Assessment of Innovative Fuel Designs for High Performance Light Water Reactors

by

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ABSTRACT

To increase the power density and maximum allowable fuel burnup in light water reactors, new fuel rod designs are investigated. Such fuel is desirable for improving the economic performance light water reactors loaded with transuranic-bearing fuel for transmutation, as well as those using UO₂ fuel.

A proposal for using silicon carbide duplex as fuel cladding is investigated. The cladding consists of a monolithic inner layer surrounded by a tightly wound fiber-matrix composite. The monolith layer retains the volatile fission products while the composite adds strength. The FRAPCON steady-state thermo-mechanical fuel rod modeling code is used to examine the performance of SiC cladding at high fuel burnup and high power density.

Empirical models are developed to describe the physical properties of the composite as a function of operating temperature and neutron fluence. A comparison of the behavior of the SiC cladding to the conventional Zircaloy cladding demonstrates that the SiC has superior resistance to creep and mechanical degradation due to radiation or oxidation. However, the lower thermal conductivity of the SiC is a major issue, which results in significantly increased peak fuel temperatures. Mixed UO₂-PuO₂ fuel is also examined in place of traditional UO₂ pellets, since this may better resemble transmutation fuels of the future. It is found that the use of plutonium-bearing mixed-oxide fuels further exacerbates the high fuel temperatures.

The silicon carbide cladding is predicted to have more favorable performance when used for internally- and externally-cooled annular fuel rods developed at MIT. Both sintered annular pellets and VIPAC granular fuel are examined. Because of the fuel geometry, the average fuel temperature is significantly lower, and the stiffness of the SiC cladding helps to maintain the geometry of the annulus during extended irradiation.

Experimental projects have been undertaken to study the performance of both the annular fuel rods and silicon carbide duplex cladding. A post-irradiation examination of prototype annular fuel rods with VIPAC fuel, irradiated in the MIT reactor, has been designed and executed. Through this non-destructive examination, the disposition of the fuel grains is examined, and fuel burnup and fission gas release is estimated. These experimental results correlate well with computer calculations. A new irradiation facility was also planned and constructed that consists of a closed loop, operated at pressurized water reactor pressure, temperature, and chemistry conditions. This facility contains silicon carbide duplex cladding samples of various constructions, and it will be irradiated in the core of the MIT reactor for several months.
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<tr>
<td>AFCI</td>
<td>Advanced Fuel Cycle Initiative</td>
</tr>
<tr>
<td>CVD</td>
<td>Carbon Vapor Deposition</td>
</tr>
<tr>
<td>CVI</td>
<td>Carbon Vapor Infiltration</td>
</tr>
<tr>
<td>DNBR</td>
<td>Departure from Nucleate Boiling Ratio</td>
</tr>
<tr>
<td>DPA</td>
<td>Displacements per Atom</td>
</tr>
<tr>
<td>EFPD</td>
<td>Effective Full Power Days</td>
</tr>
<tr>
<td>I&amp;EC</td>
<td>Internally- and Externally-Cooled</td>
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<tr>
<td>LHGR</td>
<td>Linear Heat Generation Rate</td>
</tr>
<tr>
<td>LWR</td>
<td>Light Water Reactor</td>
</tr>
<tr>
<td>MCNP</td>
<td>Monte Carlo N-Particle Code</td>
</tr>
<tr>
<td>MITR</td>
<td>MIT Nuclear Reactor</td>
</tr>
<tr>
<td>MOX</td>
<td>Mixed Oxide</td>
</tr>
<tr>
<td>ORIGEN</td>
<td>Oak Ridge Isotope Generation Code</td>
</tr>
<tr>
<td>PIE</td>
<td>Post-Irradiation Examination</td>
</tr>
<tr>
<td>PIP</td>
<td>Polymer Impregnation Pyrolysis</td>
</tr>
<tr>
<td>PWR</td>
<td>Pressurized Water Reactor</td>
</tr>
<tr>
<td>SiC</td>
<td>Silicon Carbide</td>
</tr>
<tr>
<td>TD</td>
<td>Theoretical Density</td>
</tr>
<tr>
<td>TRU</td>
<td>Transuranic</td>
</tr>
<tr>
<td>VIPAC</td>
<td>Vibration Packed</td>
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1. Introduction

1.1 Background

Transuranic (TRU) elements are responsible for a significant fraction of the long-term radiation and decay heat of commercial spent reactor fuel, and some TRU isotopes, such as Pu-239, represent a proliferation risk. It has been demonstrated that it is possible to destroy these TRU isotopes efficiently in a thermal spectrum, such as that in a light water reactor (LWR), and therefore there is interest in developing transmutation fuels for use in current-generation power reactors [Shwageraus et al., 2003].

Transmutation fuels replace some or all of the UO$_2$ in the fuel rod with plutonium and other TRU from spent fuel, enabling these isotopes to be fissioned in the reactor. Since all operating commercial reactors in the United States are LWRs utilizing traditional pin-type fuel rods, the performance of this fuel is critical to the transmutation performance. These rods consist of a stack of sintered UO$_2$ pellets encased in a thin zirconium-based cladding. While performance has been continually improved since their inception, this solid pin design has remained basically unchanged. Recent research at MIT has investigated possible approaches for significantly improving the power density supported by LWR fuel rods by altering their geometry or by using different cladding materials [Yuan et al., 2004]. These changes are applicable to both traditional UO$_2$ fuel as well as the transmutation fuels under development.

The primary purpose of these changes is to allow the fuel rods to be run at a higher power density and to withstand higher burnup. A higher power density increases the rate at which power is produced (more fissions per unit time), within a given fuel volume, whereas the increased burnup allows a larger fraction of the fuel in the rod to be consumed before it is discharged. In addition, these changes will maintain or improve upon fuel rod performance and safety margins during both normal operation and accident scenarios.
The improved design could allow power uprating of existing LWR cores with minimal core modification, and increase the maximum allowable discharge burnup. This is beneficial for the traditional fuel management in once-through fuel cycles since it increases the efficiency of uranium usage and increases power output. Since less uranium fuel volume is required for the same energy output, the volume of waste is reduced. The design improvements also benefit transmutation since increasing power density increases the rate that TRU is destroyed and higher burnup enables more TRU to be transmuted before the fuel is reprocessed. For both traditional and transmutation fuels, higher burnup significantly reduces the amount of plutonium in the spent fuel for a given amount of energy generation, and enhances the proliferation resistance by increasing the fraction of Pu-238 in the plutonium [Zhiwen et al., 2005].

To increase the maximum power density, the fuel rods need to be redesigned. For example, they can be changed from the traditional pin design to an annular geometry with internal and external cooling, which increases the cooled surface area for the same volume of fuel. Research has been done to optimize the geometry, materials, and manufacturing of this annular fuel and recently prototype annular rods were irradiated in the MIT reactor. This scoping analysis sought to verify the computer modeling of the thermal hydraulic and irradiation behavior of the rods. In addition, because the annular rods used vibration-packed UO₂ fuel grains (VIPAC) instead of traditional sintered pellets, the in-core irradiation also provided an opportunity to examine the VIPAC fuel irradiation behavior [Kazimi et al., 2005]. VIPAC is an attractive technology because it increases the ease of manufacturing; a simple manufacturing process is especially important for fuels with significant amounts of TRU because of its high activity and spontaneous fission rate.

One of the tasks of this project is the post-irradiation examination of the annular fuel rods. This experimental analysis is critical because it will allow measurement of the burnup, fission gas release, and structure of the fuel, which will help to validate the computer models and give insights into future optimization strategies.
In addition to the geometry changes, a new cladding material could allow increased burnup and power density by improving heat transfer and the thermal, chemical, and mechanical stability of the rod. Such improvements in behavior during normal and transient operation would allow power density and burnup limits set by safety analysis to be increased.

Silicon carbide (SiC) has been proposed as such a replacement for the traditional Zircaloy cladding, as it has beneficial features like reasonable thermal conductivity, a higher melting temperature, does not experience creep like a metal, and does not react exothermically with water. In order to determine the viability of SiC cladding it will be necessary to perform computer modeling of its in-core behavior, and ultimately to conduct irradiation testing of prototype cladding designs for verification of the effects of radiation damage to its mechanical and thermal properties.

1.2 Objectives and Scope

The overall objective is to examine some of the innovative ideas for increasing the power density and burnup limits for LWR fuels, in particular by using ceramic cladding for internally- and externally-cooled annular fuel containing UO₂ and mixed oxide (MOX) fuel. This objective will be accomplished through four tasks.

The first task is modeling of the thermal and mechanical behavior of the SiC cladding using a computer code. This requires accurate prediction of the relevant properties of SiC cladding given different temperature, stress, and irradiation conditions. Next, these properties will be used to create a model that appropriately predicts the behavior of a SiC-clad fuel rod under realistic LWR operating conditions. The behavior of this rod can then be compared to a Zircaloy-clad rod under identical circumstances. Finally, this modeling will be used to explore the benefits and limitations of SiC cladding by quantifying important behavior, measuring the sensitivity of the cladding behavior to property variations, and optimizing the geometry.
The second task is an assessment of the attempt to improve the performance of the proposed annular fuel design by using SiC cladding. This first involves combining the SiC cladding model with a previously developed internally- and externally-cooled annular fuel rod model [Yuan et al., 2004]. Next, the performance of this SiC-clad annular fuel rod will be compared to a Zr-clad annular rod under identical conditions in order to understand what additional advantages the ceramic provides, and both rod types will be studied in combination with sintered annular fuel pellets and the VIPAC fuel form. In addition, because transmutation fuels would contain a large amount of plutonium, a MOX fuel model will be incorporated. Modeling MOX fuel allows a study of the effect of different fuel compositions on fuel rod behavior.

The third task is to conduct the post irradiation examination of the prototype annular UO₂ fuel rods. The goals of the examination are to obtain estimates for the average burnup of the VIPAC fuel, the fraction of fission gas released, and the distribution of fuel particles within the rod using facilities available at the MIT Nuclear Reactor Laboratory. This information will be compared to the predictions of the computer models in order to qualify their precision. It will also expand understanding of the behavior of the annular rod design and the VIPAC fuel under conditions similar to that of a LWR core.

The final task is preparation for the in-core irradiation of SiC cladding samples in the MIT reactor (MITR-II). This preparation involves the design and construction of the in-core test rig, and preparation of the procedures necessary for loading, on-line monitoring, and removal of the rig from the core. This irradiation will allow multiple cladding samples of different shapes and construction to be irradiated at known temperatures and fluxes. The test rig also allows samples to be replaced mid-run without major disassembly of the rig. Finally, the test rig must meet all reactor safety requirements and not interfere with possible concurrent in-core experiments. The post-irradiation examination of the SiC specimens is not within the scope of this work.
1.3 Internally- and Externally-Cooled Annular Fuel

It is desirable to reduce the fuel temperature in order to reduce fission gas release, swelling, and increase the margin to overheating. Because the highest temperature in a fuel rod occurs in the center of the pellet, previous investigations in the U.S. [Caner and Dugan, 2000] and in Russia [Bibilashvili et al., 1994] have looked at creating annular fuel pellets. These sintered pellets are formed with a central void, usually 10-50 volume percent, which removes the temperature peaking at the center of the pellet.

This central void is still limiting, however, since heat can still only be removed through the outer surface of the pellet and it creates a channel for fuel and debris relocation. Recent work at MIT has resulted in the design of an internally- and externally-cooled (I&EC) annular fuel rod, which creates an open channel through the center of the fuel rod to allow coolant flow and the removal of heat. Because the goal of this investigation was a fuel rod that could be used in existing LWRs, the rod configuration was constrained by the Westinghouse PWR fuel assembly dimensions, and a 13x13 fuel rod array, shown in Figure 1.1, was chosen as the base design [Kazimi, 2002].

![Figure 1.1 Conceptual Design for 13x13 I&EC Annular Fuel PWR Assembly](Kazimi, 2002).
1.3.1 Sintered Pellets

Similar to the annular pellets for solid fuel rods discussed above, one proposed approach to the I&EC annular fuel rods is through pressed and sintered annular pellets. Such a manufacturing approach has been considered previously in France with the Advanced Plutonium Assembly, which used large I&EC plutonium-fueled rods in place of four UO$_2$ pins in a 17x17 PWR assembly [Puill et al., 2001].

Sintered annular pellets are able to achieve the same density as standard sintered pellets, but are more difficult to manufacture with the appropriate tolerances on the outer and inner surfaces. In addition, asymmetry of the pellet-cladding gaps is a point of concern [Puill 2001]. Such asymmetries may occur due to thermal expansion of the fuel, which will tend to close the outer fuel-cladding gap first. This closure will cause the gap conductance to be much larger on the fuel’s outer surface, and correspondingly the heat flux through the fuel rod’s outer diameter will increase. A higher heat flux will have safety implications, as it reduces the Departure from Nucleate Boiling Ratio (DNBR). DNBR has an NRC regulated minimum, and that would be a limiting factor for the sintered annular fuel’s performance in a PWR. Similarly, in Boiling Water Reactor there would be concerns over exceeding channel heat flux limits. Possible methods for alleviating this problem are increasing the initial outer radial gap, decreasing the thermal conductivity of the outer gap, adding highly conductive bonding filler in place of the gap cover gas, or decreasing the inner gap [Yuan et al., 2004].

The recent MIT design for I&EC annular fuel with sintered annular pellets, shown in Figure 1.2, was analyzed for use in a PWR in 13x13 assemblies [Yuan et al., 2004]. In a collaboration between Gamma Engineering Corporation, MIT, and Westinghouse Electric Corporation, these pellets were successfully manufactured by Westinghouse in 2003. These pellets, which are shown in Figure 1.3, were made to demonstrate the mechanical and economic feasibility of creating sintered UO$_2$ pellets, and that the manufacturing was within the scope of current pellet fabrication facilities [Kazimi, 2003].
1.3.2 VIPAC Fuel

An alternative to the use of sintered pellets is Vibration Packed (VIPAC) fuel, which consists of fuel grains that are compacted into the fuel rod via vibration and compressive
impacts. These grains are obtained by crushing and milling sintered fuel forms, then sorting the resulting shards by diameter. This technique has been used to create MOX and UO$_2$ fuel for irradiation in the U.S. and Russia [Yuan et al., 2004].

The main benefits of VIPAC fuel are easy loading of the annular geometry, elimination of the fuel-cladding gap, and simple fuel manufacturing. The simple manufacturing of the VIPAC grains and straightforward process for loading the fuel rod are key advantages over the sintered pellets, and these factors could translate into an economic incentive for using VIPAC fuel. Since there is no fuel-cladding gap the initial thermal resistance at that interface is significantly reduced [Yuan et al., 2004]. This addresses the asymmetry problem found with sintered pellets while at the same time lowering the average fuel temperature. In addition, VIPAC fuel irradiation have shown less chemical and mechanical interaction with the inside surface of the cladding than pelletized fuel.

The difficulty with VIPAC is achieving acceptable fuel smear densities, at least equivalent to those of sintered pellets. A high smear density is important both to maximize fuel loading and to achieve the highest possible thermal conductance in the fuel. VIPAC fabrication techniques have achieved up to 86% of theoretical density (TD), compared to the standard sintered pellet density of 94% TD [Icenhour, 2005]. It is possible that the density could be increased further through selection of different grain sizes, grain mixing ratios, packing steps, and additives such as powdered uranium metal.

The I&EC annular fuel rod designed by MIT with sintered annular pellets was also analyzed for use with VIPAC fuel. The inner and outer dimensions of the rod are the same, however because of the nature of VIPAC fuel there are no gaps between the fuel and cladding, as shown in Figure 1.4 [Yuan et al., 2004].

In addition, in 2003 a four-foot long I&EC VIPAC annular fuel rod was manufactured by the Atomic Energy of Canada, Ltd. (AECL) Chalk River Laboratory to the MIT-specified annulus dimensions. By using two size categories of fuel granules (25-53 μm and 250-
595 µm), and packing by impacting the fuel rod and using an annular tamping rod, AECL was able to achieve up to 76.9% TD [Kazimi, 2003].

