and several approaches are recommended: (1) adjusting the gap sizes to allow a larger outer gap and a smaller inner gap, (2) enlarging the fuel and cladding surface roughness, and (3) spattering the fuel surface with ZrO₂ particles. The above three approaches should be combined to reduce or eliminate the gap imbalance. Large fuel cladding roughness and spattered ZrO₂ particles limit the values of gap conductance upon contact and therefore limit the potential magnitude of conductance imbalance. Fuel roughness and ZrO₂ particles are found to be effective in reducing the strains due to cladding creep-down. The optimized annular fuel is analyzed at different power levels and shows great potential for achieving high burnup (up to 86MWd/kgU rod average) without compromising annular fuel safety.

For VIPAC fuel:

It was found that the 150% power VIPAC fuel operates at lower fuel temperatures than the sintered annular fuel for an initial period due to elimination of the gap temperature drops. After the sintered annular fuel gap closure, the sintered fuel has lower fuel temperature than VIPAC fuel due to higher thermal conductivity. The fission gas release from VIPAC fuel is higher than both the sintered annular and the solid rods since its particle bed composition provides larger surface area and larger porosity which facilitate fission gas release. However, the end of life internal pressure of the VIPAC fuel turned out to be lower than the sintered annular fuel due to the fact that the VIPAC fuel has larger free volume.

The VIPAC annular fuel EOL cladding oxide thicknesses and hydrogen concentrations are comparable to those of the sintered annular fuel since the irradiation durations are the same and the cladding temperatures are not much different. The VIPAC annular rod cladding strains are significantly reduced for two reasons: (1) elimination of the gaps restrains the fuel cladding creep-down due to cladding pressure differential and (2) the large porosity accommodates the fuel solid fission products at high burnup. Another obvious advantage for the VIPAC annular fuel is that it eliminates the gap conductance imbalance problem which is a serious concern for the sintered annular fuel.

A sensitivity study of the important parameters of the VIPAC fuel has been performed to identify the optimum values of these parameters:

- The initial helium gas in the fuel rod plays an important role in improving conductivity of the fuel. At very low gas pressures, the thermal conductivity is low and the fission gas release is rather high. At high pressures, the improvement in thermal conductivity does not reduce the fission gas release since the gas release is mainly activated by athermal mechanism, yet the EOL rod pressure is very high. Therefore the optimum initial helium gas pressure is around 1.4-2.0 MPa.

- Larger particle size reduces the fission gas release by reducing the surface to volume ratio, yet it increases the jump distance which leads to larger temperature drop at the fuel cladding interface. The optimum particle size is around 300-600 μ m.
- Larger smear density results in higher thermal conductivity and improved fission gas resolution rate, the fission gas release reduction by improving the smear density is significant at lower smear densities but at high smear densities the reduction is modest. However higher smear densities lead to higher cladding strain due to reduction of the fission products accommodation capability. The optimum smear density is in the range of 85%-90%.
- The achievable smear density is related to the particle size of the VIPAC fuel. In practice, the fuel is composed of a mixture of particles of different sizes. Therefore, the fission gas release and the cladding strain reduction have to take into account the achievable average particle size and smear density.
- The VIPAC fuel performance can also be improved by addition of uranium metal powders in the particle bed. Due to high thermal conductivity of the uranium metal, it improves the bulk fuel thermal conductivity. The presence of fine metallic powders at the fuel cladding interface also improves the gap conductance. The uranium powder can improve the heavy metal content in the fuel and it serves to control the oxygen potential in the fuel, hence reducing the fuel cladding chemical interaction. However, the uranium powder tends to release all the fission gas at high burnup and its compatibility with water is a concern. If the manufacturing of such fuel in a long fuel rod for LWR can be proven (and so far only the shorter fuel of the LMFBR has been proven in Russia), it would be worth conducting further investigation of this design option.

In general, the VIPAC shows good behavior in terms of cladding strains and gap balance. The fission gas release is predicted to be relatively higher but could be accommodated by large free volumes in fuel rod. The VIPAC fuel needs to operate to a higher burnup (~ 97 MWd/kgU) than the sintered fuel at 150% power due to its smaller fuel content.

9.1.3 Preliminary Findings of Annular Fuel Transient Behavior

Previous analyses by other authors showed that during a large LOCA the Peak Cladding Temperature (PCT) of annular fuel is found to be much lower than that of the solid fuel case (606 °C for annular fuel and 926 °C for solid fuel) and is far below the 1200 °C limit by NRC for cladding temperature during LOCA. It was found that the annular fuel cladding is rapidly quenched to coolant temperature while the solid fuel cladding temperatures decrease more slowly because the annular fuel cladding has lower initial temperature and the thinner annular fuel can be more efficiently cooled than the solid fuel.

A preliminary investigation of annular fuel Reactivity Initiated Accident (RIA) behavior has been performed in this work using the FRAPTRAN code. Although FRAPTRAN does not capture the internal cooling of the annular fuel, by imposing the cladding high burnup parameters and the fuel power profile in the annular pellet version of the code, it could provide some insights into the annular fuel behavior and give a relatively conservative estimation of fuel RIA behavior.

Three hypothetical cases have been calculated for comparison: a 150% power sintered annular fuel case, a 150% power VIPAC annular fuel case and a 100% power solid fuel case. It is assumed that the power pulse height (above the hot zero power level) of the annular fuel is 2.6 times of that for solid fuel, which is derived by scaling the power pulse by the annular to solid fuel linear power ratio.

It has been found that, although the power pulse height of annular fuel is significantly larger, the relatively larger fuel volume plus an even temperature distribution makes the fuel peak enthalpy comparable to that of the solid case. Permanent hoop strains for the VIPAC annular fuel are the highest due to absence of fuel-clad gap. The sintered annular fuel has lower permanent hoop strain than that of the solid fuel case because the lower power.

9.1.4 Annular Irradiation Test Planning

Irradiation tests are being performed in the MITR-II research reactor of the MIT Nuclear Reactor Laboratory. Some work to prepare for irradiation test has been accomplished during this work:

- Thermal calculations have been performed to identify the irradiation capsule dimensions so that the fuel temperature profile of the target annular fuel can be simulated and occurrence of the nucleate boiling is avoided.
- The top test segment will be extracted from the reactor after about 100 days of irradiation and 30 days of cooling. Initial conditions of the fuel sample and extraction procedures have been determined.
- A fission gas release measurement device has been designed. The fission gas will be released to a free volume where measurement is performed. Initial assessment has shown that most of the fission gas can be extracted to the free volume and even at 0.1% of fission gas release, there will be enough counts for Kr-85 in a NaI detector.
- The burnup measurement set has also been designed. The lead collimator allows scanning of the fuel activity in each axial location while maintaining adequate shielding to reduce the dose and the background noise. It was found by using 15 inch long lead collimator (weighing ~42kg), the count rate for Cs-137 is large enough and the background noise is negligible.

9.2 Recommendations for Future Work

9.2.1 Validation of Fuel Models with Experimental Data

Although annular fuel performance codes have been developed, some of the performance models were based on assumptions which need to be validated with experimental data. These assumptions include:

- The fuel cladding mechanical interaction models for both the sintered and VIPAC fuel need to be validated. For annular fuel rod, since the gap conductance imbalance poses serious concerns for fuel rod safety, it is vital to identify through experiments the fuel dimensional changes due to thermal expansion and the evolution of the fuel-cladding gaps with the irradiation history. For VIPAC fuel, it is important to find out the bulk thermal expansion rate for fuel with various packing smear densities, particle sizes and shapes. It is also critical to identify the dimensional increments due to the thermal expansion at both inner and outer fuel-clad interface.
- The options to circumvent the annular fuel gap imbalance problem need to be further investigated with experiments. These options include: (1) increase of the fuel and cladding roughness, (2) fabrication of different initial gaps, (3) coating and spattering of the fuel surface with ZrO_2 or other materials, and (4) alternative cladding materials.
- Fuel cracking behavior of the sintered annular fuel need to be analyzed and its implication for fuel-cladding interaction should be evaluated.
- The VIPAC fuel irradiation behavior should also be investigated by experiments, especially the irradiation induced sintering and swelling. The bulk dimensional changes due to swelling and sintering should be measured to compare with the model prediction
- The fission gas behavior of the sintered annular fuel and VIPAC fuel at low temperatures to high burnup should be assessed with experiment. The fission gas release models should

be validated with experimental data. The impact of increased surface effects should be analyzed. For VIPAC fuel, the impacts of particle size shape and smear density on fission gas release should be investigated.

- The benefits and disadvantages of the addition of uranium powders into the VIPAC fuel should be further investigated.

9.2.2 Future Modeling Work

The following future work for fuel modeling is recommended:

- Currently, the flow split loop is not installed due to convergence problem and fuel flow split is dictated as input. In the future, the flow split should be calculated with reasonable accuracy. The DNBR should also be calculated in the code in order to capture the MDNBR with dynamic gap size evolution. This can be accomplished if FRAPCON is coupled to a subchannel code such as VIPRE.
- The annular fuel RIA and LOCA behavior should be modeled with a specially modified FRAPTRAN code. Coupling of the future FRAPTRAN ANNULAR and RELAP should accomplish this. This effort should include: (1) addition of an inner cladding, (2) fuel temperature calculations with new boundary conditions, (3) fuel cladding mechanical interaction model, (4) fission gas release, and fuel swelling behavior during the transient.
- The VIPAC fuel with multiple particle sizes should be modeled.