![Figure 1.4 Schematic of I&EC VIPAC Annular Fuel for a 13x13 Assembly](image)

**Figure 1.4 Schematic of I&EC VIPAC Annular Fuel for a 13x13 Assembly** [Kazimi, 2003].

### 1.4 MIT I&EC Annular Fuel Irradiation Program

As part of a US Department of Energy sponsored program, six I&EC fuel rods, shown in Figure 1.5, were manufactured by AECL for irradiation in the MIT reactor. These fuel rods contained 5% enriched UO₂ in VIPAC grains, and three had uranium metal powder added to increase the smear density. The purpose of this irradiation was to investigate the behavior of the fuel as a result of the annular geometry and VIPAC fuel form. In particular, the goals were to examine the temperature difference between the inner and outer cladding surfaces, as well as the temperature behavior with burnup. Other parameters of interest were fission gas release, fuel swelling and relocation, and average burnup [Kazimi, 2003].
The geometry of these fuel rods was slightly different than the 13x13 design specified earlier by MIT because of manufacturing constraints and the need to enclose the cladding in a secondary containment before irradiation. The fuel rods were 3.94" long with a 2.76" active fuel length; the fuel region had an OD of 1.17" and an ID of 0.93". By using two grain sizes (25-53 μm and 250-500 μm) AECL was able to achieve a smear density of up to 82% TD in the UO₂-only fuel rods, and 87.5% TD in the fuel rods with 15% uranium metal powder added [AECL, 2003].

Two of the UO₂-only fuel rods were irradiated in the core of the MIT reactor from March to September of 2004 inside of specially designed I&EC aluminum capsules [Kazimi et al., 2004]. The top of the capsule assembly is shown in Figure 1.6. The inside and outside surfaces of these capsules were instrumented with thermocouples to allow the temperature at the inner and outer cladding to be monitored during irradiation, and cover gas from inside of the aluminum capsule was periodically sampled to measure any fission gasses escaping from weld imperfections.

After irradiation, the two annular fuel rods were removed from the core and placed in underwater storage for one year to allow for cooling and decay. In August of 2005, both
irradiated capsules were removed from storage and underwent a post-irradiation examination at MIT. The design, results, and an analysis of this examination is presented in Chapter 6 of this report. In the future, these capsules may be sent to the Idaho National Laboratory’s Hot Fuel Examination Facility for further analysis.

Figure 1.6 I&EC VIPAC Annular Fuel irradiation Rig Installed in the Core of the MITR-II.

1.5 Silicon Carbide

1.5.1 Background of SiC Development

Silicon Carbide (SiC) has been considered for use in high-temperature, high-radiation environments since first investigated by Rovner and Hopkins in 1976 as a first wall material for fusion reactors [Rovner, 1976]. It has also been considered as structural coating on fuel kernels for high temperature gas-cooled reactors for many years [CEGA, 1993], and as an inert matrix for fuel pellets [Alkan et al., 2001]. Pure SiC has attractive properties when compared to available metal alloys, primarily as a consequence of relative stability up to tens of Displacements per Atom (DPA). Unlike metals, it
experiences minimal swelling or loss of strength, does not creep below 1000°C, and is less prone to degrading chemical interactions. Importantly for fission energy applications, it also has a small neutron absorption cross section [Hasegawa, 2000].

SiC can be manufactured using several different processes, although the one of most interest for nuclear applications is Carbon Vapor Deposition (CVD) because it is able to produce a high-purity SiC layer. This process involves reacting silicon and carbon compounds, often in gaseous form, with hydrogen at high temperatures. This has been used to coat fuel particles, but can also be used to create bulk solids [Brook, 1991].

A significant issue when considering SiC as a moderate temperature (<1000°C) structural material is primarily its brittle fracture characteristic. The small elastic yield is significant when it must operate up to PWR core temperatures and pressures, around 300°C and 15 MPa, since it cannot dissipate accumulating external stresses (from irradiation swelling and thermal expansion of other materials, or fission gas release) through creep. A second difficulty is the degradation of SiC thermal conductivity with DPA. Although stoichiometric SiC has excellent thermal conductivity, after only one DPA the thermal conductivity will saturate at a much lower value due to accumulation of microstructure defects, typically to 2-8 W/m-K. This is significantly less than other common reactor materials exposed to similar conditions, such as Zircaloy [PNNL, 2002].

1.5.2 SiC Composites

In order to create large SiC structures, as well as to increase bend strength, SiC/SiC composite materials were developed. The composite consists of SiC fibers arranged parallel or braided, and then bonded with an SiC matrix. There are several fabrication techniques, depending on the method of matrix infiltration, but the two of most interest in this work are Carbon Vapor Infiltration (CVI) and Polymer Impregnation Pyrolysis (PIP) [Hasegawa, 2000]; both methods create a high purity SiC matrix between the fibers. The fibers provide large tensile strength and the ability to withstand localized fractures without completely failing the structure. The matrix provides stability while allowing the
fibers some ability to flex, meaning the composite will have significantly more yield perpendicular to the fiber axis than a solid structure.

The mechanical benefits of the composite are balanced by the creation of a less dense, porous structure, with a lower thermal conductivity. In addition, because the fibers generally contain impurities of free silicon and carbon, as well as nitrogen or oxygen additives, there tends to be a difference in irradiation swelling behavior between the matrix and the fibers that must be considered.

### 1.5.3 Advanced Clad Project

A collaboration organized by Gamma Engineering, and involving the MIT Center for Advanced Nuclear Energy Systems (CANES), is investigating the use of SiC as a fuel rod cladding material for Light Water Reactors (LWRs). This concept involves the use of a duplex design, which combines an SiC monolith and an SiC/SiC woven composite to create a cladding tube where the SiC duplex replaces the traditional Zircaloy cladding. An example of this duplex design is shown in Figure 1.7. The monolith provides for containment of fission gasses while the composite provides additional strength and protection of the monolith surface.

![Figure 1.7 Example SiC Duplex Solid Fuel Rod.](image)
There is interest in using SiC as a cladding material because it maintains strength even at high temperature (>1000°C), has a small neutron absorption cross section, and in general has good property performance to high DPA. It is expected that these characteristics will allow higher allowable cladding temperatures, and in particular, better transient and accident performance, and power uprates. In addition, the stability of its physical properties under extended irradiation and ability to contain larger internal pressures without creeping outward may enable the extension of the fuel rod’s maximum allowable burnup.

The MIT part of the current collaboration involves modeling the behavior of SiC duplex cladding in LWRs using several computer codes, including the steady-state thermomechanical code FRAPCON, which is covered in Chapters 2 through 5 of this report. Other modeling work is to include transient and accident analysis using the codes FRAPTRAN and RELAP. In addition to the computer modeling, this project also includes the irradiation of various duplex cladding designs for at least three months in the MIT reactor under PWR conditions. After irradiation, the samples will undergo PIE both at MIT and at Oak Ridge National Laboratory. The purpose and design of this experiment is covered in more detail in Chapter 7 of this report.

1.6 FRAPCON Computer Code

The FRAPCON-3 steady state fuel rod modeling code was developed by the Pacific Northwest National Laboratory for use by the U.S. Nuclear Regulatory Commission in evaluation of LWR fuel rod behavior up to a burnup of 65 MWd/kgU. The code models the fuel and cladding of a single fuel rod, and calculates temperature distributions, stress and strain, fission gas release, cladding oxidation, and other physical behavior as a function of an input power history and core conditions [Berna et al., 1997].

FRAPCON is a deterministic code that relies on a combination of theoretical and empirical relations to determine the properties of the fuel rod only at discreet time steps, where the step interval is generally days to weeks. The code’s predictions have been
benchmarked against experimental irradiation data, and it is expected to provide characteristic results for limited variations on traditional fuel rod design and operating conditions.

The use of an I&EC annular fuel rod geometry, VIPAC fuel, and SiC cladding, as investigated in this report, are all outside the scope of FRAPCON's original programming. Adding these features requires modification of the source code and has resulted in the creation of several distinct versions of FRAPCON at MIT. The modifications made to FRAPCON to incorporate SiC cladding are discussed in Chapter 3 of this report. Pertinent modifications made by previous researchers, and other official updates to the source code are discussed in Chapter 2.

1.7 Thesis Organization

This thesis covers both theoretical and experimental investigation relevant to the development of a new fuel design for fission reactors: the development of computer based SiC-clad fuel rod models for evaluation, and the design and evaluation of experimental studies of the proposed fuel, fuel rod geometry, and cladding materials.

In Chapter 2, the general structure and solution routine of the FRAPCON steady-state fuel rod modeling code is discussed. Several major modifications to the code relevant to this project are also discussed, involving the implementation of I&EC annular fuel, VIPAC fuel, MOX fuel, and improved modeling at high burnup.

In Chapter 3, new FRAPCON routines that were created to model SiC cladding are discussed and compared to the available data. These changes include all of the material properties, with particular focus on thermal conductivity, thermal expansion, and irradiation swelling.

In Chapter 4, the newly created version of FRAPCON with SiC cladding is used to model fuel rods under typical reactor operating conditions and conditions of special interest,
such as high power density and high burnup. In addition, possible avenues for enhancing the performance of the SiC-clad fuel rod are explored, such as variation of fill gas pressure, use of annular pellets, different assembly configurations, and MOX fuel.

In Chapter 5, the SiC cladding models are applied to the I&EC annular fuel version of FRAPCON in order to investigate the benefits of this combined construction. There is a discussion and analysis of the modeling of both VIPAC fuel and sintered annular pellets.

In Chapter 6, the post-irradiation examination of prototype I&EC annular fuel rods is discussed. This section covers both the design of the PIE station at the MIT reactor and results of the examination of two irradiated fuel rods. The fuel rods were examined visually and were analyzed using gamma spectroscopy to infer fuel distribution and swelling, burnup, and fission gas release.

In Chapter 7, the design and construction of a test rig for the irradiation of SiC cladding samples is discussed. This rig is designed to allow irradiation of multiple samples of unfueled cladding in PWR pressure, temperature, and chemistry conditions in the core of the MIT reactor. In addition, groups of samples can be removed from the rig in MIT hot cell facilities after irradiation, and the rig returned to the core with some new samples for continued irradiation.

In Chapter 8, the results of this thesis research will be summarized and possibilities for future investigation of the topics covered in this work will be proposed.
2. Fuel Rod Modeling in FRAPCON

2.1 Code Structure

The FRAPCON computer code couples the thermal and mechanical behavior of a single LWR fuel pin during irradiation at a specified linear heat generation rate under specified core conditions. It arrives at a solution through the use of empirical models for fuel and cladding properties, which feed into theoretical calculations of temperature distributions and physical (mechanical, chemical, and nuclear) responses. It then iterates this process to find the steady-state behavior of the fuel rod after a given segment of irradiation history [Berna et al., 1997]. A simplified version of the FRAPCON solution scheme is shown in Figure 2.1.

![Figure 2.1 Simplified Flow Chart of FRAPCON-3 Iterative Solution Scheme](Berna et al., 1997).
The required code inputs include fuel and cladding geometry, fuel enrichment, primary coolant conditions, and the listing of the neutron flux, linear heat generation rate, and axial power shape at each time step. The user also inputs a mesh sizing for FRAPCON’s finite difference calculations of pellet heat conduction and fission gas release. FRAPCON takes these inputs and then uses a pre-defined set of relations for UO₂ or MOX pellets and Zircaloy cladding to calculate physical properties during each iteration. In order to specify new material behavior, the subroutines controlling the desired property must be modified and the code re-compiled.

2.1.1 Implementation of I&EC Annular Fuel

The FRAPCON-3 source code was modified by Yi Yuan [Yuan et al., 2004] to model I&EC annular fuel rods with Zircaloy cladding, and both sintered annular pellets and VIPAC fuel. This modified code was then used to analyze the behavior of the new fuel rod geometry and fuel forms, and to conduct parametric studies on the fuel rod design.

The changes that were made to the source code can be generalized into seven areas:

- Radial fuel temperature profile calculation
- Fuel-cladding interaction model
- Heat flux and coolant flow models
- Fission gas release
- Fuel dimensional changes
- Radial power profile
- Cladding conditions

These modifications were focused on adding parameters for the simultaneous calculation of the inner and outer cladding and fuel behavior where previously only the outer surface was of concern, as well as new physical models to account for the increased moderation in the inner coolant channel.
2.1.2 Implementation of VIPAC Fuel

As a part of the implementation of the I&EC annular fuel models in FRAPCON, models for VIPAC fuel were added [Yuan et al., 2004]. In some ways, the VIPAC fuel models resemble sintered pellets operating in a closed fuel-cladding gap regime, with some differences based on the interaction of individual fuel grains. The VIPAC fuel model differs from the solid sintered pellets and solid annular sintered pellets in the same basic ways: the inner and outer fuel-cladding gaps are always closed, fuel radial relocation is removed, thermal expansion serves to contract the inner annulus as well as expand the outer diameter, and fission gas released is enhanced in proportion to total exposed grain surface area.

2.1.3 Implementation of Updated $\text{UO}_2$ and MOX Fuel Models

The organization responsible for the development of FRAPCON, Pacific Northwest National Laboratory, issued an update for FRAPCON-3 in May 2005 [Lanning et al., 2005]. This update, FRAPCON-3.3, included several changes relevant to this work including updates to models for the properties of urania and MOX fuel. Because these changes increase the accuracy of the code’s predictions, it was desirable to incorporate these changes into the versions of FRAPCON created for I&EC annular fuel with sintered pellets and VIPAC fuel.

The changes to the urania fuel models incorporate relations based on newly available experimental data. Both the urania thermal conductivity and fission gas release models were modified to better reflect data at higher temperatures and burnup (>2500 K and 30 MWd/kgU).

The update also contains changes to many of the MOX fuel models, which are receiving attention due to renewed interest in MOX use in LWRs. A significant amount of new data has become available worldwide from MOX irradiations, and the target MOX plutonium composition has decreased. To reflect this new information, the following models were updated: MOX thermal conductivity, fission gas release and He production, swelling and thermal expansion, and radial power profile [Lanning et al., 2005].

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In addition to these model-specific changes, varieties of other small changes were made to correct errors or oversights in the previous release version of FRAPCON.

2.1.4 Implementation of High-Burnup Fission Gas Release Model

The recommended maximum burnup in FRAPCON, based on the data used for the creation of the fuel thermal and mechanical properties and fission gas release, is 62 MWd/kgU, which provides sufficient coverage of expected burnup in current generation LWR fuel pins [Lanning et al., 2005]. However, in this investigation there is interest in extension of the maximum burnup because of the combination SiC cladding's good radiation tolerance and the improved performance of I&EC annular fuel (i.e. lower fuel and cladding temperatures). It is thought that these factors could allow LWRs to achieve burnup up to 100 MWd/kgU.

An investigation into the possibility of using FRAPCON to model fuel rods into this very high burnup regime was performed by Yun Long at MIT [Long, 2002]. In the course of this research, fission gas release was identified as a driving parameter for fuel rod performance at high burnup; however, the current FRAPCON predictions did not agree well with measured data at very high burnup. As a result, several modifications were made to the urania fission gas release model to conform better to the current phenomenological understanding of fission gas release, and replicate experimental release data at very high burnup more accurately.

The new models explicitly treat the buildup of fission gasses in the rim region of the pellets, and threshold release caused by fission gas saturation at high burnup. In particular, the threshold release model replaces the standard athermal model, which treated fission gas release as linear at high burnup.
2.2 Benchmarking of Updates

2.2.1 FRAPCON-3.3 with High Burnup Modifications

As the first part of this investigation, Yun Long’s changes to FRAPCON concerning fission gas release in UO₂ at high burnup were incorporated into the latest release of FRAPCON, version 3.3. This new version, hereafter referred to as FRAPCON-3.3HB, was used as the basis for the development of all other FRAPCON versions used in this project.

In order to evaluate the performance of this new code, an input case was developed based on data from an experimental campaign measuring fission gas release up to high burnup. A recent study of the effect of high burnup on UO₂ was conducted by Manzel and Walker in Germany [Manzel and Walker, 2000]. In this study, a group of similar fuel rods was tracked in Siemens PWRs. In order to reach extended burnup these rods were removed from spent assemblies and placed into a fresh assembly, then irradiated for another series of burnup cycles. During each refueling, some rods were removed from this cycle and destructively examined for microstructure changes and fission gas release. In this way, some fuel rods were irradiated to a maximum burnup of over 100 MWd/kgU. From this study, fission gas release data is available for average fuel rod burnup between 19 and 98 MWd/kgU.

Data given by Manzel and Walker concerning linear heat generation rate, enrichment, irradiation cycles, and fuel rod construction was incorporated into a FRAPCON input file. However, insufficient data on certain factors, such as coolant characteristics and neutron flux, were given to completely specify the required input, and therefore additional data had to be obtained from other sources. The missing information was incorporated from FRAPCON input data supplied by Pacific Northwest National Laboratory on a reactor similar to the Siemens PWRs, the Oconee plant, a Babcock and Wilcox PWR utilizing 15x15 assemblies with fuel rod dimensions nearly identical to the Siemens design. The fuel and cladding properties used in this input file, hereafter referred
to as the “Manzel” test case, are given in Table 2.1. The axial power shape is the Oconee mid-life profile, which has a peaking factor of 1.12.

Table 2.1 Initial Fuel and Cladding Properties for the Manzel Test Case.

<table>
<thead>
<tr>
<th>Dimension</th>
<th>Units</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Cladding outside diameter</td>
<td>cm (in.)</td>
<td>1.09 (4.30x10⁻¹)</td>
</tr>
<tr>
<td>Cladding inside diameter</td>
<td>cm (in.)</td>
<td>9.58x10⁻¹ (3.77x10⁻¹)</td>
</tr>
<tr>
<td>Cladding thickness</td>
<td>mm (in.)</td>
<td>6.73x10⁻¹ (2.65x10⁻²)</td>
</tr>
<tr>
<td>Clad arithmetic mean roughness</td>
<td>mm (mils)</td>
<td>5.00x10⁻⁴ (1.97x10⁻²)</td>
</tr>
<tr>
<td>Diametral gap thickness</td>
<td>mm (mils)</td>
<td>2.54x10⁻¹ (10.0)</td>
</tr>
<tr>
<td>Fuel pellet diameter</td>
<td>cm (in.)</td>
<td>9.32x10⁻¹ (3.67x10⁻¹)</td>
</tr>
<tr>
<td>Fuel stack height</td>
<td>m (ft.)</td>
<td>3.58 (11.8)</td>
</tr>
<tr>
<td>U-235 enrichment</td>
<td>at% in U</td>
<td>3.85</td>
</tr>
<tr>
<td>Fuel rod pitch</td>
<td>cm (in.)</td>
<td>1.42 (5.60x10⁻¹)</td>
</tr>
<tr>
<td>Channel equivalent diameter</td>
<td>cm (in.)</td>
<td>1.27 (4.99x10⁻¹)</td>
</tr>
</tbody>
</table>

The Manzel input case was run in three versions of FRAPCON for comparison: the most recently released version (FRAPCON-3.3), the version created by Yun Long incorporating a new high burnup fission gas release model (FRAPCON-3.2HB), and the new version created for this study that combines the new fission gas release model with release version 3.3 (FRAPCON-3.3HB). The average rod linear heat generation rate at each time step for this input is given in Figure 2.2.
The fission gas release calculated by each of these three versions of FRAPCON is presented in Figure 2.3, where the data from Manzel and Walker are approximated by a solid line with error bars showing the approximate scatter in the data. Up to about 70 MWd/kgU, all three codes predict a similar fission gas release fraction, within 5%. However, at this burnup the effect of the new fission gas release model is apparent; there is an exponential increase of gas release with burnup in the modified versions of FRAPCON compared to a nearly linear dependence in FRAPCON-3.3, which is based on the athermal release model.

Above 70 MWd/kgU there is a dramatic disparity between the two models, with the modified code comparing well with the measured data with about 25% release by 100 MWd/kgU; FRAPCON-3.3 under predicts this release by almost 70%. This disparity is especially significant at very high burnup when there is a large inventory of fission gas in the fuel, so that the fraction released contributes a greater amount to pressurization of the rod.
The small difference between the fission gas release predicted by FRAPCON-3.2HB and FRAPCON-3.3HB can be attributed to the modifications made by the code’s developers, discussed in section 2.1.3, to the urania thermal conductivity and fission gas release model.

2.2.2 I&EC Annular Fuel with Sintered Annular Pellets Modifications

In the same way that Yun Long’s very high burnup fission gas release model was addressed in the previous section, the I&EC annular fuel model was integrated into FRAPCON-3.3HB. Yi Yuan, who created this model [Yuan et al., 2004], made two separate versions of the I&EC annular fuel code: one to handle sintered annular pellets (FRAPCON-SA), and a second for VIPAC annular fuel (FRAPCON-VA). These two versions of the code were used to create two new versions incorporating the version 3.3 FRAPCON updates and the high burnup fission gas release modifications to create FRAPCON-3.3HB-SA and FRAPCON-3.3HB-VIPAC.

In order to evaluate the new solid annular version of FRAPCON, an input file developed by Yi Yuan [Yuan et al., 2004] to evaluate I&EC fuel performance in current PWRs was used. This case (hereafter referred to as SA-100) represents an I&EC annular fuel rod of the 13x13 MIT design. It is irradiated under conditions equivalent to standard fuel rods in
a Westinghouse 4-loop PWR at 100% of rated power (3411 MWth) with the linear heat generation rate adjusted for the new fuel geometry. The properties of the fuel rod used for this case are presented in Table 2.2.

Table 2.2 Initial Fuel and Cladding Properties for the SA-100 Test Case.

<table>
<thead>
<tr>
<th>Dimension</th>
<th>Units</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Outer cladding outside diameter</td>
<td>mm</td>
<td>15.4</td>
</tr>
<tr>
<td>Outer cladding inside diameter</td>
<td>mm</td>
<td>14.2</td>
</tr>
<tr>
<td>Inner cladding outside diameter</td>
<td>mm</td>
<td>8.63</td>
</tr>
<tr>
<td>Inner cladding inside diameter</td>
<td>mm</td>
<td>9.78</td>
</tr>
<tr>
<td>Outer cladding thickness</td>
<td>mm</td>
<td>0.571</td>
</tr>
<tr>
<td>Inner cladding thickness</td>
<td>mm</td>
<td>0.571</td>
</tr>
<tr>
<td>Clad arithmetic mean roughness</td>
<td>mm</td>
<td>0.003</td>
</tr>
<tr>
<td>Inner diametral gap thickness</td>
<td>mm</td>
<td>0.124</td>
</tr>
<tr>
<td>Outer diametral gap thickness</td>
<td>mm</td>
<td>0.124</td>
</tr>
<tr>
<td>Initial fuel smear density</td>
<td>%</td>
<td>95</td>
</tr>
<tr>
<td>Fuel stack height</td>
<td>m</td>
<td>3.66</td>
</tr>
<tr>
<td>U-235 enrichment</td>
<td>%</td>
<td>5.00</td>
</tr>
<tr>
<td>Fuel rod pitch</td>
<td>mm</td>
<td>16.5</td>
</tr>
<tr>
<td>Axial peak-to-average power ratio</td>
<td></td>
<td>1.3</td>
</tr>
</tbody>
</table>

Using this input file, the results of FRAPCON-SA and the new FRAPCON-3.3HB-SA were compared. The power history of this input case, identical for the two versions of the code, is given in Figure 2.4. This power history yields a discharge burnup of about 58 MWd/kgU.
The average fuel temperature for both of these cases decreases in steps as the power level changes, as shown in Figure 2.5, and slowly rises over each step mainly due to fission gas release and cladding oxide buildup decreasing the efficiency of heat conductance through the rod. The temperature behavior up to the first 10 MWd/kgU is more complex as the
inner and outer fuel cladding gaps close, increasing gap conductance, shown in Figure 2.6 for the outer fuel-cladding gap and Figure 2.7 for the inner gap.

The gaps close by 20 MWd/kgU, causing a sudden increase in the gap conductance in both codes, however the gap conductance model in FRAPCON-3.3HB-SA predicts a
higher final conductance due to the changes introduced in the version update. In particular, the conductance drops as the fission gas release, shown in Figure 2.8, increases. The athermal fission gas release model in the original code predicts a linear release past 40 MWd/kgU, whereas the new version has yet to reach the threshold for its exponential release.

Figure 2.8 Fission Gas Release Fraction Predicted by Original and Updated FRAPCON I&EC with Sintered Annular Pellets.

Figure 2.9 Outer Cladding Inner Diameter Predicted by Original and Updated FRAPCON I&EC with Sintered Annular Pellets.
The dimensional changes of the cladding in each model correspond well with each other; both the inner and outer cladding creep down onto the fuel, then expand as the fuel swells outward. The new FRAPCON model predicts a larger amount of cladding creep down because there is less fuel swelling, which is manifested as a smaller cladding outer diameter.

### 2.2.3 I&EC Annular Fuel with VIPAC Fuel Modifications

An input file similar to SA-100 was developed by Yi Yuan [Yuan et al., 2004] for an I&EC annular fuel rod with VIPAC fuel corresponding the MIT design dimensions and loading. This input file, VA-100, corresponds to conditions in a standard Westinghouse PWR operating at 100% of rated power (3411 MWth) with the linear heat generation rate adjusted for a 13x13 assembly size. The properties of the fuel rod used in this analysis are given in Table 2.3. Note that the VIPAC version of FRAPCON only supports a single grain size, whereas in experimental VIPAC fuel fabrication multiple grain sizes are used to increase the packing density.
Table 2.3 Initial Fuel and Cladding Properties for the VA-100 Test Case.

<table>
<thead>
<tr>
<th>Dimensions</th>
<th>Units</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Outer cladding outside diameter</td>
<td>mm</td>
<td>15.4</td>
</tr>
<tr>
<td>Outer cladding inside diameter</td>
<td>mm</td>
<td>14.2</td>
</tr>
<tr>
<td>Inner cladding outside diameter</td>
<td>mm</td>
<td>8.63</td>
</tr>
<tr>
<td>Inner cladding inside diameter</td>
<td>mm</td>
<td>9.78</td>
</tr>
<tr>
<td>Outer cladding thickness</td>
<td>mm</td>
<td>0.571</td>
</tr>
<tr>
<td>Inner cladding thickness</td>
<td>mm</td>
<td>0.571</td>
</tr>
<tr>
<td>Clad arithmetic mean roughness</td>
<td>mm</td>
<td>0.003</td>
</tr>
<tr>
<td>Fuel particle diameter</td>
<td>µm</td>
<td>400</td>
</tr>
<tr>
<td>Initial fuel smear density</td>
<td>%</td>
<td>85</td>
</tr>
<tr>
<td>Fuel stack height</td>
<td>m</td>
<td>3.66</td>
</tr>
<tr>
<td>U-235 enrichment</td>
<td>%</td>
<td>5.00</td>
</tr>
<tr>
<td>Fuel rod pitch</td>
<td>mm</td>
<td>16.5</td>
</tr>
<tr>
<td>Peak-to-average power ratio</td>
<td></td>
<td>1.3</td>
</tr>
</tbody>
</table>

The power profile used in Annular-100 test case is the same as that shown in Figure 2.2 for the sintered annular pellets. In this case, the behavior of the fuel is more straightforward since both fuel-cladding gaps are always closed. The average fuel temperature for each case, shown in Figure 2.11, shows very close agreement between the old and new version of the code. Toward the end of life, the fuel temperatures diverge slightly as the athermal fission gas release model in FRAPCON-VA activates, shown in Figure 2.12.
The mechanical behavior predicted by the two codes is also in very good agreement, as shown in Figure 2.13 for the fuel outer diameter and Figure 2.14 for the fuel inner diameter. There is a slight initial contraction of the fuel due to densification, and then swelling causing the cladding to creep outward at approximately the same rate on the
inner and outer surface. This behavior is particular to VIPAC fuel, where the swelling of fuel grains will strain both the inner and outer diameter.

Figure 2.13 Fuel Outer Diameter Predicted by Original and Updated FRAPCON I&EC with VIPAC Fuel.

Figure 2.14 Fuel Inner Diameter Predicted by Original and Updated FRAPCON I&EC with VIPAC Fuel.
2.3 Integration of SiC Cladding

Although SiC has been investigated for use in fusion and gas-cooled fission reactor applications, there is not a large amount of data on its performance under irradiation in LWR conditions. Research has been conducted to identify the properties of SiC and SiC composites under conditions of interest such as temperature, fluence, and stress, and this information can be compiled to achieve an empirical approximation of the behavior under these conditions simultaneously. This approach to modeling fits well within the structure of the FRAPCON code, as each cladding property is handled by a separate subroutine.

This project introduces additional complications, since the duplex cladding involves the interaction of two distinct cladding layers: a solid monolith, and the fiber-matrix composite. To limit the scope of this thesis, it was decided to focus on the behavior of the composite, since it is hoped this structure will provide the majority of the strength of the fuel rod, and because it will be directly exposed to the reactor coolant. This simplification allows the cladding to be treated normally, as a single homogeneous cylinder. In addition, by assuming the SiC cladding behavior is governed by the same theoretical relations, such as mechanical response to stress and strain, as the zirconium-based cladding, the magnitude of changes to the FRAPCON code are minimized. This approximation is further aided by the limited scope of the cladding behavior allowed in FRAPCON, since in a reactor only minimal deformation of fuel rod is permitted.
3. General Silicon Carbide Cladding Model Development

3.1 Design Parameters

Over its operational lifetime, the fuel cladding in an LWR is subjected to a range of conditions that evolve from insertion to discharge. Early in life there are the highest temperatures, initial swelling, and restructuring of the fuel. Later in life the temperatures decrease but fuel-cladding mechanical interaction is more pronounced, the cladding is embrittled with hydrogen uptake and its thickness is reduced by oxidation, and there is more stress from fission gas buildup within the fuel and the fuel rod. Based on these considerations, a design envelope for the SiC clad was developed, and is given in Table 3.1; models created for this investigation therefore should be accurate at least within this range of variables.

<table>
<thead>
<tr>
<th>Condition</th>
<th>Max</th>
<th>Min</th>
</tr>
</thead>
<tbody>
<tr>
<td>Temperature</td>
<td>1000 K</td>
<td>300 K</td>
</tr>
<tr>
<td>Radiation Damage</td>
<td>50 DPA</td>
<td>0 DPA</td>
</tr>
<tr>
<td>Fiber Material</td>
<td>Stoichiometric SiC</td>
<td>SiC-based fibers (various impurities)</td>
</tr>
<tr>
<td>Matrix Material</td>
<td>Stoichiometric SiC</td>
<td></td>
</tr>
</tbody>
</table>

In the development of the SiC cladding property models, a specific set of materials of interest were identified based on the LWR operating criteria as well as progress in development and manufacturing of nuclear grade SiC. In general, these materials are defined as high purity and high density SiC. Also, although this modeling is focused on the composite, because the ultimate goal of the Advanced Clad Project is modeling of duplex cladding, it is necessary to quantify the behavior of both the monolith and the composite and to identify any significant differences. Based on a review of the literature on the manufacturing and evaluation of SiC and SiC composites, a listing of the materials of interest was compiled; the fibers are listed in Table 3.2 and monolith materials in Table 3.3.
Table 3.2 SiC Fibers of Interest [Ichikawa and Ishikawa, 2000 and Osborne et al., 1998].