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APPENDIX A: Reference Westinghouse LWR Design

Operating Parameters	Value
Plant	
Number of primary loops	4
Total heat output of the core(MWth)	3411
Total plant thermal efficiency (%)	34
Electrical output of the plant (MWe)	1150
Energy deposited in the fuel (%)	97.4
Energy deposited in the moderator	2.6
Core	
Core thermal inside diameter/outside diameter (m)	3.76/3.87
Mass of UO ₂ (MT)	101
Mass of U (MTU)	88.2
Mass of cladding material (MT)	23.1
Rated power density (kW/l)	104.5
Specific power (kW/kgU)	38.7
Average linear heat generation rate (kW/ft)	5.6
Core volume (m ³)	32.6
Design axial enthalpy rise (F _{ΔH})	1.65
Allowable core total peak factor (F _Q)	2.5
Primary coolant	
System pressure (MPa)	15.51
Total core flow rate (Mg/s)	18.63
Rated coolant mass flux (kg/m ² -s)	2087.6
Core inlet temperature (°C)	292.7
Fuel rods	
Total number	50592
Fuel density (% of theoretical)	94
Pellet diameter (mm)	8.19
Pellet height (mm)	13.4
Fuel-clad radial gap width (μm)	82
Cladding material	Zircaloy-4
Cladding thickness (mm)	0.57
Cladding outer diameter (mm)	9.5
Total fuel height (m)	3.66
Fuel assemblies	
Assembly array	17×17
Number of assemblies	193
Number of fuel rods per assembly	264
Number of grids per assembly	7
Rod pitch (mm)	12.6
Overall dimensions (mm × mm)	214 × 214

APPENDIX B: Selected Subroutines for Annular Fuel Codes

Subroutine fcmi

```
*define pc
*define in32
*define inel
*deck fcmi

c *****
c   fcmi is developed to calculate the annular fuel cladding mechanical c
c   interactions at MIT by Yi Yuan c
c   *****

      subroutine fcmi (GapPress,CoolantPress
+ ,CladAveTemp,CladAveTemp_inner,IgapGapIndex,IgapGapIndex_inner
+ ,eps,eps_inner,epp,epp_inner,FuelSurfDispl,FuelSurfDispl_inner
+ ,sig,sig_inner,reprs,reprs_inner,rfepr,rfepr_inner
+ ,feps,feps_inner
+ ,CladEffPlasStrain,CladEffPlasStrain_inner
+ ,AxialNodLength,intgr,FuelCladGap,FuelCladGap_inner
+ ,RinterfacPress,RinterfacPress_inner,rci,rci_inner
+ ,rco, rco_inner,dlrod,dlrel
+ ,rfo,rfo_inner,crep1,crep1_inner,eps1,eps1_inner
+ ,OldCladStrn,OldCladStrn_inner,OldFuelStrn,OldFuelStrn_inner
+ ,OldCladAvTemp,OldCladAvTemp_inner,OldGapPress,OldCoolPress
+ ,OldFuelDispl,OldFuelDispl_inner
+ ,IgapIndexOld,IgapIndexOld_inner,IbothgapclosureOld
+ ,eppsav,eppsav_inner,epsav,epsav_inner,sig1,sig1_inner
+ ,dtime,time
+ ,CladInSurDisp,CladInSurDisp_inner,CreepStrain,CreepStrain_inner
+ ,reprsav,reprsav_inner,rfeprsav,rfeprsav_inner
+ ,PlastStrnep1,PlastStrnep1_inner,epp1,epp1_inner,nudep,nudep_inner
+ ,CladH2Concen,CladH2Concen_inner,ExcessH2Concen
+ ,ExcessH2Concen_inner,UniformAxNodStrn,hrad,na,nr
+ ,CladDiamHot,CladDiamHot_inner,displacement) !no!

c
c   ***this package of subroutines performs an elasto-plastic
c   analysis of a typical pwr fuel rod. fuel radial displacements
c   and axial strains at each axial node are input.
c   fcmi is called from fracas.
c   *****
c   input arguments
```

```

c *****
c dtime - time increment (hrs)
c AxialNodLength - axial node length (in)
c feps - hoop, axial, & radial fuel strains
c it - power-time step number
c jjj - axial node index
c mode - simplified stack switch
c na - maximum number of axial nodes
c ndbg - debug output index
c nrelax - creep flag
c nplast - elastic-plastic flag
c OldCoolPress - coolant channel pressure of previous power step (psia)
c OldGapPress - rod internal gas pressure of previous power step (psia)
c OldCladAvTemp - cladding average temperature of previous power step (F)
c OldFuelDispl - fuel radial displacement of previous power step (in)
c OldCladStrn - old cladding strains - previous power step
c OldFuelStrn - old fuel strains of previous power step
c IgapIndexOld - old values of gap closure index
c CoolantPress - coolant channel pressure (psia)
c GapPress - rod internal gas pressure (psia)
c rci - Outer cladding inside radius (in)
c rco - Outer cladding outside radius (in)
c repsav - residual cladding strain
c rfo - pellet radius (in)
c rfpsav - residual fuel strain
c CladAveTemp - cladding average temperature (F)
c time - end of step time (hrs)
c FuelSurfDispl - fuel surface radial displacement (in)
c CladH2Concen(k) = cladding H2 concentration at axial node k (ppm)
c ExcessH2Concen(k) = excess cladding H2 concentration at axial node k (ppm)
c *****
c output arguments
c *****
c dlrel - relative change in length wrt the fuel length change (in)
c dlrod - change in length of the active cladding length (in)
c CreepStrain - total accumulated creep strain (dimensionless)
c FuelCladGap - radial gap thickness (in)
c IgapGapIndex - gap closure index
c CladEffPlasStrain - cladding effective plastic strain (in/in)
c epp - cladding hoop, axial, & radial strain (in/in)
c eps - cladding hoop, axial, & radial strain per node
c eppsav - plastic cladding strain
c epsav - cladding strain
c RinterfacPress - interfacial pressure (psia)
c reps - residual cladding strains
c rfeps - residual fuel strains

```

```

c sig - cladding hoop, axial, & radial stress per node (psi)
c UniformAxNodStrn(k) = value of uniform strain at axial node k
c cldIdx- cladding index for inner and outer cladding
c      =0, inner cladding. =1, outer cladding
c *****
c implicit real*8 (a-h,o-z)
  real nudepp
    real nudepp_inner,movdisplacement
    real gap_outer,gap_inner,gap_total
    integer cldIdx !index for inner and outer cladding
    integer gapIdx !index for both gap closure
    dimension CladAveTemp(21),eps(21,3) ,epp(21,3)
+    ,epp1(21,3) ,AxialNodLength(21),CladEffPlasStrain(21)
+ ,sig(21,3),reps(21,3) ,FuelSurfDispl(21),PlastStrnep1(21)
+ ,CladInSurDisp(21),RinterfacPress(21) ,FuelCladGap(21)
+ ,rfeps(21,3)
+ ,feps(21,3) ,csig(3) ,ceps(3) ,cepp(3)
+ ,GapPress(21) ,CoolantPress(21) ,sig1(21,3)
+ ,eps1(21,3)
+ ,OldCladStrn(21) ,OldFuelStrn(21) ,eppsav(21,3)
+ ,epsav(21)
+ ,OldCladAvTemp(21) ,OldGapPress(21),OldCoolPress(21)
+ ,OldFuelDispl(21)
+ ,repsav(21,3),rfpsav(21,3),IgapGapIndex(21),IgapIndexOld(21)
+ ,CreepStrain(21),crep1(21)
+ ,nudepp(21,3),intgr(10),hrad(50,21)
+ ,CladH2Concen(21), ExcessH2Concen(21), UniformAxNodStrn(21)
+ ,CladDiamHot(21), displacement(21)!no!

c add parameters for inner cladding
  dimension CladAveTemp_inner(21),eps_inner(21,3) ,epp_inner(21,3)
+ ,epp1_inner(21,3),CladEffPlasStrain_inner(21),sig_inner(21,3)
+ ,reps_inner(21,3),FuelSurfDispl_inner(21),PlastStrnep1_inner(21)
+ ,CladInSurDisp_inner(21),RinterfacPress_inner(21)
+ ,FuelCladGap_inner(21),rfeps_inner(21,3)
+ ,feps_inner(21,3),csig_inner(3) ,ceps_inner(3) ,cepp_inner(3)
+ ,CoolantPress_inner(21) ,sig1_inner(21,3)
+ ,eps1_inner(21,3)
+ ,OldCladStrn_inner(21) ,OldFuelStrn_inner(21) ,eppsav_inner(21,3)
+ ,epsav_inner(21)
+ ,OldCladAvTemp_inner(21)
+ ,OldFuelDispl_inner(21)
+ ,repsav_inner(21,3),rfpsav_inner(21,3),IgapGapIndex_inner(21)
+ ,IgapIndexOld_inner(21)
+ ,CreepStrain_inner(21),crep1_inner(21)
+ ,nudepp_inner(21,3) ,CladH2Concen_inner(21)

```

```

+ ,ExcessH2Concen_inner(21),gapIdx(21)
+, Ibothgapclosure(21),IbothgapclosureOld(21),CladDiamHot_inner(21) !no!
c **find free clad displacement due to internal and
c external pressures. compute a gap width.
  ndbg = integr(1)
  iquit = integr(2)
  jjj = integr(3)
  mode = integr(4)
  it = integr(5)
  nplast = integr(6)
  nrelax = integr(7)
  nn = na-1
  mode = 1
  delta = rci-rfo
    delta_inner=rfo_inner-rci_inner

  do 120 j=1,nn
    cepp(1) = epp(j,1)
    cepp_inner(1) = epp_inner(j,1)
    cepp(2) = epp(j,2)
    cepp_inner(2) = epp_inner(j,2)
    cepp(3) = epp(j,3)
    cepp_inner(3) = epp_inner(j,3)
    cep = CladEffPlasStrain(j)
    cep_inner = CladEffPlasStrain_inner(j)
    crep = CreepStrain(j)
    crep_inner = CreepStrain_inner(j)
    fs = 0.0
    testr=550.0
    excessh2 = min(ExcessH2Concen(j),testr)
    excessh2_inner = min(ExcessH2Concen_inner(j),testr)
c
c   calculate the outer cladding free standing condition
c last parameter 1 indicate it is outer cladding

    call cladf (GapPress(j),CoolantPress(j),CladAveTemp(j),rci,
+   rci_inner,rco,rco_inner,csig,ceps,cepp,cep,fs ,nrelax
+   ,dtime,time,crep,CladH2Concen(j),excessh2,nplast,1) !no!

    sig1(j,1) = csig(1)
    sig1(j,2) = csig(2)
    sig1(j,3) = 0.e0
    eps1(j,1) = ceps(1)
    eps1(j,2) = ceps(2)
    eps1(j,3) = ceps(3)
    epp1(j,1) = cepp(1)