<table>
<thead>
<tr>
<th>Name</th>
<th>Manufacturer</th>
<th>Elemental Composition</th>
</tr>
</thead>
<tbody>
<tr>
<td>Hi-Nicalon</td>
<td>Nippon Carbon</td>
<td>Si, C, O</td>
</tr>
<tr>
<td>Hi-Nicalon Type S</td>
<td>Nippon Carbon</td>
<td>Si, C, O</td>
</tr>
<tr>
<td>Sylramic</td>
<td>Dow Corning</td>
<td>Si, C, O, B, N, Ti</td>
</tr>
<tr>
<td>Tyranno SA</td>
<td>UBE Industries</td>
<td>Si, C, Al</td>
</tr>
</tbody>
</table>

Table 3.3 SiC Monolith Materials of Interest [Snead et al., 2005].

<table>
<thead>
<tr>
<th>Name</th>
<th>Manufacturer</th>
</tr>
</thead>
<tbody>
<tr>
<td>Morton CVD</td>
<td>Rohm and Haas</td>
</tr>
<tr>
<td>Hexoloy-SA</td>
<td>Carborundum</td>
</tr>
<tr>
<td>GE SiC</td>
<td>General Electric</td>
</tr>
</tbody>
</table>

3.2 Model Implementation

The changes discussed below modify the properties of the FRAPCON cladding model in order to simulate the behavior of SiC cladding. As discussed above, these new models will focus on the behavior of the SiC fiber-matrix composite, which is assumed to comprise the entire thickness of the cladding.

Each of these models was implemented in a new version of FRAPCON called FRAPCON-SiC. This version is based on FRAPCON-3.3HB, which was discussed in Chapter 2, and therefore already includes updates to the fuel behavior at high burnup.

3.3 SiC Thermal Conductivity

3.3.1 Background

The thermal conductivity of the cladding is important because it helps to determine the temperature of the fuel, which controls swelling, strength, and fission product transport. A higher thermal conductivity will decrease both the average cladding temperature as well as the average fuel temperature, and in general lead to better performance over the
fuel rod lifetime. The FRAPCON-SiC thermal conductivity model is particularly important in this investigation because the difference in conductivity behavior in Zircaloy and SiC may have a dramatic effect on long-term fuel rod performance.

Because of the lack of conduction shell electrons, thermal conduction in ceramics such as SiC is predominantly by phonon transport. Despite this fact, SiC can have an extremely high thermal conductivity, around 500 W/m-K at room temperature, when it is highly pure and crystalline. This is higher than most high conductivity materials such as copper (400 W/m-K), and would seem to make SiC an ideal cladding material [Snead et al., 2005].

For pure SiC, the thermal conductivity decreases as a function of temperature, due to increased phonon-phonon interactions, reaching a minimum around 1000°C. Irradiation also decreases the thermal conductivity dramatically by introducing defects in the SiC crystal, with the effect saturating around 1 DPA [Maruyama and Harayama, 2004]. The behavior of the composite is similar, although it starts with a lower thermal conductivity due to the anisotropy introduced by the fibers. In addition, the thermal conductivity in the radial direction (perpendicular to the fibers) is lower due to added phonon scattering from the fiber-matrix interfaces [Jones et al., 1997].

### 3.3.2 Model Development

The FRAPCON-3.3 model for Zircaloy cladding thermal conductivity is only a function of temperature. It increases slightly with temperature, and is about 16 W/m-K at the standard cladding temperature of 330°C. Because of its strong dependence on both temperature and fluence, the new SiC cladding model incorporates both variables.

For the FRAPCON-SiC model, the temperature dependence of SiC was based on the measured thermal conductivity of SiC composites given by Maruyama and Harayama [Maruyama and Harayama, 2004], and the displacement damage (measured in DPA, assumed to be approximately 1x10^{25} neutrons/m² [Osborne et al., 1998]) dependence given by Snead [Snead, 2004]. An empirical relation was developed that first calculates
the thermal conductivity as a function of temperature and DPA as a power relation such that,

\[ k_{\text{clad}} = k_{\text{sat}} \cdot \left( \frac{(d + d_0)}{d_{\text{sat}}} \right)^{-0.4} \]  

(3.1)

where \( d \) is the cumulative cladding DPA, \( d_0 \) is the effective DPA for \( k(T) > k_{\text{sat}} \), which is the cladding thermal conductivity after reaching the saturation DPA, \( d_{\text{sat}} \). In this model, it is assumed that the thermal conductivity saturates after 1 DPA at 4 W/m-K regardless of temperature. The temperature effect is then accounted for in the effective DPA factor,

\[ d_0 = d_{\text{sat}} \cdot \left( \frac{k_{\text{sat}}}{k(T)} \right)^{2.5} \]  

(3.2)

Until reaching the radiation saturation point, the temperature dependence of the cladding then follows the relation,

\[ k(T) = 8 \times 10^{-6} \cdot T_{\text{lad}}^2 - 0.02 \cdot T_{\text{clad}} + (k_{\text{rt}} + 26.4) \]  

(3.3)

where \( k_{\text{rt}} \) is the cladding thermal conductivity at room temperature (300 K) and 0 DPA, and \( T \) is the cladding temperature at the current geometric mesh point in degree Kelvin. As irradiation progresses, the conductivity becomes a weak function of temperature, and past the saturation DPA it is assumed to be constant at the saturation conductivity.

The results of this relation as a function of temperature compared to other data are plotted in Figure 3.1. It should be noted that data show higher values of thermal conductivity for un-irradiated composites, as presented in Figure 3.2. However, based on the majority of the data available, and for purposes of a conservative evaluation, the lower conductivity range was used, and for FRAPCON-SiC \( k_{\text{rt}} = 20 \) W/m-K.
Likewise, the relation as a function of radiation damage at room temperature is presented in Figure 3.3. Overall, there is good agreement between this empirical function and the
available data in terms of saturation time and shape, although there is some variation around the final value of the conductivity. Again, a conservative estimation was used to determine \( d_{\text{sat}} = 1 \text{ DPA} \) and \( k_{\text{sat}} = 4 \text{ W/m-K} \).

Combining the temperature and irradiation effects into the full model, Figure 3.4 shows the thermal conductivity predicted by FRAPCON-SiC as a function of temperature at different DPA, with the complimentary experimental data in Figure 3.5. Once again, there is good agreement both in the general behavior and in the absolute values of the thermal conductivity model and the experimental data. In all the data presented it is apparent that the temperature dependence of the thermal conductivity has moderate influence in the temperature range of interest \((600 - 800 \text{ K})\), but that this becomes less significant as irradiation proceeds. Given typical LWR flux levels, the irradiation saturation of the SiC thermal conductivity will take place within several weeks, leaving the major variable in the long-term performance of the cladding the value and stability of the saturation conductivity.

![Figure 3.3](image)

**Figure 3.3** Thermal Conductivity of SiC Composites Irradiated at Room Temperature.
Figure 3.4 SiC Composite Thermal Conductivity Predicted by FRAPCON as a Function of DPA and Temperature.

Figure 3.5 SiC Composite (Hi-Nicalon Type-S with CVI) Thermal Conductivity as a Function of DPA and Temperature [Youngblood, 2004].
3.4 SiC Thermal Expansion

Thermal expansion of the cladding is most important at the beginning of life, since the cladding temperature generally does not vary significantly during irradiation. The primary effect of the thermal expansion is to drive the change in the geometry of the radial fuel-cladding gap due to the difference between the expansion of the fuel and cladding. This gap creates a significant resistance to heat flow between the fuel and the cladding, and will therefore significantly affect the temperature of the fuel.

Based on published data, the thermal expansion coefficient for SiC composites has a weak temperature dependence in comparison to pure SiC, and is generally $2.5 \times 10^{-6}$ 1/K depending on the choice of fiber and matrix bonding method [Jones et al., 1997 and Schwetz, 2000]. See Figure 3.6 and Figure 3.7 for samples of some of the data for the thermal expansion coefficient of various composites, for measurement through the thickness or parallel to the fibers.

![Figure 3.6 Through Thickness Thermal Expansion Coefficient versus Temperature for SiC Composites.](image-url)
It is not apparent if there is a significant difference between swelling parallel and perpendicular to the fiber axis, so it was assumed that the expansion coefficient is identical in each direction. In addition, because of the small amount of data on variation with temperature, a constant value was used. The thermal expansion coefficient for SiC cladding used in FRAPCON-SiC is $3 \times 10^{-6}$ 1/K.

### 3.5 SiC Irradiation Swelling

Radiation interaction with the SiC crystal produces displacement damage and amorphization that has been experimentally observed to cause swelling. Recently atomistic modeling and simulation has been used to attempt to explain the cause and predict the extent of this swelling by studying relaxation after an amorphization of the SiC crystal structure similar to that caused by neutron and heavy ion bombardment [Romano et al., 2003]. This work has demonstrated that the swelling due to irradiation is a non-linear process, but additional study is needed for realization of a comprehensive theoretical model to predict macroscopic behavior. In general, this cladding swelling competes with the fuel swelling and thermal expansion to determine the fuel-cladding gap width, although the irradiation swelling takes place over a longer time scale.
The irradiation swelling of SiC composites with Hi-Nicalon fibers irradiated to 0.5 DPA are shown in Figure 3.8. Despite the large amount of scatter in the data, it seems that the composite swells slightly when irradiated at temperatures characteristic of PWR cladding, around 600 K [Ishihara et al., 2002]. It should be noted that some SiC fibers, such as Nicalon and Hi-Nicalon fibers, have been shown to increase in density under irradiation as a result of impurities like carbon. In contrast, the highly stoichiometric matrix would swell. New, higher purity fibers such as Hi-Nicalon Type-S behave more like stoichiometric SiC (swelling during irradiation), and therefore it is likely these would be used in nuclear-grade composites [Brook, 1991].

Based on the limited available information on SiC composites utilizing Hi-Nicalon Type-S fibers, the cladding in FRAPCON-SiC is assumed to swell with irradiation, exponentially approaching a saturation value of 2 v/o [Snead et al., 2003]. Based on this model, the swelling has reached 95% of its saturated value after 1 DPA. It is further assumed this swelling acts equally in the plane and through the thickness of the composite, which equates to a maximum of 0.67% linear strain due to irradiation. This linear strain is then given by the equation,

\[
\frac{\Delta L}{L} = 0.0067 \cdot (1 - e^{-\left(\frac{DPA}{3}\right)})
\]

(3.4)

While there may be a temperature dependence to this swelling affecting a transition to densification around 800 K, the functional form is not clear from this data.
3.6 SiC Stress and Strain Behavior

3.6.1 Young’s Modulus

The Young’s modulus describes a material’s response to stress and strain during elastic deformation due to tension or compression. This may be approximated by plotting stress versus strain for an isotropic sample and determining the slope during the elastic portion of the deformation, where the curve should be linear.

This behavior is somewhat more complex for a composite specimen, since even after matrix cracking the fibers can continue to hold some load before yielding or de-bonding entirely from the matrix. In addition, differences between composite types are introduced since the modulus of the fibers and matrix is different, and other factors such as the fractional fiber loading, weave pattern, and void volume are important.
Given the uncertainties over how to quantify the SiC composite structure and incorporate this information into estimates of the mechanical properties, an average value for the SiC composite Young’s modulus was implemented in FRAPCON-SiC. Based on the data shown in Figure 3.9, which shows a wide variability among even recently developed fibers, the Young’s modulus as a function of temperature was specified using a linear relation,

$$E(T) = -4 \times 10^7 \cdot T_{\text{clad}} + 1.62 \times 10^{11}$$  \hspace{1cm} (3.5)

where $T_{\text{clad}}$ is given in degrees Kelvin, and $E$ is in Pa, which is expected to be valid for composites around the typical cladding temperature of 600 K.

It is also expected that the modulus may decrease slightly with irradiation, and this can be seen in data for Nicalon-based composites in Figure 3.10. Using a conservative average for the behavior, it is assumed that the Young’s modulus decreases exponentially, and saturates at 40% of the un-irradiated value after 20 DPA. The relation based on this assumption gives,
\[ E(T, DPA) = E(T) \cdot \left( 1 - 0.4 \cdot \left( 1 - e^{-\frac{DPA}{20}} \right) \right) \] (3.6)

It is not clear from the data how composites based on newer fibers (such as Hi-Nicalon Type-S) will respond to irradiation. In general, it is expected there will still be some degradation of the modulus, however, and therefore this model should provide a conservative estimate of that effect [Snead et al., 1995].

![Graph showing Young's Modulus of SiC Composites as a Function of DPA.](image)

**Figure 3.10 Young’s Modulus of SiC Composites as a Function of DPA.**

### 3.6.2 Yield Strength

The yield strength of a material is the stress at which it transitions from elastic to plastic deformation, and therefore represents a maximum on a stress-strain curve. SiC in general has a higher yield strength than Zircaloy. This is one of the major advantages of SiC cladding, since it can better resist the significant internal and external stresses imposed in the LWR environment.

The yield strength model developed for FRAPCON-SiC depends on both temperature and fluence. The yield strength of several SiC composites versus temperature is plotted in
Figure 3.11, where it can be seen that the performance of composites with new fibers (such as Hi-Nicalon) compares similarly to older composites.

![Graph showing yield strength of SiC composites versus temperature.](image)

**Figure 3.11 Yield Strength of SiC Composites versus Temperature.**

Based on this information, the yield strength for the cladding in FRAPCON-SiC was modeled as,

\[
S_Y(T) = 2.66 \times 10^4 \cdot T_{\text{clad}} + 2 \times 10^8 \quad (3.7)
\]

with \( T_{\text{clad}} \) given in degrees Kelvin, and \( S_Y \) in Pa. Because brittle materials such as SiC experience little plastic deformation before fracturing, the yield strength is very close to the ultimate strength. For simplicity, in the FRAPCON-SiC model the cladding ultimate strength is defined as equal to the yield strength. In addition, strains at yield and failure are defined using Young’s modulus and the yield and ultimate strengths, respectively.

The fluence dependence of the yield strength was determined using data shown in Figure 3.12, which suggests a strong fluence dependence saturating after 20 DPA. The decrease in strength is around 40% for the composites with Nicalon fibers. The effect is even more
pronounced in the newer Hi-Nicalon fibers, although the data for these composites only extend to 7 DPA. Additional data from Hinoki suggests that the 40% degradation may hold for Hi-Nicalon and Tyranno SA based composites as well, at least up to 7.7 DPA [Hinoki et al., 2002]. However, he also presents data that shows for an even higher grade fiber, Hi-Nicalon Type-S, the effect of irradiation is a possible 5% increase in tensile strength.

Given the majority of the data supporting a decrease in strength with fluence with commonly used fibers, it is conservative to include this behavior in the model. The FRAPCON-SiC model therefore incorporates an exponential function that saturates at a 40% reduction in the temperature-dependent ultimate tensile and strengths,

$$S_Y(T, DPA) = S_Y(T) \left(1 - 0.4 \cdot \left(1 - e^{-\frac{DPA}{20}}\right)\right)$$

(3.8)

Figure 3.12 SiC Composite Ultimate Tensile Strength as a Function of DPA.
3.6.3 Shear Modulus

The shear modulus, which describes a material’s response to shear stress, can be defined as a function of the Young’s modulus and Poisson’s ratio for that material. Poisson’s ratio describes the strain behavior of the material perpendicular to the direction of strain. The Poisson’s ratio used for SiC cladding is 0.18, which indicates only a relatively small amount of transverse strain given a stress loading [Shackelford, 1994]. From this information the shear modulus is calculated as,

\[
G = \frac{E}{2(1+v)}
\]  

where \( G \) is the shear modulus, \( E \) the Young’s modulus, and \( v \) is Poisson’s ratio.

3.7 Other SiC Properties

3.7.1 Creep

The creep rate of a material is the time rate of change of strain given an applied stress. The deformation due to creep generally increases with temperature and time, and will lead to the fracture of the material. Creep of Zircaloy at PWR operating temperatures and pressures is significant (around 0.001% per hour) and must be taken into account. SiC, however, experiences negligible creep below 1000 K [Zimmerman and Adams, 1989 and Brook, 1991]. Based on this data, and the fact that maximum cladding temperature is well below 1000 K in current LWRs, the creep rate in FRAPCON-SiC was set to zero.