```

```

    epp1(j,2) = cepp(2)
    epp1(j,3) = cepp(3)
    PlastStrnep1(j) = cep
    crep1(j) = crep
    urc1 = 0.5*((rco+rci)*eps1(j,1)
&          -(rco-rci)*eps1(j,3))

c
c   calculate the inner cladding free standing condition
c last parameter 0 indicate it is inner cladding

    call cladf (GapPress(j),CoolantPress(j),CladAveTemp_inner(j)
+   ,rci,rci_inner,rco,rco_inner,csig_inner,ceps_inner
+   ,cepp_inner,cep_inner,fs ,nrelax,dtime,time,crep_inner
+   ,CladH2Concen_inner(j),excesh2_inner,nplast,0) !no!

    sig1_inner(j,1) = csig_inner(1)
    sig1_inner(j,2) = csig_inner(2)
    sig1_inner(j,3) = 0.e0
    eps1_inner(j,1) = ceps_inner(1)
    eps1_inner(j,2) = ceps_inner(2)
    eps1_inner(j,3) = ceps_inner(3)
    epp1_inner(j,1) = cepp_inner(1)
    epp1_inner(j,2) = cepp_inner(2)
    epp1_inner(j,3) = cepp_inner(3)
    PlastStrnep1_inner(j) = cep_inner
    crep1_inner(j) = crep_inner

c urc1_inner is the inner surface movement of inner cladding
    urc1_inner = 0.5*((rco_inner+rci_inner)*eps1_inner(j,1)
+   +(rci_inner-rco_inner)*eps1_inner(j,3))
c above are modified for inner cladding

c ***now check for interference for outer cladding
    gap_outer = urc1+delta-FuelSurfDispl(j)
    gap_inner = delta_inner+FuelSurfDispl_inner(j)-urc1_inner
    gap_total=gap_outer+gap_inner
c*****check if both gaps are closed*****
    if (gap_total) 80, 80, 85
80      Ibothgapclosure(j)=1
    go to 90
85      Ibothgapclosure(j)=0
90      continue
c*****check if outer gap is closed*****

```

```

        if (gap_outer) 100,100,110
100    IgapGapIndex(j) = 1
        go to 120
110    IgapGapIndex(j) = 0
120    continue
c*****check if inner gap is closed*****
        if (gap_inner) 105,105,115
105    IgapGapIndex_inner(j) = 1
        go to 125
115    IgapGapIndex_inner(j) = 0
125    continue
c major modifications start here
        do 190 j=1,nn
c        if (IgapGapIndex(j)) 170,170,130
        if (Ibothgapclosure(j)) 170,170,130
c    node j is in contact
c    compute prescribed axial strain in clad
c    based on last values of axial strain
c    prior to contact. local fuel radial
c    displacement is passed to *couple* .
130    continue
c *****
        if (nrelax.eq.1) go to 140
        if (IgapGapIndex(j).eq.1.and.j.ne.jjj) go to 190
140    continue
c *****
c        if (IgapIndexOld(j).ne.0) go to 150
        if (IbothgapclosureOld(j).ne.0) go to 150

        call gapcls(CladAveTemp,CladAveTemp_inner,GapPress
& ,CoolantPress,FuelSurfDispl,FuelSurfDispl_inner,delta,delta_inner
& ,epp,epp_inner,CladEffPlasStrain,CladEffPlasStrain_inner
& ,rci,rci_inner,rco,rco_inner,rfo,rfo_inner,OldCladAvTemp
& ,OldCladAvTemp_inner,OldGapPress,OldCoolPress,OldFuelDispl
& ,OldFuelDispl_inner,j,feps,feps_inner,rfeps,rfeps_inner
& ,reps,reps_inner,OldCladStrn,OldCladStrn_inner
& ,OldFuelStrn,OldFuelStrn_inner,nrelax,dtime
& ,time,CreepStrain,CreepStrain_inner,CladH2Concen(j)
& ,CladH2Concen_inner(j),ExcessH2Concen(j),ExcessH2Concen_inner(j)
& ,na,iquit,nplast)

150    eps(j,2) = reps(j,2)+(feps(j,2)-rfeps(j,2))
        cepts(2) = eps(j,2)
        cepp(1) = epp(j,1)
        cepp(2) = epp(j,2)
        cepp(3) = epp(j,3)

```

```

    cep = CladEffPlasStrain(j)
    crep = CreepStrain(j)
c
    eps_inner(j,2) = reps_inner(j,2)
+    +(feps_inner(j,2)-rfeps_inner(j,2))
    ceps_inner(2) = eps_inner(j,2)
    cepp_inner(1) = epp_inner(j,1)
    cepp_inner(2) = epp_inner(j,2)
    cepp_inner(3) = epp_inner(j,3)
    cep_inner = CladEffPlasStrain_inner(j)
    crep_inner = CreepStrain_inner(j)

c no slip assumed in axial direction as long
c as interface pressure is above local gas pressure
c dincree=0
c155 dincree=dincree+displadjust
c
    call couple (GapPress(j),CoolantPress(j),CladAveTemp(j)
+    ,CladAveTemp_inner(j),rci,rci_inner
+    ,rco,rco_inner,csig,csig_inner,ceps,ceps_inner,cepp,cepp_inner
+    ,cep,cep_inner,FuelSurfDispl(j),FuelSurfDispl_inner(j)
+    ,delta,delta_inner,RinterfacPress(j),RinterfacPress_inner(j)
+    ,ndbg,nrelax,dtime,time,crep,crep_inner
+    ,CladH2Concen(j),CladH2Concen_inner(j),ExcessH2Concen(j)
+    ,ExcessH2Concen_inner(j),iquit,nplast,it,1)
c
    sig(j,1) = csig(1)
    sig(j,2) = csig(2)
    sig(j,3) = 0.0
    eps(j,1) = ceps(1)
    eps(j,2) = ceps(2)
    eps(j,3) = ceps(3)
    epp(j,1) = cepp(1)
    epp(j,2) = cepp(2)
    epp(j,3) = cepp(3)
    CladEffPlasStrain(j) = cep
    CreepStrain(j) = crep
c
    sig_inner(j,1) = csig_inner(1)
    sig_inner(j,2) = csig_inner(2)
    sig_inner(j,3) = 0.0
    eps_inner(j,1) = ceps_inner(1)
    eps_inner(j,2) = ceps_inner(2)
    eps_inner(j,3) = ceps_inner(3)
    epp_inner(j,1) = cepp_inner(1)
    epp_inner(j,2) = cepp_inner(2)

```

```

    epp_inner(j,3) = cepp_inner(3)
    CladEffPlasStrain_inner(j) = cep_inner
    CreepStrain_inner(j) = crep_inner

c *****
    if (nrelax.eq.1) go to 160
    eppsav(j,1) = cepp(1)
    eppsav(j,2) = cepp(2)
    eppsav(j,3) = cepp(3)
    epsav(j) = cep
    repsav(j,1) = reps(j,1)
    repsav(j,2) = reps(j,2)
    repsav(j,3) = reps(j,3)
    rfpsav(j,1) = rfeps(j,1)
    rfpsav(j,2) = rfeps(j,2)
    rfpsav(j,3) = rfeps(j,3)

c
    eppsav_inner(j,1) = cepp_inner(1)
    eppsav_inner(j,2) = cepp_inner(2)
    eppsav_inner(j,3) = cepp_inner(3)
    epsav_inner(j) = cep_inner
    repsav_inner(j,1) = reps_inner(j,1)
    repsav_inner(j,2) = reps_inner(j,2)
    repsav_inner(j,3) = reps_inner(j,3)
    rfpsav_inner(j,1) = rfeps_inner(j,1)
    rfpsav_inner(j,2) = rfeps_inner(j,2)
    rfpsav_inner(j,3) = rfeps_inner(j,3)
160    continue
c *****
    FuelCladGap(j) = 0.0
    FuelCladGap_inner(j) = 0.0
    CladInSurDisp(j)=FuelSurfDispl(j)-delta
    CladInSurDisp_inner(j)=FuelSurfDispl_inner(j)+delta_inner

    go to 190
170    continue
    k = j
    sig(k,1) = sig1(k,1)
    sig(k,2) = sig1(k,2)
    sig(k,3) = sig1(k,3)
c *****
    sig_inner(k,1) = sig1_inner(k,1)
    sig_inner(k,2) = sig1_inner(k,2)
    sig_inner(k,3) = sig1_inner(k,3)
c *****
    eps(k,1) = eps1(k,1)

```

```

      eps(k,2) = eps1(k,2)
      eps(k,3) = eps1(k,3)
c *****
      eps_inner(k,1) = eps1_inner(k,1)
      eps_inner(k,2) = eps1_inner(k,2)
      eps_inner(k,3) = eps1_inner(k,3)
c *****
      reps(k,2) = eps(k,2)
      reps_inner(k,2) = eps_inner(k,2)
c *****
c case 1. if both gap are open
      if (IgapGapIndex(j).eq.0.and.
&   IgapGapIndex_inner(j).eq.0) then
          rfeps(k,2) = feps(k,2)
          rfeps_inner(k,2) = feps_inner(k,2)
c
          CladInSurDisp(k)=0.5*(rco+rci)*eps(k,1)
          & -0.5*(rco-rci)*eps(k,3)
c
          CladInSurDisp_inner(k)=0.5*(rco_inner+rci_inner)
          &*eps_inner(k,1)
          &+0.5*(rci_inner-rco_inner)*eps_inner(k,3)
c
          FuelCladGap(k) = CladInSurDisp(k)+delta-FuelSurfDispl(k)
c
          FuelCladGap_inner(k) = delta_inner+FuelSurfDispl_inner(k)
          &-CladInSurDisp_inner(k)    inner gap
          end if
c case 2. if outside gap is closed and inside gap is open
          if (IgapGapIndex(j).eq.1.and.
&   IgapGapIndex_inner(j).eq.0) then

              CladInSurDisp(k)=0.5*(rco+rci)*eps(k,1)
              & -0.5*(rco-rci)*eps(k,3)
c
              CladInSurDisp_inner(k)=0.5*(rco_inner+rci_inner)
              &*eps_inner(k,1)
              &+0.5*(rci_inner-rco_inner)*eps_inner(k,3)
c
              movdisplacement=FuelSurfDispl(k)-(CladInSurDisp(k)+delta)
              if (movdisplacement.le.0.0) movdisplacement=0.0

              FuelSurfDispl(k)=CladInSurDisp(k)+delta
c
              FuelSurfDispl_inner(k)=
&   FuelSurfDispl_inner(k)-(movdisplacement) !no!

```

```

c
  feps(j,1) = FuelSurfDispl(j)/rpp
  feps_inner(j,1) = FuelSurfDispl_inner(j)/rpp_inner
c
  rfeps(k,2) = feps(k,2)
  rfeps_inner(k,2) = feps_inner(k,2)
c
  FuelCladGap(k) = 0.0
  FuelCladGap_inner(k) = delta_inner+FuelSurfDispl_inner(k)
  &-CladInSurDisp_inner(k) inner gap
c readjust the hotring arrangement
  hrad(1,j)=rfo+FuelSurfDispl(k)
  do 175 ll=2,nr
    hrad(ll,j)=hrad(ll,j)-(movdisplacement-displacement(j)) !no!
175 continue !no!
c displacement(j)=movdisplacement
  end if

c case 3. if inside gap is closed and outside gap is open
  if (IgapGapIndex(j).eq.0.and.
    & IgapGapIndex_inner(j).eq.1) then

    CladInSurDisp(k)=0.5*(rco+rci)*eps(k,1)
    & -0.5*(rco-rci)*eps(k,3)
c
    CladInSurDisp_inner(k)=0.5*(rco_inner+rci_inner)
    &*eps_inner(k,1)
    &+0.5*(rci_inner-rco_inner)*eps_inner(k,3)
c
    movdisplacement=CladInSurDisp_inner(k)
    & -FuelSurfDispl_inner(k)-delta_inner
    if (movdisplacement.le.0.0) movdisplacement=0.0

    FuelSurfDispl_inner(k)=CladInSurDisp_inner(k)-delta_inner
c
    FuelSurfDispl(k)=
    & FuelSurfDispl(k)+(movdisplacement) !no!
c
    feps(j,1) = FuelSurfDispl(j)/rpp
    feps_inner(j,1) = FuelSurfDispl_inner(j)/rpp_inner
c
    rfeps(k,2) = feps(k,2)
    rfeps_inner(k,2) = feps_inner(k,2)
c
    FuelCladGap_inner(k) = 0
    FuelCladGap(k) = CladInSurDisp(k)+delta-FuelSurfDispl(k) inner gap

```

```

c adjust the hotring diameters
displadjust
  hrad(nr,j)=rfo_inner+FuelSurfDispl_inner(k)
  displacement(j)=movdisplacement
  end if
c for outer cladding
  RinterfacPress(k) = 0.0
  epp(k,1) = epp1(k,1)
  epp(k,2) = epp1(k,2)
  epp(k,3) = epp1(k,3)
  CladEffPlasStrain(k) = PlastStrnep1(k)
  CreepStrain(k) = crep1(k)
  CladDiamHot(k)=2.d0*(CladInSurDisp(k)+rci) !no!

c for inner cladding
  RinterfacPress_inner(k) = 0.0
  epp_inner(k,1) = epp1_inner(k,1)
  epp_inner(k,2) = epp1_inner(k,2)
  epp_inner(k,3) = epp1_inner(k,3)
  CladEffPlasStrain_inner(k)=PlastStrnep1_inner(k)
  CreepStrain_inner(k) = crep1_inner(k)
  CladDiamHot_inner(k)=2.d0*(CladInSurDisp_inner(k)+rci_inner)
c *****
  if (nrelax.eq.1) go to 180
  if (j.ne.jjj) go to 190
  eppsav(k,1) = epp1(k,1)
  eppsav(k,2) = epp1(k,2)
  eppsav(k,3) = epp1(k,3)
  epsav(k) = PlastStrnep1(k)
  repsav(k,1) = reps(k,1)
  repsav(k,2) = reps(k,2)
  repsav(k,3) = reps(k,3)
  rfpsav(k,1) = rfeps(k,1)
  rfpsav(k,2) = rfeps(k,2)
  rfpsav(k,3) = rfeps(k,3)
c*****for inner cladding*****
  eppsav_inner(k,1) = epp1_inner(k,1)
  eppsav_inner(k,2) = epp1_inner(k,2)
  eppsav_inner(k,3) = epp1_inner(k,3)
  epsav(k) = PlastStrnep1(k)
  epsav_inner(k) = PlastStrnep1_inner(k)
  repsav_inner(k,1) = reps_inner(k,1)
  repsav_inner(k,2) = reps_inner(k,2)
  repsav_inner(k,3) = reps_inner(k,3)
  rfpsav_inner(k,1) = rfeps_inner(k,1)
  rfpsav_inner(k,2) = rfeps_inner(k,2)

```

```

      rfpsav_inner(k,3) = rfeps_inner(k,3)
180   continue
c   ****
      go to 190
190   continue
      hrad(1,j)=rfo+FuelSurfDispl(k)
      hrad(nr,j)=rfo_inner+FuelSurfDispl_inner(k)

      CladDiamHot(k)=2.d0*(CladInSurDisp(k)+rci) !no!
      CladDiamHot_inner(k)=2.d0*(CladInSurDisp_inner(k)+rci_inner) !no!

c   ***compute rod length and displacement between fuel stack + clad
      dlrod = 0.0
      dlrel = 0.0
      do 200 n=1,nn
c      dlrod = dlrod+(0.5*eps(n,2)
c      &+0.5*eps_inner(n,2))*AxialNodLength(n)
      dlrod = dlrod+eps(n,2)*AxialNodLength(n)
      dlrel = dlrel+0.5*(((eps(n,2)-feps(n,2))
      &+(eps_inner(n,2)-feps_inner(n,2))))*AxialNodLength(n)   modified
200   continue
      do 210 k=1,nn
      OldCladStrn(k) = eps(k,2)
      OldFuelStrn(k) = feps(k,2)
      IgapIndexOld(k) = IgapGapIndex(k)
      OldCladAvTemp(k) = CladAveTemp(k)
      OldGapPress(k) = GapPress(k)
      OldCoolPress(k) = CoolantPress(k)
      OldFuelDispl(k) = FuelSurfDispl(k)
c
      OldCladStrn_inner(k)= eps_inner(k,2)
      OldFuelStrn_inner(k)= feps_inner(k,2)
      IgapIndexOld_inner(k)= IgapGapIndex_inner(k)
      OldCladAvTemp_inner(k)= CladAveTemp_inner(k)
      OldFuelDispl_inner(k) = FuelSurfDispl_inner(k)
      IbothgapclosureOld(k)=Ibothgapclosure(k)
210   continue

      return
      end

```

Subroutine couple

```
*define pc
*define in32
*define inel
*deck couple
```

```
c *****
c  subroutine calculates the cladding stress and strains as well as c
c  fuel-clad interfacial pressure as both the inner and outer gaps c
c  are closed
c  *****
c  subroutine couple (GapPress,CoolantPress,CladAveTemp
&  ,CladAveTemp_inner,rci,rci_inner,rco,rco_inner
& ,csig,csig_inner,ceps,ceps_inner, cepp,cepp_inner,cep
& ,cep_inner,FuelSurfDispl,FuelSurfDispl_inner,delta,delta_inner
& ,RinterfacPress,RinterfacPress_inner,ndbg,nrelax,dtime,time,crep
& ,crep_inner,CladH2Concen,CladH2Concen_inner
& ,ExcessH2Concen,ExcessH2Concen_inner,iquit,nplast,it,Index) !no!
```

INCLUDE 'CXML_INCLUDE.F90'

```
c
c  the following common block consists of material properties
c  used in fracas 1, fracas 2
c  common /matens/ anrin ,comp ,cwkf ,cwnf ,deloxy ,flux,
&  ,fnck ,fncn ,fotmtl ,frden ,ftmelt ,rstran,
&  ,clcrip(22,2) ,jclad ,nnncrp
c  anrin - contractile strain ratio during uniaxial tensil test
c  (hoop strain)/(radial strain)
c  comp - puo2 content (wt%)
c  cwkf - effective cold work for strength coefficient (m**2/m**2)
c  cwnf - effective cold work for strain hardening exponent
c  deloxy - change from the oxygen concentration of as received
c  cladding (kg oxygen/kg zircaloy)
c  flux - fast neutron flux (n/m**2-sec)
c  fnck - effective fast fluence for strength coefficient (n/m**2)
c  fncn - effective fast fluence for strain hardening exponent
c  fotmtl - fuel oxygen to metal raito
c  frden - (fuel actual density)/(fuel theoritical density)
c  ftmelt - uo2 or mixed oxide fuel melting points (K)
c  rstran - true strain rate (1/sec)
c  CladH2Concen - concentration of H2 in cladding (ppm)
c  ExcessH2Concen - concentration of H2 in cladding above solubility limit(ppm)
c  implicit real*8 (a-h,o-z)
```

REAL A(8,8), B(8)

INTEGER IPIV(8)

```

dimension csig(3) ,ceps(3) ,cepp(3)
& ,depp(3) ,oldepp(3) ,alfdt(3)
dimension csig1(3) ,ceps1(3) ,cepp1(3)
& ,depp1(3) ,oldepp1(3) ,alfdt1(3)
dimension csig_inner(3) ,ceps_inner(3) ,cepp_inner(3)
& ,depp_inner(3) ,oldepp_inner(3) ,alfdt_inner(3)
c
c real nudepp(3),nudepp_inner(3)
c the following data statement contains various conversion factors
c data cnmtps / 6.894757e3 /, cktor / 1.8e0 /, cftor / 459.67e0 /
c data zero / 0.e0 /, small / 1.e-4 /
c *****
c input arguments
c *****
c cep - temporary effective plastic strain (in/in)
c cepp - temporary hoop, axial, & radial plastic strains (in/in)
c ceps - temporary hoop, axial, & radial strains (in/in)
c crep - creep strain (in/in)
c csig - temporary hoop, axial, & radial stress (psi)
c delta - cold state radial gap (in)
c dtime - creep step time increment (hrs)
c iquit - termination index
c it - power-time step index
c ndbg - debug output index
c nplast - elastic-plastic flag
c = 0, elastic calculation performed
c = 1, plasticity calculation considered
c nrelax - creep index
c = 0, no creep calculation
c = 1, creep calculation performed
c CoolantPress - coolant channel pressure (psia)
c GapPress - rod internal gas pressure (psia)
c CladAveTemp - cladding average temperature (F)
c time - time at the beginning of creep step (hrs)
c FuelSurfDispl - fuel radial displacement (in)
c rci - Outer cladding inside radius (in)
c rco - Outer cladding outside radius (in)
c *****
c output arguments
c *****
c RinterfacPress - interfacial pressure (psi)
c *****
c ***check here for elastic solution
c compute stresses