3.7.2 Oxidation

There is limited information available on the corrosion of SiC in water at LWR temperatures, pressures, and flow rates, and none for composites like those now being considered for fuel rod cladding. Additional considerations that may affect the corrosion rate are the effects of common chemical additives such as boric acid and lithium, the presence of a significant heat flux through the composite, as well as structural changes due to dimensional changes with temperature and irradiation.
It is also not known whether exposure to the coolant will result in the formation of a protective oxide layer on the surface of the SiC cladding, as occurs with Zircaloy, which may lead to weight gain and reduced thermal conductivity. Conversely, there may be a corrosive effect that results in a weight loss, thinning of the cladding or increase in porosity.

The data that is available for monolithic SiC in water around 600 K suggests a rate of weight loss around $1 \times 10^{-5}$ g/cm$^2$-hr, which is two orders of magnitude lower that that of Zircaloy; however, this is for tests of only a few weeks duration. In light of the available information, the oxidation model in FRAPCON-SiC was set to a zero oxidation rate, however this should be investigated further as the mechanism for SiC composite oxidation becomes better understood.

3.7.3 Emissivity

Emissivity is a measure of the fraction of thermal radiation emitted by the surface of a body relative to a blackbody. The closer to unity, the better the surface is at emitting thermal radiation, although it is also better at absorbing thermal radiation. From the available data on composites, a constant value of the cladding emissivity, 0.8, was obtained [Jones et al., 1997]. Although the emissivity of a surface can be changed through application of different surface coatings and by altering the roughness, this emissivity value compares well with the values for monolithic SiC, which fall between 0.8 and 0.95 [CINDAS, 2005].

3.7.4 Phase Transitions

SiC predominantly exists in two crystalline structures, α- and β-phase. It is usually found in the β-phase below 2300 K, and high-purity SiC fibers and matrix deposits will be of this structure [Schwetz, 2000]. Phase change of SiC is not modeled in FRAPCON-SiC.
Formally, SiC does not melt, but it will begin to decompose around 2600 K at atmospheric pressure. However, this is well above the temperature range of interest for LWR cladding. This high decomposition point is one of the major advantages of SiC cladding, especially with respect to transient and accident survival.

### 3.7.5 Crud Accumulation

When the fuel rod is installed in the reactor and there is flow through the core, any corrosion products suspended in the water will begin to plate onto the surface of the cladding. This accumulation is accounted for in FRAPCON as either a constant or a growing crud layer on top of any oxide layer. It will likely be necessary to determine experimentally the composite cladding's affinity for accumulating these deposits based on material and surface texture. For this investigation, the rate of crud accumulation in FRAPCON-SiC, and its thermal resistance, is equal to that of Zircaloy cladding.
4. Predicted Behavior of SiC Clad PWR Fuel Rods

4.1 FRAPCON Test Cases

For analysis of the FRAPCON-SiC computer code, two characteristic input cases were selected: first, a case at constant power to normal burnup, and second the very high burnup Manzel test case described in Chapter 2. The constant power case removes the disruptions due to sudden power level changes and the extreme effects of high burnup and large fission gas release. The Manzel case, on the other hand, represents a more realistic case for an operating reactor without the variations accompanying actual power histories. It also provides insight into the fuel rod behavior at high burnup where the SiC duplex cladding is needed in order to exceed the performance of Zircaloy.

4.2 SiC Model Evaluation

4.2.1 Constant Power Analysis

Using the input parameters from the Manzel test case, as outlined in Chapter 2 and Table 2.1, the average linear heat generation rate was set to a constant 5 kW/ft for 1500 EFPD, as shown in Figure 4.1 for the Zircaloy-clad (Zr) and SiC composite-clad (SiC) solid fuel rods. From this input information, FRAPCON generated a discharge burnup of 40 MWd/kgU, shown in Figure 4.2.
The first major difference that is apparent between the Zircaloy and SiC cladding is the thermal conductivity, graphed in Figure 4.3. It is interesting to note that based on the hot full-power temperature and limited irradiation damage over the first time step (0.1 EFPD), the thermal conductivity of the SiC has already significantly degraded from its room-temperature value of 20 W/m-K, and is about 4 W/m-K, less than the value of the Zircaloy cladding by a factor of 2.5 to 3.0.
The SiC thermal conductivity then saturates, due to radiation damage, by the second time step (65 EFPD) near 3.6 W/m-K, while the Zircaloy value remains basically unchanged. Even at discharge, the SiC thermal conductivity is less than half of that predicted for the Zircaloy, which has both crud and oxide layer buildup. Note that the saturation value used in the SiC thermal conductivity model is 4 W/m-K, however the presence of crud on the exterior of the cladding further reduces this value. In both the constant power and Manzel test cases, it is assumed that there is a constant crud layer 0.2 mils thick for SiC and Zircaloy.

In Figure 4.3, as well as all following figures showing fuel rod temperatures and strains, the property graphed is for the FRAPCON spatial node that registered the peak axial temperature. Because of the that power shape and fuel rod behavior, this is always the same axial location, seven feet above the base of the active fuel region, just above the fuel rod centerline. Therefore, this data should reflect the point of most extreme change in the fuel rod over time.

The SiC cladding thermal conductivity does not decrease over time because in this model there is no effect of oxidation on the SiC outer surface. The effect of this lower thermal
conductivity is to increase the temperature drop across the cladding, and thereby increase the average cladding and fuel temperature, as shown by Figure 4.4 and Figure 4.5. The SiC inner cladding temperature is higher by about 60 K over the life of the fuel rod, while the fuel is consistently about 200 K hotter.

Figure 4.4 Cladding Temperature at the Cladding Outer Diameter (OD) and Cladding Inner Diameter (ID) for the Constant Power Test Case.

Figure 4.5 Fuel Temperature at the Fuel Outer Diameter (OD) and Fuel Centerline for the Constant Power Test Case.
The Zr-clad rod benefits from the lower thermal conductivity as well as the creep-down of the cladding onto the fuel. This has the effect of closing the fuel-cladding gap and decreasing the thermal resistance through the fuel rod. The fuel-cladding gap width, plotted in Figure 4.6, increases initially as the fuel density increases and the cladding swells in the first months after reactor startup. Very quickly, however, the Zircaloy cladding begins to creep down onto the fuel at a significant rate, achieving soft gap closure around 26 MWd/kgU, but no hard fuel-cladding contact before discharge. The SiC cladding experiences no creep down; instead, the gap closes as the fuel pellets swell outwards due to irradiation swelling and fission gas buildup.

![Figure 4.6 Fuel-Cladding Radial Gap Width for the Constant Power Test Case.](image-url)
The fission gas release predicted by FRAPCON and FRAPCON-SiC is identical for this constant power case, as shown in Figure 4.7. The total gas release fraction is also very low in both cases, below 0.3% at discharge. The similarity in results is due to the low power level, fuel temperatures, and burnup achieved, such that neither radial saturation nor high-burnup threshold was reached in either case.

These results demonstrate the primary differences in the behavior of traditional Zircaloy and the new SiC cladding at low power levels: the SiC exhibits lower thermal conductivity after irradiation and does not experience creep. Other changes, such as swelling and oxidation also have a noticeable effect. In this irradiation scenario, the higher fuel temperature would not pose a problem during normal operation, but may decrease the fuel safety margins for accident analysis.

### 4.2.2 High Burnup Analysis

The constant power test case gives a metric of the performance of the new SiC cladding models, however it does not test the cladding in the regime where it is favored over Zircaloy: at very high burnup. A second analysis of the new FRACPON-SiC was therefore performed using the Manzel test case input as described in Chapter 2.
The power history, Figure 4.8, and burnup, Figure 4.9, for this case are the same for both the Zircaloy and SiC fuel rods, as expected. The discharge burnup is 102 MWd/kgU after 3240 EFPD for both. This case represents very high burnup compared to the average assembly discharge burnup of 40 MWd/kgU analyzed earlier, or the 50 MWd/kgU achieved today in most U.S. PWRs.

Figure 4.8 Average LHGR for Zircaloy (Zr) and SiC Composite (SiC) Clad Fuel Rods for Manzel Test Case.

Figure 4.9 Average Burnup for Manzel Test Case.
The cladding thermal conductivities behave as expected in both cases, and they are in part continuations of the behavior calculated during the constant power test case. Both conductivities, shown in Figure 4.10, decrease with burnup and approach a stable value. The SiC cladding decreases primarily due to radiation damage, although there is also an effect of crud. The Zircaloy cladding has a slower change dictated primarily by the rate of formation of an oxide layer on the outer cladding surface. After about 70 MWd/kgU, this layer has grown to its maximum thickness and begins to spall away, leaving the minimum Zr cladding conductivity still 150% greater than the SiC saturation value.

![Figure 4.10 Cladding Thermal Conductivity for Manzel Test Case.](image)

The effect of this difference in thermal conductivity is again aggravated by the lack of creep in SiC, which could serve to close the fuel-cladding gap quickly, as shown in Figure 4.11. The Zr-clad fuel rod achieves soft closure around 15 MWd/kgU, and hard fuel-cladding contact after 20 MWd/kgU. The SiC cladding gap is governed by the fuel pellets swelling outward so that the gap does not close until almost 70 MWd/kgU, and there is no hard fuel-cladding contact.

This difference in cladding behavior is highlighted in the change of the cladding inner diameter, shown in Figure 4.12. After initial swelling, the Zircaloy cladding creeps down quickly onto the fuel given the large external pressure of the coolant. The SiC cladding,
conversely, swells outward due to radiation damage up to its saturation value. It is also notable that the effect of changing power level, and therefore cladding temperature, is not as apparent in the SiC cladding as it is for Zircaloy. This difference highlights both the dominance of the radiation swelling as well as the small thermal expansion of SiC.

![Graph showing radial fuel-cladding gap width for Manzel test case.](image)

**Figure 4.11 Radial Fuel-Cladding Gap Width for Manzel Test Case.**

![Graph showing cladding inner diameter for Manzel test case.](image)

**Figure 4.12 Cladding Inner Diameter for Manzel Test Case.**

The peak cladding and fuel temperatures, plotted in Figure 4.13 and Figure 4.14, reflect these differences in thermal conductivity and gap closure rate. As the SiC fuel-cladding gap widens initially, the cladding inside diameter temperature quickly increases, and only
slowly approaches the Zircaloy as the gap closes. However, even when both gaps are closed the difference in temperatures between the two claddings is almost 40 K.

The fuel temperatures are even more widely divergent, with a 400 K difference in centerline temperatures due to the closing Zr-clad gap and widening SiC-clad gap. The temperature of the SiC-clad fuel gradually approaches that of the Zr-clad rod as the gap closes around 70 MWd/kgU. The effect of the SiC-clad rod gap conductance and cladding thermal conductivity also drive the difference in behavior of the pellet temperature profile. The Zr-clad pellet has a nearly constant outside temperature over its lifetime, whereas the SiC-clad pellet is up to 300 K hotter.

![Figure 4.13 Cladding Inner and Outer Diameter Temperatures for Manzel Test Case.](image)
Figure 4.14 Fuel Temperature for Zircaloy and SiC fuel rods at Fuel Outer Diameter and Centerline for Manzel Test Case.

Figure 4.15 Radial Fuel-Cladding Gap Thermal Conductance for Manzel Test Case.

The conductance of the fuel-cladding gap, plotted in Figure 4.15, is being driven by two factors: the radial width of the gap, and the conductivity of the fill gas. The effect of the gap width is apparent, with a dramatic rise in conductance by several orders of magnitude in both cases as the gap closes.
The thermal conductivity of the gap itself is primarily affected by the buildup of fission gases, which tend to increase the thermal resistance over the initial state of pure helium. The higher average fuel temperatures in the SiC-clad fuel rod increase the fission gas release fraction, shown in Figure 4.16, and therefore serve to further decrease the gap conductance.

![Figure 4.16 Average Fractional Fission Gas Release for the Manzel Test Case.](image)

The much higher fuel temperatures in the SiC-clad fuel rod have dramatically increased the fission gas release after only 5 MWd/kgU of burnup. The release reaches 20% by 35 MWd/kgU, while the predicted release fraction in the Zircaloy rod at the same time is on order of 1%. This release has the effect of holding down the gap conductance even as the SiC-clad gap is closing, so that a significant increase in the gap conductance is not realized until just before full closure.

This high fraction of release creates other issues, primarily pressurization of the fuel rod, as shown in Figure 4.17. Although the SiC-clad rod’s release fraction peaks around 20 MWd/kgU, there is not much pressure increase since the total inventory of fission gas in the fuel at that time is small. However, by 30 MWd/kg the release fraction is still significant and the fuel-cladding gap has decreased in the radial direction by almost half,
causing a quick increase in pressure such that beyond 30 MWd/kgU the internal pressure exceeds the external primary coolant pressure of 15.2 MPa. The higher fuel temperatures and previous gas release contributes to increasing the pressure difference between the Zr-clad and SiC-clad fuel rods even more after the gap is closed at 70 MWd/kgU.

![Figure 4.17 Fuel Rod Free Gas Pressure for Manzel Test Case.](image)

The Manzel test case demonstrates the interaction of the SiC cladding properties in a high heat flux and high burnup scenario. The low thermal conductivity and lack of creep down result in higher fuel temperatures and fission gas release than Zircaloy rods under the same conditions. The behavior becomes similar after closure of the fuel-cladding gap, but this does not occur until very high burnup, around 70 MWd/kgU.

A significant result of the differences in cladding performance is the higher internal pressures in the SiC-clad fuel rod, especially at high burnup. Based on the yield strength model discussed in Chapter 3, the code predicts that the SiC cladding will fail due to hoop stress around 95 MWd/kgU, when the internal pressure is about 22 MPa higher than the external coolant pressure of 15 MPa. The failure of the Zircaloy is not predicted until the net internal pressure reaches 60 MPa.
4.3 Sensitivity Analysis

Because the new FRAPCON models governing SiC cladding behavior rely on experimental data generated under specific conditions, it is prudent to consider how small differences in the empirical functions that were developed impact the predicted fuel rod behavior. This information is important both for gauging the precision of the FRAPCON-SiC code as well as identifying those properties that require increased scrutiny.

In order to obtain a better understanding of these uncertainties, a basic sensitivity analysis was performed in which the forms of certain relations were altered to represent the variations in the available data. The four properties that were chosen—thermal conductivity, thermal expansion, irradiation swelling, and Young’s modulus—represent properties that showed a significant impact on fuel rod behavior in the constant power and Manzel test cases in the previous section. The model properties were varied to represent the minimum and maximum extremes of the available data.

4.3.1 Thermal Conductivity Variations

The thermal conductivity was associated with a wide range of possible values, as discussed in Chapter 3, although the general form of the temperature and irradiation dependence are approximately the same. To represent the largest possible extremes two cases were developed: one representing the highest likely value of the thermal conductivity, the other representing the lowest likely value. These two scenarios, representing a high and low range for the thermal conductivity are presented in Table 4.1.

<table>
<thead>
<tr>
<th>Condition</th>
<th>Selected Value</th>
<th>High Range</th>
<th>Low Range</th>
</tr>
</thead>
<tbody>
<tr>
<td>Room temperature conductivity</td>
<td>20 W/m-K</td>
<td>50 W/m-K</td>
<td>15 W/m-K</td>
</tr>
<tr>
<td>Irradiation saturation conductivity</td>
<td>4 W/m-K</td>
<td>10 W/m-K</td>
<td>2 W/m-K</td>
</tr>
<tr>
<td>Conductivity Saturation DPA</td>
<td>1</td>
<td>2</td>
<td>0.1</td>
</tr>
</tbody>
</table>

The results of these changes on the output thermal conductivity are presented in Figure 4.18, where it is apparent by comparing the final conductivity values in each case that the
crud layer has a significant effect on the effective cladding conductivity. For the low range case, this results in a 2 W/m-K decrease from its saturation value due to the 0.2 mils of crud, whereas the high range case more than doubles the default value.

![Figure 4.18 Thermal Conductivity Based on Different Thermal Conductivity Model Parameters.](image)

These changes in the thermal conductivity values have the expected effect on the temperature of the cladding, shown in Figure 4.19. The higher thermal conductivity range is a beneficial change, resulting in lower temperatures throughout the cladding. On the other hand, the low range conductivity model increases the cladding inner surface temperature by over 175 K. At these high temperatures, it is possible that behavior not included in the FRAPCON-SiC model, such as creep, may begin to be significant. In addition, this low conductivity generates a very large temperature gradient across the cladding, which may drive other undesirable processes such as chemical diffusion and cracking and increased thermal stresses.
Figure 4.19 Cladding Temperature Based on Different Thermal Conductivity Model Parameters.

The fuel temperature also changes in the expected direction for the two extreme models, although the change is slightly more extreme than that of the cladding, with at a 225 K increase in fuel centerline temperature for the low conductivity model. This higher pellet temperature has several effects, most notably an increase in fission gas release and an increase in the rate of swelling. The fission gas release, plotted in Figure 4.21, shows a significant change for both models. The high conductivity case decreases the release by about 1/3 up to 80 MWd/kgU, after which, in the very high burnup regime, it more closely approximates the observed and FRAPCON-predicted behavior of Zircaloy fuel rods.
The low conductivity model increases the fission gas release by about 50%. The absolute release is 25-30% for a majority of the operational life of the rod. This behavior results in a large increase in the fuel rod internal pressure, as shown in Figure 4.22. It is important to note the point where the rod internal pressure surpasses the external coolant pressure, thus moving the cladding from compression into tension. The low conductivity case surpasses this point early in life, at 20 MWd/kgU. The chosen model takes longer to cross
the threshold, around 30 MWd/kgU, and the high conductivity model shows the best performance, not crossing until past 60 MWd/kgU. In the high burnup regime, the pressure rises more dramatically for all rods, but they remain similar in relation to each other.

![Graph showing internal free gas pressure based on different thermal conductivity model parameters.](image)

**Figure 4.22 Internal Free Gas Pressure Based on Different Thermal Conductivity Model Parameters.**

An interesting result of the thermal conductivity change is the relative fuel-cladding gap conductance predicted by each code, shown in Figure 4.23. Although the fission gas release in the low conductivity model is much higher than for the other scenarios, the gap conductance is higher than either of the other models. In fact, the ranking of the gap thermal conductance below 70 MWd/kgU is the opposite of what would be expected based on fission gas release, and is instead driven by the gap thickness, shown in Figure 4.24.

The increasing fuel temperature with decreasing cladding conductivity drives the swelling of the fuel above the thermal expansion of the cladding, causing the gap to close fastest in the low conductivity model. Past 70 MWd/kgU, when all three models predict soft gap closure, the conductance is controlled by the fission gas release, and the order of conductance's reverse.
Figure 4.23 Radial Fuel-Cladding Gap Conductance Based on Different Thermal Conductivity Model Parameters.

Figure 4.24 Radial Fuel-Cladding Gap Width Based on Different Thermal Conductivity Model Parameters.
4.3.2 Thermal Expansion Variations

The thermal expansion model presented in Chapter 3 was based on data that suggested that the SiC linear thermal expansion coefficient had a value range between $2.5 \times 10^{-6}$ and $4.5 \times 10^{-6}$ 1/K, with very little variation with temperature over the range of interest. In addition, it was assumed that this expansion was equal in the radial and axial directions.

Although there is limited data for modern composites, it seems reasonable that they will exhibit properties within a factor of two of the chosen value for both the axial and radial dimensions. Given this assumption, the sampling range presented in Table 4.2 was developed.

Table 4.2 Thermal Expansion Sensitivity Conditions.

<table>
<thead>
<tr>
<th>Condition</th>
<th>Selected Value</th>
<th>High Range</th>
<th>Low Range</th>
</tr>
</thead>
<tbody>
<tr>
<td>Thermal Expansion Coefficient</td>
<td>$3 \times 10^{-6}$ 1/K</td>
<td>$6 \times 10^{-6}$ 1/K</td>
<td>$1.5 \times 10^{-6}$ 1/K</td>
</tr>
</tbody>
</table>

The results of varying this parameter are less dramatic than the thermal conductivity, since the radial strain induced by thermal expansion around 700 K is only 0.25% in the high range model. In addition, the temperature of the cladding does not fluctuate significantly during operation, so there is little dynamic effect of the thermal expansion. Some change is noticeable in the gap width, as shown in Figure 4.25. However, the difference between the cases of higher and lower thermal expansion is only several thousandths of a millimeter.
Because of the wider gap, the higher thermal expansion case has fuel centerline temperatures 10-20 K higher than for the selected model. This in turn increases the fission gas release by almost 5% as shown in Figure 4.26 between 30 and 80 MWd/kgU. The effect of this additional release is also not dramatic in terms of added internal fuel rod pressure, as it amounts to 1-2 MPa beyond that of the selected case.
4.3.3 Irradiation Swelling Variations

The data available for the SiC irradiation swelling model exhibited a large amount of scatter. The variance in measurements is at least partially attributable to the densification apparent in SiC fibers with significant impurities, such as free carbon, compared to the significant swelling of pure SiC crystals, such as the inter-fiber matrix. In addition, this swelling is also on the same order of magnitude as the thermal expansion (±1%).

The values for the swelling sensitivity analysis are given in Table 4.3. Hi-Nicalon fibers irradiated to 0.5 DPA showed some shrinkage [Ishihara et al., 2002], which is used to set a low range of -0.05% linear swelling in the radial direction and -0.8% axially. Conversely, an SiC composite with Hi-Nicalon Type-S fibers, irradiated to 1.1 DPA, exhibited over 0.7% linear swelling, which supports the idea of an upper range of 0.8% linear swelling at 1 DPA [Snead et al., 2003].

<table>
<thead>
<tr>
<th>Condition</th>
<th>Selected Value</th>
<th>High Range</th>
<th>Low Range</th>
</tr>
</thead>
<tbody>
<tr>
<td>Radial Swelling Limit</td>
<td>0.67%</td>
<td>0.8%</td>
<td>-0.05%</td>
</tr>
<tr>
<td>Axial Swelling Limit</td>
<td>0.67%</td>
<td>0.8%</td>
<td>-0.8%</td>
</tr>
<tr>
<td>Saturation DPA</td>
<td>1</td>
<td>1.5</td>
<td>0.5</td>
</tr>
</tbody>
</table>

The selected and high range swelling models have similar behavior to the thermal expansion variations; the increased swelling increases the radial fuel-cladding gap width and increases the time needed to close the gap, as shown in Figure 4.27. The effect of the low range model, which proposes densification instead of swelling, is more pronounced. The gap begins closing immediately, unlike the behavior seen previously, and closes by 30 MWd/kgU, at almost 1/3 the burnup of the other cases.
From Figure 4.28 it can be seen that the quick closing of the gap in the low range model greatly increases the gap conductance, and as a result, the fuel temperature for this case is around 200 K lower than the other two cases until they also begin to close around 60 MWd/kgU. The difference between the selected and high cases is smaller, around 30 K for the 0.13% difference in strain. At very high burnup, above 75 MWd/kgU, all three models predict gap closure and the fuel temperature profiles are nearly identical.

The most significant result of the lower fuel temperature in the low range model is a decrease in the predicted fission gas release to about 1/3 the value of the standard model. This low release, shown in Figure 4.29, is a result of the lower fuel temperature for this case, and it in turn helps to keep the fuel temperature lower since the conductance of the gap is not lowered as much by fission gas buildup. The other two cases have a release fraction around 20% for most of their life, with the higher swelling case showing about 5% higher release than the standard case.
Although there is a large difference in the release fraction, none of the three cases examined caused a large change in fuel rod internal pressure; the low swelling case is about 3 MPa less than the standard case, and the high swelling case about 1 MPa higher during the majority of the rod life. At very high burnup, above 80 MWd/kgU, when the high burnup fission gas release becomes significant, the fission gas release and plenum
pressures increase for all three cases and approach the same value, since this release is
dependent on burnup and not fuel temperature.

### 4.3.4 Young's Modulus Variations

The variation in Young's modulus from the data on SiC composites in Chapter 3 gives
values between 30 and 300 GPa at room temperature, depending on the construction
[Jones et al., 1997 and Nogami et al., 2004]. In addition, the neutron fluence dependence
varies, with reported degradation up to 60% by 26 DPA [Hollenberg et al., 1995], but
with much less change predicted for composites incorporating newer fiber types.
Therefore the models, given in Table 4.4, select these extreme cases in order to elicit the
widest range of likely stress-strain behavior. Since the temperature dependence varies
only slightly in the available cases, the current linear assumption of -40 MPa/K between
300 and 1000 K is maintained in all three models.

#### Table 4.4 Young's Modulus Sensitivity Conditions.

<table>
<thead>
<tr>
<th>Condition</th>
<th>Selected Value</th>
<th>High Range</th>
<th>Low Range</th>
</tr>
</thead>
<tbody>
<tr>
<td>Room Temperature Modulus</td>
<td>150 GPa</td>
<td>300 GPa</td>
<td>30 GPa</td>
</tr>
<tr>
<td>Fluence Dependence</td>
<td>-40% at 20 DPA</td>
<td>None</td>
<td>-60% at 20 DPA</td>
</tr>
</tbody>
</table>

The change in Young's modulus has the anticipated effect of changing the strain of the
cladding at various points during operation, as shown in Figure 4.30. For the models
using the selected value and the high range value of the modulus there is very little
change, but the differences are more apparent for the low range model. The lower
modulus allows the external pressure to have a larger compressive effect on the cladding,
reducing the diameter after the effects of swelling and thermal expansion are considered.

At 30 MWd/kgU, the rod internal pressure equals the external pressure, and all three
cases approach the same value. As rod internal pressure continues to increase due to
fission gas release, the low range modulus case again shows this pressure effect most
clearly. It increases rapidly in the very high fission gas release region, and the calculation
becomes numerically unstable past 95 MWd/kgU. The other two cases show a small
increase in hoop strain, but do not dramatically increase until acted on by fuel swelling at 95 MWd/kgU.

The impact of these three modulus values on gap width is small. As shown in Figure 4.31, at normal burnup they differ by only one thousandths of a millimeter at the widest point, and the values begin to converge at higher burnup. This small change in gap width has no appreciable affect on the predicted fuel pellet temperature or the rod internal pressure, and therefore does not change the predicted fission gas release either. At very high burnup, the low range model predicts cladding lift-off from the fuel due to the rapid increase in internal pressure over the external coolant pressure. Again, the results become numerically unstable as the cladding expands; however, by that point the large strain would be unacceptable performance in a real reactor.

Figure 4.30 Cladding Inner Diameter for Different Young’s Modulus Models.
4.4 Investigation of Possibilities for Performance Enhancement

Based on the results obtained in the constant power and Manzel test case, there are several areas of SiC-clad fuel rod behavior that indicate a possible need for design review. In particular, it would be beneficial to the performance of SiC-clad fuel rods both in normal and very high burnup scenarios to mitigate the low cladding thermal conductivity and low gap conductance.

The result of the cladding’s poor thermal performance is a higher average fuel temperature, which increases both the rate of fission gas release and pellet swelling. It is desirable to reduce the fission gas release and pellet swelling because they put additional mechanical stress on the cladding, and may accelerate chemical interactions. There are several avenues for addressing these issues, including changing the initial fill gas pressure and using annular fuel pellets, which are discussed below.

4.4.1 Initial Fill Gas Pressure

In a Zircaloy fuel rod the initial fill gas is required to prevent the cladding from collapsing under the coolant pressure, and reduces the likelihood of fuel-cladding
mechanical interaction during the majority of the fuel rod’s life. In addition, helium is used as the fill gas since it has a high thermal conductivity and low neutron capture cross section.

Because of the high strength of the SiC cladding, large Young’s modulus, and lack of creep, it is possible to change the initial fill gas pressure significantly without compromising the initial structural stability of the fuel rod. With this ability in mind, two scenarios were examined: reducing the initial fill gas pressure in order to prevent overpressurization, and increasing the initial fill gas pressure in order to improve the gap conductance.

The fill gas pressure used in the Manzel test case is 3.3 MPa as stipulated by the Oconee test case supplied by PNNL. For a low pressure test, one tenth of this value was used, which is about three times atmospheric pressure. For the high pressure case, it was decided to have an initial pressure that would produce at most one-half of the cladding failure hoop stress when the rod external surface is at atmospheric pressure (as a safety factor). The cladding, with internal diameter of 0.958 cm and thickness of 0.673 mm, may be treated as a thin-walled pressure vessel. The hoop stress is then given as,

\[ \sigma_h = \frac{P \cdot r}{t} \]  (4.1)

where \( P \) is the net pressure, \( r \) is the cladding radius, and \( t \) is the cladding thickness. Based on the model used for FRAPCON-SiC, the minimum ultimate tensile strength of the cladding is 87 MPa after reaching radiation-induced saturation at 700 K; the desired initial He fill pressure is then 6 MPa. The reactor coolant pressure is maintained at 15.2 MPa for all three cases.

<table>
<thead>
<tr>
<th>Table 4.5 Initial Fill Gas Pressure Options.</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Selected Value</strong></td>
</tr>
<tr>
<td>Cold, zero power He fill gas pressure</td>
</tr>
</tbody>
</table>

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The fuel rod internal pressure histories resulting from these choices of initial fill gas pressure, which are outlined in Table 4.5, are shown in Figure 4.32. At the beginning of life there is a large increase in the internal pressure as the rod goes from cold, zero power to hot, full power. The resulting initial “hot” pressure is 19 MPa for the high range case, 10 MPa for the standard case, and 1 MPa for the low range case. Two interesting trends are apparent in this plot: the high pressure case mirrors the standard case shifted by the difference in initial “hot” pressure all the way to very high burnup. On the other hand, the low case quickly gains pressure and actually surpasses the standard case around 35 MWd/kgU.

![Figure 4.32 Internal Free Gas Pressure for Different Initial Fill Gas Pressures.](image)

The reason for the large difference in pressure is the fission gas release, which is driven by fuel temperature. The fuel temperature for each case as a function of burnup is shown in Figure 4.33, and clearly identifies the undesirable effects of the low fill pressure case as the fuel temperatures peaks 400 K higher than the standard case. The difference in temperature is due to the low gap conductance in the low pressure rod, shown in Figure 4.34, which is caused by the low initial fill pressure and exacerbated by a faster dilution by fission gasses. Similar to the other two cases, the conductance improves past 70 MWd/kgU as the gap closes.
The higher initial fill gas pressure, while not producing as dramatic effect as the low pressure case, does bring measurable improvement to the fuel rod’s performance. The fuel pellet temperatures are reduced up to 50 K from 10 to 50 MWd/kgU, and in general, the gap conductance is higher than the standard case. The lower fuel temperature lowers the fission gas release in Figure 4.35 by 5 to 10% during most of operation; at very high burnup the release figures converge.
It should be noted that while the higher pressure did result in lower fuel temperature, the absolute internal rod pressure was significantly higher than the standard case for the length of operation. With the higher internal pressure, the SiC failure stress is achieved at 85 MWd/kgU, about 10 MWd/kgU earlier than the standard case. In addition, the time of gap closure (and therefore fuel-cladding mechanical interaction) given by the rise in conductance in Figure 4.34 was not significantly delayed from the standard case. One can conclude that the selected fill gas pressure shows the best overall performance. This reflects a proper selection of the initial fill gas pressure by the manufacturer.