```

c

```
tcak = (CladAveTemp+cftor)/cktor
tcak_inner = (CladAveTemp_inner+cftor)/cktor
e = celmod(tcak,fnck,cwkf,deloxy)/cnmtps
cr = cshear(tcak,fnck,cwkf,deloxy)/cnmtps
v = e/(2*cr)-1.
call cthexp (tcak,cathex,cdthex)
alfdt(1) = cdthex
alfdt(2) = cathex
alfdt(3) = cdthex
```

c

```
call cthexp (tcak_inner,cathex_inner,cdthex_inner)
alfdt_inner(1) = cdthex_inner
alfdt_inner(2) = cathex_inner
alfdt_inner(3) = cdthex_inner
```

c

```
aa = 1.e0+v*(rco-rci)/(rco+rci)
bb = v*((rco-rci)/(rco+rci)-1.e0)
dd = -v
ee = 1.e0
if (nrelax.eq.1) go to 110
depp(1) = 0.e+0
depp(2) = 0.e+0
depp(3) = 0.e+0
nudepp(1) = 0.e+0
nudepp(2) = 0.e+0
nudepp(3) = 0.e+0
dep = 0.e0
```

c

```
depp_inner(1) = 0.e+0
depp_inner(2) = 0.e+0
depp_inner(3) = 0.e+0
nudepp_inner(1) = 0.e+0
nudepp_inner(2) = 0.e+0
nudepp_inner(3) = 0.e+0
dep_inner = 0.e0
do 25 ii=1,8
  do 25 ij=1,8
    A(ii,ij)=0
```

25 continue

```
A(1,1)=rci_inner-rco_inner
A(1,3)=rco-rci
A(2,5)=0.5*(rco+rci)
```

```

A(2,6)=-0.5*(rco-rci)
A(2,7)=-0.5*(rco_inner+rci_inner)
A(2,8)=-0.5*(rci_inner-rco_inner)
A(3,3)=-1.0/e
A(3,4)=v/e
A(3,5)=1.0
A(4,3)=v/e
A(4,4)=-1.0/e
A(5,3)=v/e
A(5,4)=v/e
A(5,6)=1.0
A(6,1)=-1.0/e
A(6,2)=v/e
A(6,7)=1.0
A(7,1)=v/e
A(7,2)=-1.0/e
A(8,1)=v/e
A(8,2)=v/e
A(8,8)=1.0

```

c3

```

B(1)=(rco_inner-rco)*CoolantPress
B(2)=FuelSurfDispl-FuelSurfDispl_inner
& -Index*(delta+delta_inner)
B(3)=cepp(1)+oldepp(1)+alfdt(1)
B(4)=-ceps(2)+(cepp(2)+oldepp(2)+alfdt(2))
B(5)=cepp(3)+oldepp(3)+alfdt(3)
B(6)=cepp_inner(1)+oldepp_inner(1)+alfdt_inner(1)
B(7)=-ceps_inner(2)+(cepp_inner(2)+oldepp_inner(2)+alfdt_inner(2))
B(8)=cepp_inner(3)+oldepp_inner(3)+alfdt_inner(3)

```

```

N=8
NRHS=1
LDA=8
LDB=8

```

```
CALL DGESV(N,NRHS,A,LDA,IPIV,B,LDB,INFO)
```

```

csig_inner(1) = B(1)
csig_inner(2) = B(2)
csig(1) = B(3)
csig(2) = B(4)

```

c

```

if (nplast.eq.0) go to 170
sigef1 = sqrt(csig(1)**2+csig(2)**2-csig(1)*csig(2))
    sigef1_inner = sqrt(csig_inner(1)**2
& +csig_inner(2)**2-csig_inner(1)*csig_inner(2))

cep1 = cep
cep1_inner = cep_inner !yuan yi!
testr=550.0
excesh2 = min(ExcessH2Concen,testr)
    excesh2_inner = min(ExcessH2Concen_inner,testr)
c
    call strain (sigef1,cc,cep1,CladAveTemp,rstran,anrin
& ,deloxy,cwkf,cwnf,fnck,fncn,excesh2)
c
    call strain (sigef1_inner,cc,cep1_inner,CladAveTemp_inner
& ,rstran,anrin,deloxy,cwkf,cwnf,fnck,fncn,excesh2_inner)
    dep = cep1-cep
    dep1=cep1_inner-cep_inner
    deptot=dep+dep1
    if (deptot-1.e-10) 170,170,100
c ***start iteration solution here
100 continue
    cep1 = cep
    cep1_inner = cep_inner
    testr=550.0
    excesh2 = min(ExcessH2Concen,testr)
    excesh2_inner = min(ExcessH2Concen_inner,testr)
c
    call stress (olsigf,zero,cep1,CladAveTemp,rstran,anrin
& ,deloxy,cwkf,cwnf,fnck,fncn,excesh2)
c
    call stress (olsigf_inner,zero,cep1_inner,CladAveTemp_inner
& ,rstran,anrin,deloxy,cwkf,cwnf,fnck,fncn,excesh2_inner)
    cep1 = cep
    cep1_inner = cep_inner
c
    call stress (dsigf,small,cep1,CladAveTemp,rstran,anrin
& ,deloxy,cwkf,cwnf,fnck,fncn,excesh2)
    call stress (dsigf_inner,small,cep1_inner,CladAveTemp_inner
& ,rstran,anrin,deloxy,cwkf,cwnf,fnck,fncn,excesh2_inner)
c
    slope = (dsigf-olsigf)/small
    slope_inner = (dsigf_inner-olsigf_inner)/small
    coef = 2.0*(1.0+v)/(3.0*e)
110 kk = 1
    oldepp(1) = 1.e-7

```

```

    oldepp(2) = 2.e-7
    oldepp(3) = -3.e-7
c yuan yi
    oldepp_inner(1) = 1.e-7
    oldepp_inner(2) = 2.e-7
    oldepp_inner(3) = -3.e-7
c
120 continue
    do 125 ii=1,8
        do 125 ij=1,8
            A(ii,ij)=0
125 continue

    A(1,1)=abs(rci_inner-rco_inner)/rci_inner
    A(1,3)=(rco-rci)/rci
    A(2,5)=0.5*(rco+rci)
    A(2,6)=-0.5*(rco-rci)
    A(2,7)=-0.5*(rco_inner+rci_inner)
    A(2,8)=-0.5*(rci_inner-rco_inner)
    A(3,3)=-1.0/e
    A(3,4)=v/e
    A(3,5)=1.0
    A(4,3)=v/e
    A(4,4)=-1.0/e
    A(5,3)=v/e
    A(5,4)=v/e
    A(5,6)=1.0
    A(6,1)=-1.0/e
    A(6,2)=v/e
    A(6,7)=1.0
    A(7,1)=v/e
    A(7,2)=-1.0/e
    A(8,1)=v/e
    A(8,2)=v/e
    A(8,8)=1.0

```

c3

```

    B(1)=(rco_inner/rci_inner-rco/rci)*CoolantPress
    B(2)=FuelSurfDispl-FuelSurfDispl_inner
& -Index*(delta+delta_inner)

    B(3)=cepp(1)+depp(1)+alfdt(1)
    B(4)=-ceps(2)+(cepp(2)+depp(2)+alfdt(2))
    B(5)=cepp(3)+depp(3)+alfdt(3)

```

```

B(6)=cepp_inner(1)+depp_inner(1)+alfdt_inner(1)
B(7)=-ceps_inner(2)+(cepp_inner(2)+depp_inner(2)+alfdt_inner(2))
B(8)=cepp_inner(3)+depp_inner(3)+alfdt_inner(3)

```

c

```

N=8
NRHS=1
LDA=8
LDB=8

```

```
CALL DGESV(N,NRHS,A,LDA,IPIV,B,LDB,INFO)
```

```

csig_inner(1) = B(1)
csig_inner(2) = B(2)
\ csig(1) = B(3)
csig(2) = B(4)

```

c

```

if (nrelax.eq.1) go to 130
emodh = (csig(1)-v*csig(2))/e+oldepp(1)
emodx = (csig(2)-v*csig(1))/e+oldepp(2)
emodr = -v*(csig(1)+csig(2))/e-oldepp(1)-oldepp(2)
eet = 0.471405*sqrt((emodh - emodr)**2 + (emodr - emodx)**2
&    + (emodx - emodh)**2)
dep = (eet-coef*olsigf)/(1.0+coef*slope)
em = (emodh+emodr+emodx)/3.0
nudepp(1) = (dep/eet)*(emodh-em)
nudepp(2) = (dep/eet)*(emodx-em)
nudepp(3) = -nudepp(1)-nudepp(2)

```

c

```

emodh_inner = (csig_inner(1)-v*csig_inner(2))/e+oldepp_inner(1)
emodx_inner = (csig_inner(2)-v*csig_inner(1))/e+oldepp_inner(2)
emodr_inner = -v*(csig_inner(1)+csig_inner(2))/e
& -oldepp_inner(1)-oldepp_inner(2)
eet_inner = 0.471405*sqrt((emodh_inner - emodr_inner)**2
&+(emodr_inner - emodx_inner)**2+(emodx_inner - emodh_inner)**2)

```

```

dep_inner = (eet_inner-coef*olsigf_inner)/(1.0+coef*slope_inner)
em_inner = (emodh_inner+emodr_inner+emodx_inner)/3.0
nudepp_inner(1) = (dep_inner/eet_inner)*(emodh_inner-em_inner)
nudepp_inner(2) = (dep_inner/eet_inner)*(emodx_inner-em_inner)
nudepp_inner(3) = -nudepp_inner(1)-nudepp_inner(2)

```

```
go to 140
```

c ***calculate creep strains here

```

130 continue
    sm = (csig(1)+csig(2))/3.0
    s1 = csig(1)-sm
    s2 = csig(2)-sm
    dep = 0.47140452*sqrt((oldepp(1) - oldepp(2))**2
&    + (oldepp(2) - oldepp(3))**2 + (oldepp(3) - oldepp(1))**2)
    depdot = dep/dtime
    crap = crep+dep
    call creep (sigeff,depdot,time,CladAveTemp,flux,crap,iquit)
    nudepp(1) = 1.5*dep*s1/sigeff
    nudepp(2) = 1.5*dep*s2/sigeff
    nudepp(3) = -nudepp(1)-nudepp(2)
c
    sm_inner = (csig_inner(1)+csig_inner(2))/3.0
    s1_inner = csig_inner(1)-sm_inner
    s2_inner = csig_inner(2)-sm_inner
    dep_inner = 0.47140452*sqrt((oldepp_inner(1)-oldepp_inner(2))**2
& + (oldepp_inner(2) - oldepp_inner(3))**2
& + (oldepp_inner(3) - oldepp_inner(1))**2)
    depdot_inner = dep_inner/dtime
    crap_inner = crep_inner+dep_inner
    call creep (sigeff_inner,depdot_inner,time,CladAveTemp_inner
& ,flux,crap_inner,iquit)
    nudepp_inner(1) = 1.5*dep_inner*s1_inner/sigeff_inner
    nudepp_inner(2) = 1.5*dep_inner*s2_inner/sigeff_inner
    nudepp_inner(3) = -nudepp_inner(1)-nudepp_inner(2)
140 continue
    ratio1 = (nudepp(1)-oldepp(1))/oldepp(1)
    ratio2 = (nudepp(2)-oldepp(2))/oldepp(2)
    ratio3 = (nudepp(3)-oldepp(3))/oldepp(3)
    ratio1 = abs(ratio1)
    ratio2 = abs(ratio2)
    ratio3 = abs(ratio3)
c
    ratio4 = (nudepp_inner(1)-oldepp_inner(1))/oldepp_inner(1)
    ratio5 = (nudepp_inner(2)-oldepp_inner(2))/oldepp_inner(2)
    ratio6 = (nudepp_inner(3)-oldepp_inner(3))/oldepp_inner(3)
    ratio4 = abs(ratio4)
    ratio5 = abs(ratio5)
    ratio6 = abs(ratio6)
c
    ratio = max(ratio1,ratio2,ratio3,ratio4,ratio5,ratio6)
    if (ratio-0.001) 170,170,150
150 kk = kk+1
    if (kk-199) 160,180,180
160 oldepp(1) = nudepp(1)

```

```

    oldepp(2) = nudepp(2)
    oldepp(3) = nudepp(3)
c
    oldepp_inner(1) = nudepp_inner(1)
    oldepp_inner(2) = nudepp_inner(2)
    oldepp_inner(3) = nudepp_inner(3)
    go to 120
170 continue
    if (nrelax.ne.1) cep = cep+dep
    if (nrelax.eq.1) crep = crep+dep
    cepp(1) = cepp(1)+nudepp(1)
    cepp(2) = cepp(2)+nudepp(2)
    cepp(3) = cepp(3)+nudepp(3)
c
    if (nrelax.ne.1) cep_inner = cep_inner+dep_inner
    if (nrelax.eq.1) crep_inner = crep_inner+dep_inner
    cepp_inner(1) = cepp_inner(1)+nudepp_inner(1)
    cepp_inner(2) = cepp_inner(2)+nudepp_inner(2)
    cepp_inner(3) = cepp_inner(3)+nudepp_inner(3)
    go to 190

180 write (6,220) kk

c ***even tho routine did not converge
c ***last computed values are returned, and
c ***an error message is printed
    go to 170
c ***compute interface pressure

190 RinterfacPress = ((rco-rci)*csig(1)+
    +rco*CoolantPress)/rci
    RinterfacPress_inner = ((rco_inner-rci_inner)*csig_inner(1)+
    +rco_inner*CoolantPress)/rci_inner
    RinterfacPress =abs(RinterfacPress)
    RinterfacPress_inner =abs(RinterfacPress_inner)
    if (RinterfacPress.ge.GapPress) go to 200

c ***for situation where RinterfacPress l.t. GapPress
c RinterfacPress is set equal to GapPress and elastic soln is obtained
c such that RinterfacPress always equals GapPress

    csig(1) = (rci*GapPress-rco*CoolantPress)/(rco-rci)
    rbar = 0.5*(rco+rci)
    t = rco-rci

```

```

csig(2) = ((rbar+v*t/2.0)*csig(1)+rbar*e*(cepp(1)+alfdt(1))-0.5*t*
&e*(cepp(3)+alfdt(3))-e*(FuelSurfDispl-delta))/(rci*v)
ceps(1) = (csig(1)-v*csig(2))/e+cepp(1)+alfdt(1)
ceps(2) = (csig(2)-v*csig(1))/e+cepp(2)+alfdt(2)
ceps(3) = -v*(csig(1)+csig(2))/e+cepp(3)+alfdt(3)
RinterfacPress = GapPress
go to 210
200 continue

c ***compute radial strain
ceps(3) = -v*(csig(1)+csig(2))/e+cepp(3)+alfdt(3)
ceps(1)=(FuelSurfDispl-delta+0.5*(rco-rci)*ceps(3))
+      *2.0/(rco+rci)

210 continue

220 format ('did not converge in',1x,i4,1x,' iterations in couple')

return
end

```

APPENDIX C: Input Decks for Annular Fuel Performance Modeling

150% power sintered annular case

```
*****
*   frapcon3, steady-state fuel rod analysis code
*   -----
*
*   CASE DESCRIPTION: GE Test Case Rod A1
*
*UNIT  FILE DESCRIPTION
*---  -----Output:
*   Output :
* 6    STANDARD PRINTER OUTPUT
*
*   Scratch:
* 5    SCRATCH INPUT FILE FROM ECH01
*
* Input: FRAPCON2 INPUT FILE (UNIT 55)
*
*****
* GOESINS:
FILE05='nullfile', STATUS='UNKNOWN', FORM='FORMATTED',
  CARRIAGE CONTROL='NONE'
*
* GOESOUTS:
FILE06='sinterAnnular150.out', STATUS='UNKNOWN', CARRIAGE CONTROL='LIST'
/*****
```

13X13 sintered I&E cooled annular case (150% power)

```
$frpcn
im=50, na=5,nr=17,
mechan = 2, ngasr =50,
$end
$frpcn
cpl = 0.25, crdt = 0.0, thkcl = 0.5715e-3, thkgap = 0.62e-4,
thkcl_inner=0.5715e-3,thkgap_inner=0.62e-4
dco = 15.367e-3, pitch = 16.51e-3,rc=4.95e-3,
dco_inner=8.633e-3, den = 95.,fa = 1.3,
dspgw = 0.9e-5, enrch = 5, fg pav = 1.4e6, hdish = 0.0,
```

```

hplt = 13.4e-3, icm = 4,dspg=13.0e-3
icor = 0, idxgas = 1, iplant = -2, iq = 1, jdlpr = 1,
totl = 3.66, roughc = 3.e-6, roughf = 3.e-6, vs = 8,
nunits = 0,
p2(1) = 15.5e6, tw(1) = 567.9, go(1) = 5400.0,
flux=11*0.25e17,

jst =49*1
time=0.04, 1,30,80,85,90,100,120,150,170,200,
    240,280,300,340,380,400,420,460,480,
    500,540,580,600,
    640,680,720,760,780,800,
    840,880,920,960,980,
    1000,1020,1060,1080,1120,1130,1170,1200
    1240,1280,1320,1360,1400,1440,1480
qmpy =11*101.4,9*67.6,15*48.3,15*37.1
$end

```

100% power sintered annular case

```
*****
*   frapcon3, steady-state fuel rod analysis code
*   -----
*
*   CASE DESCRIPTION: GE Test Case Rod A1
*
*UNIT  FILE DESCRIPTION
*-----Output:
*   Output :
* 6   STANDARD PRINTER OUTPUT
*
*   Scratch:
* 5   SCRATCH INPUT FILE FROM ECH01
*
* Input: FRAPCON2 INPUT FILE (UNIT 55)
*
*****
* GOESINS:
FILE05='nullfile', STATUS='UNKNOWN', FORM='FORMATTED',
  CARRIAGE CONTROL='NONE'
*
* GOESOUTS:
FILE06='sinterAnnular100.out', STATUS='UNKNOWN', CARRIAGE CONTROL='LIST'
/*****
```

13X13 sintered I&E cooled annular case (100% power)

```
$frpcn
im=50, na=5,nr=17,
mechan = 2, ngasr =50,
$end
$frpcon
cpl = 0.25, crdt = 0.0, thkcld = 0.5715e-3, thkgap = 0.62e-4,
thkcld_inner=0.5715e-3,thkgap_inner=0.62e-4
dco = 15.367e-3, pitch = 16.51e-3,rc=4.95e-3,
dco_inner=8.633e-3, den = 95.,fa = 1.3,
dspgw = 0.9e-5, enrch = 5, fgpav = 1.4e6, hdish = 0.0,
hplt = 13.4e-3, icm = 4,dspg=13.0e-3
icor = 0, idxgas = 1, iplant =-2, iq = 1, jdlpr = 1,
totl = 3.66, roughc = 3.e-6, roughf = 3.e-6, vs = 8,
nunits = 0,
p2(1) = 15.5e6, tw(1) = 567.9, go(1) = 5400.0,
flux=11*0.25e17,
```

```
jst =49*1
time=0.04, 1,30,80,85,90,100,120,150,170,200,
    240,280,300,340,380,400,420,460,480,
    500,540,580,600,
    640,680,720,760,780,800,
    840,880,920,960,980,
    1000,1020,1060,1080,1120,1130,1170,1200
    1240,1280,1320,1360,1400,1440,1480
```

```
qmpy =11*69,9*45.0,15*32.0,15*24.8
```

```
$end
```

* The input parameters for the sintered annular fuel follow the same instruction as in FRAPCON-3 manual [Lanning et al, 1997], except that parameters with _inner indicated the inner dimensions.

150% power VIPAC annular case

```
*****
*   frapcon3, steady-state fuel rod analysis code
*   -----
*
*   CASE DESCRIPTION: GE Test Case Rod A1
*
*UNIT  FILE DESCRIPTION
*-----Output:
*   Output :
*   6   STANDARD PRINTER OUTPUT
*
*   Scratch:
*   5   SCRATCH INPUT FILE FROM ECH01
*
* Input: FRAPCON2 INPUT FILE (UNIT 55)
*
*****
* GOESINS:
FILE05='nullfile', STATUS='UNKNOWN', FORM='FORMATTED',
  CARRIAGE CONTROL='NONE'
*
* GOESOUTS:
FILE06='VIPACAnnular150.out', STATUS='UNKNOWN', CARRIAGE CONTROL='LIST'
/*****
```

13X13 VIPAC I&E cooled annular case. (150% power)

```
$frpcn
im=50, na=5,nr=17,
mechan = 2, ngasr =50,
$end
$frpcon
cpl = 0.25, crdt = 0.0, thkcld = 0.5715e-3,
thkcld_inner=0.5715e-3,
dco = 15.367e-3, pitch = 16.51e-3,
dco_inner=8.633e-3, den = 83.,fa = 1.3,partsize=400.e-6
dspgw = 0.9e-5, enrch = 5, fgpav = 1.4e6, hdish = 0.0,
hplt = 13.4e-3, icm = 4,dspg=13.0e-3
icor = 0, idxgas = 1, iplant =-2, iq = 1, jdlpr = 0,
totl = 3.66, roughc = 3.e-6, roughf = 3.e-6, vs = 8,
nunits = 0,
p2(1) = 15.5e6, tw(1) = 567.9, go(1) = 5400.0,
flux=11*0.25e17,
```

```

jst =49*1
time=0.04, 1,30,80,85,90,100,120,150,170,200,
    240,280,300,340,380,400,420,460,480,
    500,540,580,600,
    640,680,720,760,780,800,
    840,880,920,960,980,
    1000,1020,1060,1080,1120,1130,1170,1200
    1240,1280,1320,1360,1400,1440,1480
qmpy =11*101.4,9*67.6,15*48.3,15*37.1
$end

```

- The input parameters for the sintered annular fuel follow the same instruction as in FRAPCON-3 manual [Lanning et al, 1997], except that parameters with _inner indicated the inner dimensions.
- Part_size is the particle size (diameter) in m.