![Figure 4.35 Fractional Fission Gas Release for Different Initial Fill Gas Pressures.](image)

### 4.4.2 Annular Pellets

As discussed in Chapter 1, annular fuel pellets have been explored as a way to reduce the peak fuel temperature. The central void can reduce the peak pellet temperature, which then occurs at the pellet inner diameter, although it removes fuel volume from the core. For this analysis, pellets with inner void volumes of 5 and 10% of total pellet volume were used, as detailed in Table 4.6. These void volumes are representative of past experiments based on data from the High Burn-Up Effects Program and Russian reports of VVER fuel performance [Turnbull, 2002 and Bibilashvili et al., 1994].
Table 4.6 Fuel Pellet Central Void Dimensions.

<table>
<thead>
<tr>
<th>Pellet type</th>
<th>Solid Pellet</th>
<th>5% Void</th>
<th>10% Void</th>
</tr>
</thead>
<tbody>
<tr>
<td>Fuel outer diameter (cm)</td>
<td>0.932</td>
<td>0.932</td>
<td>0.932</td>
</tr>
<tr>
<td>Fuel inner diameter (cm)</td>
<td>-</td>
<td>0.2085</td>
<td>0.2949</td>
</tr>
</tbody>
</table>

The addition of the void space at the center of the pellet reduces peak fuel temperature (at the pellet inner diameter), as shown in Figure 4.36 for both of the annular pellet cases. The 5% pellet central void volume decreases the peak temperature by almost 200 K at the beginning of life, with the temperature remaining below that of the standard solid pellet until discharge. Doubling the void volume adds a further temperature reduction, although only about 50% beyond the reduction achieved by the 5% void volume.

![Figure 4.36 Fuel Temperature for Solid and Annular Fuel Pellets.](image)

The pellet surface temperatures are up to 100 K higher for the annular pellets versus the solid, especially around 70 MWd/kgU. The larger difference at this point is mostly because the radial fuel-cladding gap closes earlier for the solid pellet, since it experiences more swelling due to the higher centerline temperature. As shown in Figure 4.37, the consequence of the larger void is that the gap remains open longer, not achieving soft closure until the end of life for the 10% void volume case.
Besides reducing the pellet swelling, the second intention of reducing the peak fuel temperature is to reduce the fission gas release. From Figure 4.38 it is apparent that this goal is achieved below 50 MWd/kgU for both void volumes, and extended up to 65 MWd/kgU for the 10 volume percent case. However, beyond that point the fission gas release exceeds the solid pellet by about 5% for the 5 volume percent pellet, and about 10% for the 10 volume percent pellet. This increase is attributable to the fact that the gap remains open for the annular pellets, while it has closed for the solid pellet. Therefore, although the peak fuel temperature is reduced, the average fuel temperature (and especially the temperature at the pellet rim) will be somewhat higher for the annular pellets.
Although the fission gas release is increased over the solid pellet in the second half of life, the fuel rods with annular pellets still maintain a comparable or lower internal pressure until discharge, as shown in Figure 4.39. This benefit can be attributed to the lower fission gas release in the first half of life and the increase in available void volume in the fuel rods with annular pellets. The total free gas void volume in the fuel rod with 5 volume percent pellets is about 50% greater than the fuel rod with solid pellets; the fuel
rod with 10 volume percent pellet voids has 100% more void space than the fuel rod with solid pellets.

4.5 MOX Fuel

Because there is interest in utilizing light water reactors to burn excess plutonium, either from military stockpiles or extracted from spent commercial nuclear fuel, the combination of MOX fuel with SiC was investigated. MOX fuel combines UO$_2$, generally depleted in U-235, and PuO$_2$ in a single fuel form. The plutonium loading is generally on the same order of the U-235 loading in a typical LWR fuel rod.

A typical MOX fuel loading was supplied by PNNL, and is presented in Table 4.7. The fuel for this case is 5.95 percent plutonium by weight. This plutonium isotopic vector represents typical MOX fuel fabricated from excess reactor-grade plutonium [Lanning et al., 2005]. Besides the isotopic loading, the pellet density, geometry and reactor conditions are the same as the Manzel test case used previously.

<table>
<thead>
<tr>
<th>Isotope</th>
<th>Initial Uranium Atom %</th>
<th>Initial Plutonium Atom %</th>
</tr>
</thead>
<tbody>
<tr>
<td>U-235</td>
<td>0.229</td>
<td>-</td>
</tr>
<tr>
<td>U-238</td>
<td>99.771</td>
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<tr>
<td>Pu-239</td>
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<tr>
<td>Pu-242</td>
<td>-</td>
<td>3.33</td>
</tr>
</tbody>
</table>

This analysis compared the behavior of the typical UO$_2$-fueled Zircaloy-clad rod examined in Chapter 2 with that of both a MOX-fueled Zircaloy-clad rod and the MOX-fueled SiC-clad rod based on the FRAPCON-SiC code. In this way, both the effect of the new fuel form and the new cladding can be evaluated. The modifications to the very high burnup fission gas release model discussed in Chapter 2 were used for MOX fuel modeling in part because better data was not available, and also because fission gas
release mechanisms are thought to be similar in urania and plutonia fuel. The FRAPCON Massih fission gas release model does take into account the increased rate of gas diffusion in MOX fuel.

Concerning fuel rod thermo-mechanical behavior, the major difference between UO₂ and MOX is the lower thermal conductivity of the fuel. This raises the fuel temperature, as shown by Figure 4.40, where the MOX fuel with Zircaloy cladding is 100-200 K hotter at pellet centerline than the UO₂-fuelled pin. This difference is obscured at the beginning of life, since the MOX fuel experiences more swelling and therefore closes the fuel cladding gap before this occurs with UO₂ pellets. However, by 10 MWd/kgU, when both Zircaloy fuel-cladding gaps are closed, the temperature difference due to fuel thermal conductivity is apparent. This distinction remains until the end of life, however the difference decreases as the pellet heat generation rate decreases.

![Figure 4.40 Fuel Temperature for Zircaloy-Clad Fuel Rods with UO₂ and MOX Fuel, and SiC-Clad Fuel Rods with MOX Fuel.](image)

The MOX fuel with SiC cladding experiences high pellet temperatures because of the lower thermal conductivity of the cladding and because the fuel-cladding gap does not close until there has been sufficient pellet swelling around 50 MWd/kgU. The results of these fuel temperatures on the relative fission gas release fraction follows as expected. From Figure 4.41 it can be seen that the successively higher fuel temperatures in the
MOX and SiC fuel rods leads to much higher fission gas release early in life, especially for the SiC-clad fuel rod.

This release leads to higher internal pressures, shown in Figure 4.42, where it can be seen that the SiC-clad fuel rod exceeds the primary coolant pressure after 20 MWd/kgU, whereas the Zircaloy-clad rods take 60-80 MWd/kgU to reach this point. Besides the increase in fission gas release in the SiC-clad fuel rod, this difference in pressure increase is also due to the ability of the Zircaloy cladding to expand outward due to increasing internal pressure more readily than the SiC. However, in all three cases the rod pressure begins to increase significantly once reaching the very high burnup fission gas release regime.

![Figure 4.41 Fractional Fission Gas Release from UO₂ and MOX Fuel Pins with Zr and SiC Cladding.](image-url)
4.6 Summary of SiC Model Performance

The modeling of UO₂ fueled rods with SiC cladding has demonstrated both the advantages and challenges of implementing a ceramic cladding. In general, the SiC cladding showed good tolerance to the temperatures and pressures that will exist up to extremely high burnup – 100 MWd/kgU. The models that have been developed show that the properties of SiC composites are stable after initial radiation interaction up to very high DPA. The parameters that merit particular refinement are the thermal conductivity and irradiation swelling, as variations within the available data produced large changes in critical metrics such as peak fuel temperature and fission gas release. It is also important that the Young’s modulus be sufficiently high, at least on the order of the conservative estimate used in FRAPCON-SiC, to avoid excessive strain at very high burnup.

The modeling also identified the cladding characteristics that have the greatest impact on behavior when compared to typical Zircaloy fuel rods. The lower thermal conductivity of SiC in comparison to Zircaloy increases the average cladding temperature and increases the temperature gradient across the cladding, as well as increasing the fuel pellet average temperature. Because the SiC does not experience creep at typical cladding temperatures,
it demonstrates an excellent ability to maintain geometry despite widely varying internal pressure and fuel swelling. However, because SiC does not creep down onto the fuel as occurs in a Zircaloy rod, the gap conductance over the operational life is generally lower, contributing to higher fuel temperatures. The result of these higher pellet temperatures is increased fission gas release and therefore increased pressurization of the fuel rod.

These problems could be mitigated through several means, and two were investigated here: changing the initial fill gas pressure, or using annular fuel pellets. Increasing the fill pressure does improve gap conductance and decrease the average fuel temperature, reducing fission gas release. However, the increase in initial cold pressure may reduce the margin to rod failure due to internal stress more than it is gained by lower fission gas release.

Annular pellets are very effective at reducing peak fuel pellet temperatures, which results in a significant reduction in fission gas release up to burnup around 60 MWd/kgU. Once reaching the very high burnup regime, however, the higher rim temperature tends to cause more fission gas release than a solid pellet at that burnup. However, there is a benefit from the reduced fuel rod internal pressurization, which could make this an advantageous design.

The use of MOX fuel with SiC cladding was also investigated in comparison to MOX used with Zircaloy cladding. In general, the behavior of the MOX and UO₂ fuel is equivalent, with the SiC-clad MOX fuel rod running at a higher average temperature than the Zircaloy-clad fuel rod. In particular, the time for fuel-cladding gap closure and the fission gas release were both greater in the SiC-clad fuel rod. It is possible that the same ideas explored to reduce temperature in the UO₂ fuel, such as adjusting the fill gas or using annular pellets, be beneficial applied to the MOX fuel.
5. I&EC Annular Fuel with Silicon Carbide Cladding

5.1 I&EC Annular Fuel Considerations

As discussed in Chapter 2, an internally- and externally cooled annular fuel rod was developed at MIT in order to allow an increase in LWR power density and increase thermal safety factors. Two versions of FRAPCON were created previously in order to model the I&EC annular fuel rod performance: one version with sintered annular fuel pellets, and second version with VIPAC fuel. In this investigation, both of these codes were modified to support the SiC cladding models developed for FRAPCON-SiC and discussed in Chapter 3.

It is interesting to analyze both of these cases because of the different qualities of the VIPAC and sintered pellet fuel. The pellet form is similar to the annular pellets discussed previously, except that now there is a distribution of the heat flux between the inner and outer surfaces of the fuel. This should help decrease pellet temperature, and reduce fission gas release. However, there will be both inner and outer fuel-cladding gap effects to consider, which were shown in the previous analysis to be a source of high thermal resistance in SiC-clad fuel rods. The lack of creep in SiC cladding must also be considered, since it may affect the rate at which the fuel-cladding gaps close. It is important to ensure that the inner and outer gaps close simultaneously in order to prevent large heat flux imbalances, which may have safety implications.

The VIPAC fuel may help mitigate one of the major issues identified with SiC cladding in the previous analysis, which is the low gap conductance. Because VIPAC fuel is constructed of micrometer-sized fuel grains, there is no gap in the VIPAC fuel rod. In this fuel rod design, the cladding thermal conductivity will be the primary limiting factor for heat transfer. This also eliminates the problem of ensuring simultaneous gap closures.
5.2 FRAPCON VIPAC Model

5.2.1 Model Development

The MIT I&EC annular fuel geometry, encapsulated in a SiC duplex annular cladding, is shown schematically in Figure 5.1. In this case, the inner diameter of the fuel is supported by a SiC monolith, which is reinforced by the fiber-matrix SiC composite outer layer. In this manner, only the inner and outer composite surfaces are exposed to the reactor coolant.

As discussed previously, the SiC cladding model for I&EC annular fuel includes only the properties of the fiber-matrix composite, and assumes that this composite makes up the entire thickness of the cladding. This new version of FRAPCON has been denoted as FRAPCON-3.3HB-VIPAC-SiC, and contains the same SiC cladding properties that were developed and discussed previously as a part of FRAPCON-SiC. This version of the code was developed directly from the updated version of Yi Yuan’s original annular VIPAC code, FRAPCON-VA, discussed in Chapter 2.

![Figure 5.1  I&EC VIPAC Annular Fuel Rod with SiC Duplex Cladding.](image-url)
5.2.2 Evaluation of VIPAC Fuel Performance

In order to judge the performance of the I&EC VIPAC annular fuel with SiC cladding, the results from FRAPCON-3.3HB-VIPAC were compared to those from the updated version of the annular fuel code developed for Zircaloy cladding, FRAPCON-3.3HB-VIPAC-SiC.

The input file used for this evaluation is the VA-100 case discussed in Chapter 2. This case describes a fuel rod in a 13x13 Westinghouse PWR fuel assembly running at 100% of rated power (3411 MWth). The properties of the fuel rod used in this case are given in Table 2.3, and the power history is shown in Figure 2.4.

![Figure 5.2](image)

**Figure 5.2 Average Fuel Temperature of I&EC VIPAC Annular Fuel Rods with Zircaloy and SiC Cladding.**
As shown in Figure 5.2, the SiC-clad fuel rod has a higher average fuel temperature than the Zr-clad fuel rod due to the SiC’s lower thermal conductivity. The difference is 50-60 K at beginning of life, and narrowing to less than 20 K by discharge. Overall, however, the fuel temperature behavior over the irradiation life of the fuel rod is the same as the Zr-clad fuel rod: a steady temperature profile in time at each power level.

This same relationship, in terms of temperature difference and behavior, is reflected in the cladding temperature history shown in Figure 5.3. This stable behavior is a result of the VIPAC fuel maintaining a closed fuel-cladding gap, therefore avoiding large effects due to gap conductance.

The low fuel temperatures and moderate burnup in this case result in a low fission gas release, which is calculated to be approximately linear with burnup and below 0.3% by end of life for both fuel rods. This low gas release and the low initial fill gas pressure, just 1.4 MPa, contribute to a low rod internal pressure during operation that decreases with time, as plotted in Figure 5.4. The small decrease of about 0.25 MPa over the lifetime of both fuel rods is attributable to the straining of both the inner and outer cladding due to fuel swelling. This strain is small, less than 0.2% for either of the fuel rods that were
modeled, although the higher fuel temperature in the SiC-clad fuel rod contributed to several hundredths of a percent more swelling than the Zr-clad fuel rod.

![Figure 5.4 Internal Free Gas Pressure of Zr- and SiC-clad I&EC VIPAC Annular Fuel Rods.]

5.2.3 VIPAC Fuel at Very High Burnup

In order to observe the effects of very high burnup on the thermo-mechanical behavior of the I&EC annular fuel rod with SiC cladding, a new FRAPCON case was created. This case is a hypothetical extension of the VA-100 case used above, but starts at 150% of the original power density and continues up to 100 MWd/kgU. This input case will therefore allow examination of the fuel rod behavior in the very high burnup fission gas release regime, as well as with increased fuel swelling.

The power history for this case is given in Figure 5.5 and the burnup in Figure 5.6, where the results for the fuel rod with Zircaloy cladding (Zr) and SiC cladding (SiC) are equivalent. This burnup and power scheme represents about 4.5 years of full power reactor operation at a 95% capacity factor.
Figure 5.5 Average Power of I&EC VIPAC Annular Fuel High Burnup Case with Zircaloy and SiC Cladding.

Figure 5.6 Average Burnup of I&EC VIPAC Annular Fuel High Burnup Case with Zircaloy and SiC Cladding.

The fuel temperature history for these cases, shown in Figure 5.7, gives a higher average temperature for the SiC cladding throughout the irradiation. This temperature difference is due almost entirely to the difference in thermal conductivity between the Zircaloy and the SiC, since both fuel-cladding gaps are always closed. However, as shown in Figure 5.8, the higher fuel temperature is not significant enough to yield a higher fission gas
release for the SiC cladding case. Overall, the fission gas release for both cladding types is significantly lower for this case than the Manzel high burnup case that was examined earlier; the maximum release at 100 MWd/kgU is below 10%.

Figure 5.7 Average Fuel Temperature of I&EC VIPAC Annular Fuel High Burnup Case with Zircaloy and SiC Cladding.