FRAPTRAN input for the solid case

```
* solid.in
*
*****
* FRAPTRAN, Transient fuel rod analysis code
*
*
* CASE DESCRIPTION: PWR: RIA
*      assumes a 10msec RIA for a PWR rod from hot standby
*      SI input and output
*      Manual input for burnup-dependent variables
*      Manual input of FGR and fuel swelling
*
FILE05='nullfile', STATUS='scratch', FORM='FORMATTED',
      CARRIAGE CONTROL='LIST'
FILE15='sth2xt', STATUS='old', FORM='UNFORMATTED'
*
FILE06='solid.out', STATUS='UNKNOWN', CARRIAGE CONTROL='LIST' *
FILE66='stripf.solid', STATUS='UNKNOWN', FORM='FORMATTED', *
      CARRIAGE CONTROL='LIST'
/*****
PWR Solid fuel RIA (FRAPTRAN 1.2, July 2003)
$begin
  ProblemStartTime=0.0,
  ProblemEndTime=1.0,
$end
start
$Iodata
  unitin=1, unitout=1, dtpoa(1)=0.01,0.0, 0.01,1.0,
  dtplt=0.005, inp=0,
$end
$solution
  dtmaxa(1)=0.0002,0., 0.00004,0.1, 0.0002,0.5, 0.002,1.2,
  dtss=1.0, prsacc=0.001, tmpac1=0.001, maxit=100, noiter=100,
  epsht1=1.0,
  naxn=5, nfmesh=15, ncmesh=2,
$end
$design
  RodLength=3.66, RodDiameter=0.0095, FuelPelDiam=0.00819, gapthk=0.000089,
  rshd=0.00, dishd=0.00, pelh=0.0134, dishv0= 0,
  roughf=3.0, frden=0.94, fotmtl=2.0, tsntrk=1773.0, fgrns=10.0, gadoln=0.0,
  ncs=25, coldw=0.5, roughc=1.14, cldwdc=0.04,
  spl=0.213, scd=0.00884, swd=0.00152, vplen=0.0000080,
  cfluxa=7.5e17, tflux=1.6e8, bup=0.69e7,
  gfrac=0.46,0.,0.08,0.46,3*0., gappr0=6.1e6, gsms=0.14e-1,
```

\$end

\$power

RodAvePower =

0.0,0.0000,
0.0,0.1500,
28.,0.2378,
328.,0.2613,
1722.,0.2653,
7546.,0.2693,
19439.,0.2733,
23097.,0.2753,
23474.,0.2760,
22195.,0.2773,
12369.,0.2813,
4921.,0.2853,
1788.,0.2893,
689.,0.2933,
312.,0.2973,
197.,0.3013,
148.,0.3053,
131.,0.3113,
115.,0.3273,
98.,0.4700,
33.,2.0000,

AxPowProfile =

0.3827, 0, 0.6088, 0.4067, 0.7934, 0.8133, 0.9239, 1.22, 0.9914, 1.6267, 0.9914,
2.033,
0.9239, 2.44, 0.7934, 2.8467, 0.6088, 3.2533, 0.3827, 3.66,

RadPowProfile =

0.8705, 0.0000000 ,
0.8739, 0.0007320 ,
0.8799, 0.0013725 ,
0.8890, 0.0019276 ,
0.9018, 0.0024034 ,
0.9198, 0.0028061 ,
0.9451, 0.0031418 ,
0.9806, 0.0034166 ,
1.0307, 0.0036365 ,
1.1007, 0.0038078 ,
1.1975, 0.0039365 ,
1.3287, 0.0040287 ,
1.5018, 0.0040905 ,
1.7208, 0.0041280 ,
1.9816, 0.0041472 ,
2.2616, 0.0041543 ,

2.4797, 0.0041553 ,
0.8165, 0.0000000 ,
0.8210, 0.0007387 ,
0.8289, 0.0013851 ,
0.8408, 0.0019452 ,
0.8581, 0.0024253 ,
0.8828, 0.0028314 ,
0.9184, 0.0031699 ,
0.9692, 0.0034469 ,
1.0417, 0.0036686 ,
1.1439, 0.0038412 ,
1.2859, 0.0039709 ,
1.4788, 0.0040638 ,
1.7320, 0.0041260 ,
2.0493, 0.0041638 ,
2.4216, 0.0041832 ,
2.8143, 0.0041903 ,
3.1156, 0.0041913 ,
0.7991, 0.0000000 ,
0.8041, 0.0007410 ,
0.8127, 0.0013894 ,
0.8258, 0.0019511 ,
0.8447, 0.0024325 ,
0.8720, 0.0028398 ,
0.9111, 0.0031792 ,
0.9671, 0.0034569 ,
1.0469, 0.0036791 ,
1.1593, 0.0038522 ,
1.3148, 0.0039822 ,
1.5247, 0.0040753 ,
1.7977, 0.0041377 ,
2.1364, 0.0041755 ,
2.5303, 0.0041950 ,
2.9435, 0.0042021 ,
3.2596, 0.0042031 ,
0.8165, 0.0000000 ,
0.8210, 0.0007384 ,
0.8289, 0.0013845 ,
0.8408, 0.0019444 ,
0.8581, 0.0024242 ,
0.8828, 0.0028301 ,
0.9184, 0.0031684 ,
0.9692, 0.0034453 ,
1.0417, 0.0036669 ,
1.1439, 0.0038394 ,
1.2859, 0.0039690 ,

```

1.4788, 0.0040619 ,
1.7320, 0.0041241 ,
2.0493, 0.0041619 ,
2.4216, 0.0041813 ,
2.8143, 0.0041884 ,
3.1156, 0.0041894 ,
0.8705, 0.0000000 ,
0.8739, 0.0007320 ,
0.8799, 0.0013726 ,
0.8890, 0.0019278 ,
0.9018, 0.0024037 ,
0.9198, 0.0028065 ,
0.9451, 0.0031422 ,
0.9806, 0.0034170 ,
1.0307, 0.0036370 ,
1.1007, 0.0038083 ,
1.1975, 0.0039370 ,
1.3287, 0.0040292 ,
1.5018, 0.0040910 ,
1.7208, 0.0041285 ,
1.9816, 0.0041477 ,
2.2616, 0.0041548 ,
2.4797, 0.0041558 ,
$end
$model
  internal='on',
  metal='on', cathca=1,
  odoxid=12,
  oxideod=5.25e-6, 11.1e-6, 14.9e-6, 19.4e-6, 21.6e-6, 26.9e-6, 32.4e-6, 39.0e-6,
    47.8e-6, 55.9e-6, 63.6e-6, 45.5e-6,
  cexh2a=45., 84., 110., 133., 154., 190., 226., 270., 329., 383., 434., 314.,
  deformation='on', noball=0,
  itransient=1, nthermex=0,
$end
$boundary
  heat='on',
  press=2, pbh2=15.5e6,0., 15.5e6,6.,
  zone=1, htclev=3.536,
  htco=2, htca=1.136e5,0., 1.136e5,6.,
  tem=2, tblka=550.,0., 550.,6.,
$end
$tuning
$end

```

FRAPTRAN input for sintered annular case

```
* Annular.in
*
*****
* FRAPTRAN, Transient fuel rod analysis code
*
*
* CASE DESCRIPTION: Annular fuel: RIA
*                   assumes a 10msec RIA for a PWR rod from hot standby
*                   SI input and output
*                   Manual input for burnup-dependent variables
*                   Manual input of FGR and fuel swelling
*
FILE05='nullfile', STATUS='scratch', FORM='FORMATTED',          *
      CARRIAGE CONTROL='LIST'                                   *
FILE15='sth2xt', STATUS='old', FORM='UNFORMATTED'              *
*
FILE06='ANNULAR.out', STATUS='UNKNOWN', CARRIAGE CONTROL='LIST' *
FILE66='stripf.ANNULAR', STATUS='UNKNOWN', FORM='FORMATTED',  *
      CARRIAGE CONTROL='LIST'
/*****
Annular Fuel: RIA (FRAPTRAN 1.2, July 2004)
$begin
  ProblemStartTime=0.0,
  ProblemEndTime=1.0,
$end
start
$Iodata
  unitin=1, unitout=1, dtpoa(1)=0.01,0.0, 0.01,1.0,
  dtplt=0.005, inp=0,
$end
$solution
  dtmaxa(1)=0.0002,0., 0.00004,0.1, 0.0002,0.5, 0.002,1.2,
  dtss=1.0, prsacc=0.001, tmpac1=0.001, maxit=100, noiter=100,
  epsht1=1.0,
  naxn=5, nfmesh=15, ncmesh=2,
$end
$design
  RodLength=3.66, RodDiameter=0.01537, FuelPelDiam=0.0141, gapthk=0.000089,
  rshd=0.00, dishd=0.00, pelh=0.0134, dishv0= 0,
  roughf=3.0, frden=0.94, fotmtl=2.0, tsntrk=1773.0, fgrrns=10.0, gadoln=0.0,
  ncs=25, coldw=0.5, roughc=1.14, cldwdc=0.04,
  spl=0.213, scd=0.00884, swd=0.00152, vplen=0.000021,
  cfluxa=7.5e17, tflux=1.6e8, bup=0.69e7,
  gfrac=0.46,0.,0.08,0.46,3*0., gappr0=7.5e6, gsms=0.59e-1,
```

\$end

\$power

RodAvePower =

0.0,0.0000,
0.0,0.1500,
28.,0.2378,
328.,0.2613,
1722.,0.2653,
7546.,0.2693,
19439.,0.2733,
23097.,0.2753,
23474.,0.2760,
22195.,0.2773,
12369.,0.2813,
4921.,0.2853,
1788.,0.2893,
689.,0.2933,
312.,0.2973,
197.,0.3013,
148.,0.3053,
131.,0.3113,
115.,0.3273,
98.,0.4700,
33.,2.0000,

AxPowProfile =

0.3827, 0, 0.6088, 0.4067, 0.7934, 0.8133, 0.9239, 1.22, 0.9914, 1.6267, 0.9914,
2.033,
0.9239, 2.44, 0.7934, 2.8467, 0.6088, 3.2533, 0.3827, 3.66,

RadPowProfile =

0, 0,
0, 0.004965,
1.7212, 0.0049658 ,
1.4048, 0.0049926 ,
1.2946, 0.0050194 ,
1.2218, 0.0050461 ,
1.1682, 0.0050729 ,
0.9722, 0.0053138 ,
0.9264, 0.0055548 ,
0.9158, 0.0057958 ,
0.9134, 0.0060368 ,
0.9127, 0.0062777 ,
0.9198, 0.0065187 ,
0.9617, 0.0067597 ,
1.1607, 0.0070006 ,
1.2184, 0.0070273 ,
1.2990, 0.0070541 ,

1.4271, 0.0070809 ,
 1.8726, 0.0071076 ,
 0, 0,
 0, 0.004965,
 1.9872, 0.0049677 ,
 1.5709, 0.0049948 ,
 1.4206, 0.0050218 ,
 1.3196, 0.0050489 ,
 1.2443, 0.0050760 ,
 0.9619, 0.0053195 ,
 0.8946, 0.0055632 ,
 0.8791, 0.0058069 ,
 0.8758, 0.0060506 ,
 0.8748, 0.0062943 ,
 0.8856, 0.0065379 ,
 0.9475, 0.0067816 ,
 1.2345, 0.0070251 ,
 1.3156, 0.0070522 ,
 1.4274, 0.0070792 ,
 1.6014, 0.0071063 ,
 2.1807, 0.0071333 ,
 0, 0,
 0, 0.004965,
 2.0016, 0.0049690 ,
 1.5817, 0.0049961 ,
 1.4297, 0.0050233 ,
 1.3274, 0.0050504 ,
 1.2509, 0.0050776 ,
 0.9619, 0.0053219 ,
 0.8922, 0.0055663 ,
 0.8761, 0.0058108 ,
 0.8727, 0.0060553 ,
 0.8716, 0.0062998 ,
 0.8828, 0.0065442 ,
 0.9470, 0.0067886 ,
 1.2410, 0.0070329 ,
 1.3233, 0.0070600 ,
 1.4366, 0.0070872 ,
 1.6125, 0.0071143 ,
 2.1965, 0.0071414 ,
 0, 0,
 0, 0.004965,
 1.9872, 0.0049688 ,
 1.5709, 0.0049959 ,
 1.4206, 0.0050229 ,
 1.3196, 0.0050500 ,

```

1.2443, 0.0050770 ,
0.9619, 0.0053204 ,
0.8946, 0.0055639 ,
0.8791, 0.0058074 ,
0.8758, 0.0060509 ,
0.8748, 0.0062944 ,
0.8856, 0.0065378 ,
0.9475, 0.0067813 ,
1.2345, 0.0070246 ,
1.3156, 0.0070517 ,
1.4274, 0.0070787 ,
1.6014, 0.0071057 ,
2.1807, 0.0071327 ,
    0, 0,
    0, 0.004965,
1.7212, 0.0049672 ,
1.4048, 0.0049939 ,
1.2946, 0.0050207 ,
1.2218, 0.0050475 ,
1.1682, 0.0050742 ,
0.9722, 0.0053151 ,
0.9264, 0.0055561 ,
0.9158, 0.0057970 ,
0.9134, 0.0060380 ,
0.9127, 0.0062789 ,
0.9198, 0.0065198 ,
0.9617, 0.0067607 ,
1.1607, 0.0070016 ,
1.2184, 0.0070284 ,
1.2990, 0.0070551 ,
1.4271, 0.0070819 ,
1.8726, 0.0071087 ,
$end
$model
    internal='on',
    metal='on', cathca=1,
    odoxid=12,
    oxideod=5.25e-6, 11.1e-6, 14.9e-6, 19.4e-6, 21.6e-6, 26.9e-6, 32.4e-6, 39.0e-6,
        47.8e-6, 55.9e-6, 63.6e-6, 45.5e-6,
    cexh2a=45., 84., 110., 133., 154., 190., 226., 270., 329., 383., 434., 314.,
    heat = 'on',
    cenvoi=1, zvoid1=0.0, zvoid2=3.66, rvoid=0.00495,
    deformation='on', noball=0,
    itransient=1, nthermex=0,
$end
$boundary

```

```
heat='on',  
press=2, pbh2=15.5e6,0., 15.5e6,6.,  
zone=1, htclev=3.536,  
htco=2, htca=1.136e5,0., 1.136e5,6.,  
tem=2, tblka=550.,0., 550.,6.,  
$end  
$tuning  
$end
```

FRAPTRAN input for VIPAC annular case

```
* Vipac.in
*
*****
* FRAPTRAN, Transient fuel rod analysis code
*
*
* CASE DESCRIPTION: Annular fuel: RIA
*     assumes a 10msec RIA for a PWR rod from hot standby
*     SI input and output
*     Manual input for burnup-dependent variables
*     Manual input of FGR and fuel swelling
*
FILE05='nullfile', STATUS='scratch', FORM='FORMATTED',
CARRIAGE CONTROL='LIST'
FILE15='sth2xt', STATUS='old', FORM='UNFORMATTED'
FILE06='Vipac.out', STATUS='UNKNOWN', CARRIAGE CONTROL='LIST'
FILE66='stripf.vipac', STATUS='UNKNOWN', FORM='FORMATTED',
CARRIAGE CONTROL='LIST'
/*****
Annular Fuel: RIA (FRAPTRAN 1.2, July 2004)
$begin
  ProblemStartTime=0.0,
  ProblemEndTime=1.0,
$end
start
$Iodata
  unitin=1, unitout=1, dtpoa(1)=0.01,0.0, 0.01,1.0,
  dtplt=0.005, inp=0,
$end
$solution
  dtmaxa(1)=0.0002,0., 0.00004,0.1, 0.0002,0.5, 0.002,1.2,
  dtss=1.0, prsacc=0.001, tmpac1=0.001, maxit=100, noiter=100,
  epsht1=1.0,
  naxn=3, nfmesh=15, ncmesh=2,
$end
$design
  RodLength=3.66, RodDiameter=0.01537, FuelPelDiam=0.0142, gapthk=0.000001,
  rshd=0.00, dishd=0.00, pelh=0.0134, dishv0= 0,
  roughf=3.0, frden=0.85, fotmtl=2.0, tsntrk=1773.0, fgrrns=10.0, gadoln=0.0,
  ncs=25, coldw=0.5, roughc=1.14, clwdwc=0.04,
  spl=0.213, scd=0.00884, swd=0.00152, vplen=0.000062,
  cfluxa=7.5e17, tflux=1.6e8, bup=0.69e7,
  gfrac=0.46,0.,0.08,0.46,3*0., gappr0=5.8e6, gsms=1.3e-1,
```

```

$end
$power
RodAvePower =
  0.0,0.0000,
  0.0,0.1500,
  28.,0.2378,
  328.,0.2613,
  1722.,0.2653,
  7546.,0.2693,
  19439.,0.2733,
  23097.,0.2753,
  23474.,0.2760,
  22195.,0.2773,
  12369.,0.2813,
  4921.,0.2853,
  1788.,0.2893,
  689.,0.2933,
  312.,0.2973,
  197.,0.3013,
  148.,0.3053,
  131.,0.3113,
  115.,0.3273,
  98.,0.4700,
  33.,2.0000,
AxPowProfile =
  0.3827, 0, 0.6088, 0.4067, 0.7934, 0.8133, 0.9239, 1.22, 0.9914, 1.6267, 0.9914,
2.033,
  0.9239, 2.44, 0.7934, 2.8467, 0.6088, 3.2533, 0.3827, 3.66,
RadPowProfile =
0, 0.0,
0, 0.004860,
1.9700, 0.0048675 ,
1.5494, 0.0048958 ,
1.4002, 0.0049241 ,
1.3012, 0.0049524 ,
1.2281, 0.0049807 ,
0.9611, 0.0052355 ,
0.9001, 0.0054903 ,
0.8868, 0.0057452 ,
0.8841, 0.0060000 ,
0.8830, 0.0062549 ,
0.8920, 0.0065098 ,
0.9477, 0.0067646 ,
1.2171, 0.0070193 ,
1.2952, 0.0070476 ,
1.4041, 0.0070758 ,

```

```

1.5757, 0.0071041 ,
2.1592, 0.0071324 ,
0,    0.0,
0,    0.0048580,
2.0513, 0.0048584 ,
1.6069, 0.0048869 ,
1.4470, 0.0049155 ,
1.3396, 0.0049440 ,
1.2596, 0.0049725 ,
0.9591, 0.0052293 ,
0.8881, 0.0054862 ,
0.8725, 0.0057431 ,
0.8694, 0.0060001 ,
0.8680, 0.0062570 ,
0.8786, 0.0065139 ,
0.9436, 0.0067707 ,
1.2475, 0.0070275 ,
1.3331, 0.0070560 ,
1.4512, 0.0070845 ,
1.6350, 0.0071130 ,
2.2494, 0.0071416 ,
0,    0.0,
0,    0.004860,
1.9700, 0.0048673 ,
1.5494, 0.0048956 ,
1.4002, 0.0049239 ,
1.3012, 0.0049522 ,
1.2281, 0.0049805 ,
0.9611, 0.0052353 ,
0.9001, 0.0054902 ,
0.8868, 0.0057451 ,
0.8841, 0.0060001 ,
0.8830, 0.0062550 ,
0.8920, 0.0065099 ,
0.9477, 0.0067647 ,
1.2171, 0.0070195 ,
1.2952, 0.0070478 ,
1.4041, 0.0070761 ,
1.5757, 0.0071044 ,
2.1592, 0.0071327 ,
$end
$model
  internal='on',
  metal='on', cathca=1,
  odoxid=12,
  oxideod=5.25e-6, 11.1e-6, 14.9e-6, 19.4e-6, 21.6e-6, 26.9e-6, 32.4e-6, 39.0e-6,

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        47.8e-6, 55.9e-6, 63.6e-6, 45.5e-6,
cexh2a=45., 84., 110., 133., 154., 190., 226., 270., 329., 383., 434., 314.,
heat = 'on',
cenvoi=1, zvoid1=0.0, zvoid2=3.66, rvoid=0.00486,
deformation='on', noball=0,
itransient=1, nthermex=0,
$end
$boundary
heat='on',
press=2, pbh2=15.5e6,0., 15.5e6,6.,
zone=1, htclev=3.536,
htco=2, htca=1.136e5,0., 1.136e5,6.,
tem=2, tblka=550.,0., 550.,6.,
$end
$tuning
$end

```