Figure 5.8 Fractional Fission Gas Release of I&EC VIPAC Annular Fuel High Burnup Case with Zircaloy and SiC Cladding.
Given the small amount of fission gas release over the lifetime of the fuel rods, both rod internal pressure predictions are low and roughly equivalent, as shown in Figure 5.8. Even at the end of life, the pressure in both rods is well below the external coolant pressure. There are small drops in the pressure around 30, 60, and 85 MWd/kgU due to drops in the fuel temperature as the power level decreases.

The cladding strain for each fuel rod follows a similar trend, shown in Figure 5.10, where both claddings are strained outward by a small amount (the outer cladding diameter increases while the inner cladding diameter decreases); less than one tenth of a millimeter for both fuel rods. This movement is due to mechanical interaction with the fuel as it swells due to microstructure changes and the buildup of fission products. The higher temperature of the SiC cladding, along with the higher temperature of the fuel and higher plenum pressure contributes to slightly more swelling in the SiC-clad fuel rod.

![Graph showing internal free gas pressure](image-url)

*Figure 5.9 Internal Free Gas Pressure of I&EC VIPAC Annular Fuel High Burnup Case with Zircaloy and SiC Cladding.*
5.3 FRAPCON Sintered Annular Fuel Model

5.3.1 Model Development

The sintered annular pellet model uses a fuel form very similar to the annular pellets discussed in Chapter 4, however they are generally larger in diameter, and the boundary conditions on the inner fuel surface are more complex. A cross section of this fuel rod design is shown in Figure 5.11, which highlights the major difference between this model and the VIPAC fuel model discussed previously – the presence of fuel cladding gaps in the inner and outer annulus.

The version of FRAPCON developed to model this fuel is FRAPCON-3.3HB-SA-SiC. The results were compared to the Zircaloy-clad I&EC sintered annular fuel rod using FRAPCON-3.3HB-SA. The SiC modeling version was developed from the Zircaloy version by adding the SiC property models discussed previously.
5.3.2 Evaluation of Sintered Annular Fuel Performance

The case used for this analysis of the SiC-clad I&EC sintered annular fuel rod is the SA-100 case described in Chapter 2. This case is a 13x13 Westinghouse PWR fuel assembly running at 100% of rated power (3411 MWth), with the fuel and cladding properties given in Table 2.2.

The fuel rod temperature profile develops over the first few time steps in this case due to swelling and creep in the claddings; The average fuel temperatures are shown in Figure 5.12. The Zircaloy cladding creeps down onto the fuel’s internal and external surface by about 7 MWd/kgU, and the gap remains closed for the rest of the irradiation.

The SiC cladding does not creep, but instead swells due to thermal expansion and irradiation over the first few time steps. This opens the fuel-cladding gap and raises the temperature of both the fuel and the cladding, which is shown in Figure 5.13. At the peak, the fuel temperatures of the SiC-clad fuel rod are 140 K higher than the Zr-clad rod. There is also a larger difference in the temperature profile across the fuel pellet radius, with the inner surface 20-50 K higher than the outer surface. This is compared to at most a 10 K difference in the Zr-clad pellet.
An interesting effect of the lower thermal conductivity of the SiC cladding is that the average temperatures of the inner and outer SiC cladding are almost identical, whereas there is about a 10 K difference in the Zircaloy. Although the average SiC cladding temperature is higher, a close match between these temperatures of the inner and outer cladding indicates a better balance of the heat flux through the two surfaces to the coolant, which is advantageous from a safety standpoint.

Figure 5.12 Average Fuel Temperature for I&EC Sintered Annular Pellet Fuel Rods with Zircaloy and SiC Cladding.
The SiC cladding also exhibits more stable mechanical behavior over the irradiation history, shown in Figure 5.14. By the second time step, the SiC cladding has reached its final radial deformation due to the interplay of the coolant pressure acting on the inner and outer cladding surfaces, and the thermal expansion and irradiation swelling of the cladding. The creep in the Zircaloy cladding results in larger initial strains as the fuel-cladding gap closes, and then the cladding is pushed outward through mechanical contact with the expanding fuel pellet.

The fuel rod internal pressure in these sintered annular pellet cases, plotted in Figure 5.15, is similar to that of the VIPAC fuel. It starts with a helium fill pressure of 1.4 MPa, and is aided by the low fuel temperatures that keep fission gas release around 0.3% for the length of the irradiation. The SiC-clad fuel rod pressure rises by about 1 MPa as the fuel swells and closes the radial fuel-cladding gap.
The fuel-cladding gap conductance, which is important for determining the balance of the heat flux through the inner and outer cladding surfaces, is shown in Figure 5.16. The conductivity of the gap with Zircaloy cladding rises significantly as the cladding creeps down, then drops primarily due to fission gas release. On the other hand, the SiC-clad fuel rod’s conductance remains lower but relatively constant. A steady conductance
history is important for ensuring that there are not large changes in the heat flux through either surface, which may have safety implications related to departure from nucleate boiling.

5.3.3 Sintered Annular Fuel at Very High Burnup
As with the VIPAC fuel analysis, it is desirable to predict the performance of I&EC sintered annular fuel rod at very high burnup. Using the SA-100 input case from the above analysis, the initial power density was increased by 150%, and the irradiation time extended to achieve about 100 MWd/kgU at discharge. The power history for this case is the same as the one shown in Figure 5.5, and the burnup is given in Figure 5.17.
The average fuel temperature for the SiC-clad fuel rod is even higher in this case than the VIPAC fuel rod because of the fuel-cladding gap. In Figure 5.18 the closure of the Zircaloy fuel-cladding gap, which occurs by 10 MWd/kgU, is revealed by the sudden drop in the average fuel temperature. The opposite is true for the SiC-clad fuel rod, where the fuel pellet outer diameter temperature increases during the first few time steps as the cladding swells outward, away from the fuel. The result is the fuel temperature averaging 100-150 K higher during the first half of life; the temperatures of the two fuel rods begin to converge at high burnup as the fuel swells outward and closes both gaps at 70 MWd/kgU.
Like the fission gas release prediction for VIPAC fuel, the gas releases for the sintered annular pellets is low, with the most significant release predicted in the very high burnup region. As shown in Figure 5.19, the release is very similar for the two fuel rods due to the low average fuel temperatures, and is below 8% at discharge.

The resulting internal pressurization of the fuel rods, shown in Figure 5.20, is again similar to the VIPAC case. The majority of the pressure increase occurs during the very high burnup fission gas release, but final pressures are still below the external pressure. The SiC-clad fuel rod experiences slightly higher pressures due in part to the higher gap temperatures because of the wider fuel-cladding gaps, and because the SiC does not yield outwards as readily under the pressure from the gas pressure and from the fuel mechanical interaction.
The cladding strains during this high burnup case, shown in Figure 5.21, are very similar to the SA-100 case analyzed above at low burnup. The Zircaloy creeps inward due to the external coolant pressure until it contacts the fuel, and then moves outward as the fuel swells and the fuel rod internal pressure increases. The SiC cladding experiences some initial irradiation swelling, then remains stable until very high burnup, when the fuel contacts both the inner and outer cladding surfaces and begins to stress them outwards.
Figure 5.21 Average Cladding Strain of I&EC Sintered Annular Fuel High Burnup Case with Zircaloy and SiC Cladding.

5.4 Summary

Modeling of I&EC annular fuel with SiC cladding has demonstrated that this cladding provides the same advantages as that of Zr-based cladding: lower fuel temperature at higher power densities than traditional pin-type fuel rods. Both the VIPAC fuel and sintered annular pellets experienced lower temperatures and lower fission gas release at comparable discharge burnup to those examined in the Manzel test case, around 100 MWd/kgU.

The annular fuel rod with VIPAC fuel has the advantage of no-fuel cladding gap, although there is therefore constant mechanical interaction between the cladding and the fuel grains. This combination of fuel and rod geometry achieves the lowest fuel and cladding temperatures and internal pressures, which suggests opportunity for further power density up-rating as well as a probable improvement in accident performance.

The sintered annular pellets have behavior similar to the pin-type fuel rods examined earlier; the Zircaloy cladding creep down is contrasted by the open SiC fuel-cladding
gaps, which leads to a further increased pellet temperature for the SiC-clad fuel rod. However, this also means that no mechanical interaction between the pellet and the SiC cladding is expected until very high burnup, which corresponds to a more stable rod geometry. For both fuel types, the internal pressure remained low during most of the irradiation, increasing primarily due to very high burnup fission gas release. Even at its maximum in the high burnup case, this pressure is below the external coolant pressure, and well within what is tolerated by the stress limits of the SiC composite.
6. I&EC Annular Fuel Post-Irradiation Examination

6.1 Overview

As a part of the MIT I&EC annular fuel irradiation program discussed in Chapter 1, two prototype annular fuel rods were irradiated in the core of the MIT reactor. After six months of irradiation, the fuel rods, each enclosed in a separate aluminum capsule as shown in Figure 6.1, were removed from the core and placed in underwater storage. They remained in storage for one year of decay and cooling before being extracted for post-irradiation examination (PIE) at the MIT Nuclear Reactor Laboratory.

![Figure 6.1 Pre-irradiation I&EC Annular Fuel Irradiation Capsule 4 (left) with Cross-Section Schematic.](image)

Preliminary design and planning of the PIE facilities was conducted by Yi Yuan [Yuan et al., 2004] and Jiyun Zhao [Zhao, 2005] at MIT, and called for the use of a hot box facility to conduct gamma spectroscopy and fission gas release measurements on the fuel rods.
The gamma spectroscopy was designed to be conducted in a piecewise manner along the axis of the fuel rods with the aid of a collimator, and therefore allow some spatial resolution. The proposed fission gas release measurement system included a gas sampling apparatus that would puncture the cladding and draw out the gas into a counting chamber.

As a part of this thesis, the final design of this PIE system was decided, manufactured, and installed in the hot box. The following sections describe the final configuration of the PIE systems, their setup, and the data that was acquired. In addition, estimations of the fuel disposition, burnup, and fission gas release are discussed.

### 6.2 Experimental Design

#### 6.2.1 Hot Box Design

The MIT hot box facility consists of a steel-lined, lead-filled rectangular box 6.5 feet wide, 4.5 feet tall, and 4.5 feet deep. The walls are 6 inches thick lead, with 1/2-inch thick steel plates surrounding either side; in addition, there are three 12-inch-thick leaded glass viewports. Handling inside the hot box is facilitated by two master/slave manipulators that penetrate through ports on the lid. Samples are normally inserted via a 2-inch wide cylindrical port located on the lid between the manipulators, however this was not possible in this case because of the size of the annular fuel capsules.

For this project, loading was accomplished by removing one manipulator, lowering the capsule into the hot box from the transfer cask, and then replacing the manipulator once the capsule was secure inside a specially built shield cave. There is also a sliding door mounted at the rear of the box that opens onto a 12-inch-diameter port. This door was removed in order to allow insertion of the collimator and gamma ray detector.

The hot box came equipped with an internal fire alarm and suppression system, pressure gauge, and a meter for measuring exposure rate mounted on the rear wall. In addition, there is a roof opening for ventilation ducting. A ventilated exhaust is provided in the
reactor building, and the hot box was connected to this in order to maintain internal air pressure below the ambient pressure during any work with the capsules.

In order to minimize the exposure rate around the hot box and reduce the background radiation during the gamma spectroscopy, it was decided to allow only one capsule into the hot box at any time. To ensure that the hot box would provide sufficient shielding for the capsules during their examinations and any storage time required when the hot box was not in use, a shielding analysis was performed.

This analysis was based on a measured exposure rate of 600 R/hr 13 inches from the capsules in water when they were removed from the core in September 2004. An ORIGEN calculation was performed based on this irradiation, which produced the expected intensity and energy distribution of gamma rays at discharge of the fuel from the core, as well as after three months of decay (the earliest time that examinations were expected to begin). One capsule was predicted to have somewhat higher activity since it was located at an axial position of higher flux; however, the difference between the capsules was small compared to the absolute activity.

For either capsule and from discharge to three months, the highest intensity of gamma rays was in the 0.85 MeV energy group, and therefore for a conservative analysis it was assumed that all photons had an energy of 1 MeV (The higher the photon energy, the more shielding is required to provide the same effective dose rate reduction). For the attenuation calculation, it was assumed the capsules could be represented as a mono-energetic plane source, which allowed easier calculation with the most conservative geometry considerations. In addition, all attenuation of the photons in air was neglected.

First, the effective exposure rate on contact with the capsules, $E_0$, was determined. Exposure rate is given by,

$$E = B E_0 e^{-\mu x} \quad (6.1)$$
where $B$ is the dose buildup factor, $\mu$ is the attenuation coefficient, and $x$ is the distance from the point of measurement to the source [Turner, 1995]. The properties of water and lead used for this calculation are given in Table 6.1. The attenuation coefficient is found from,

$$\mu = \mu_m \rho$$  \hspace{1cm} (6.2)

where $\mu_m$ is the mass attenuation coefficient for a material, and $\rho$ is the density.

**Table 6.1 1 MeV Photon Attenuation Calculation Material Properties.**

<table>
<thead>
<tr>
<th>Material</th>
<th>Density (g/cm$^3$)</th>
<th>Mass Attenuation Coefficient (cm$^2$/g)</th>
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</thead>
<tbody>
<tr>
<td>Water</td>
<td>1.0</td>
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</tr>
<tr>
<td>Lead</td>
<td>11.4</td>
<td>0.0710</td>
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</table>

The dose buildup factor for a material can be found from pre-compiled tables based on the photon energy and the number of relaxation lengths, which is the product $\mu x$, where $x$ is the shield thickness. From 13 inches of water, the dose buildup factor can be looked up as approximately 3.8. Thus, the exposure rate on contact for the combination of the two irradiated capsules at discharge is estimated to be 1630 R/hr at discharge.

Using this information, the expected maximum exposure rate at the surface of the shielding was calculated. The exposure rate for a single capsule through various thicknesses of shielding is given in Table 6.2.
Table 6.2 Exposure Rate for a Single Capsule through Various Thicknesses of Lead for 1 MeV Photons.

<table>
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<tr>
<th>Lead Thickness (in)</th>
<th>Relaxation Lengths (μx)</th>
<th>Dose Buildup Factor</th>
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</tr>
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<td>0.65</td>
</tr>
<tr>
<td>6</td>
<td>15.2</td>
<td>3.6</td>
<td>0.013</td>
</tr>
<tr>
<td>8</td>
<td>20.3</td>
<td>4.3</td>
<td>0.00026</td>
</tr>
<tr>
<td>10</td>
<td>25.4</td>
<td>5.1</td>
<td>5×10⁻⁶</td>
</tr>
</tbody>
</table>

From this calculation, the exposure rate at the surface of the 6-inch thick lead walls of the hot box will be at most 13 mR/hr, and likely less due to decay, the source geometry, and attenuation in the air and the steel plates.

Based on this result, it was determined that no additional shielding was required for the hot box during counting. However, a storage cave was created inside the hot box, adding an additional 4 inches of lead when the capsule was stored between counts, and allowing limited unshielded access to the interior of the hot box, such as during the re-installation of the manipulator after transferring in a capsule.

### 6.2.2 Counting System

A system for measuring fuel burnup using a gamma detector was described in previous work on the annular fuel project by Yi Yuan [Yuan et al., 2004]. In addition, a separate apparatus for measuring fission gas release was proposed; this system would require the aluminum capsule to be removed so that the cladding could be punctured and the plenum space swept for fission gasses. This system was ultimately discarded because of the complex machinery required, the small amount of fission gas expected, and the hazardous waste that would be produced by compromising the aluminum and Zircaloy containments.
Instead, it was decided to use the gamma spectroscopy system to accomplish both burnup and fission gas release measurements, as well as to attempt to evaluate the physical disposition of the VIPAC fuel. This method has the added advantage of avoiding any destructive examination, so the fuel capsules can still be easily shipped to other laboratories for further analysis in their original condition.

In order to allow gamma counting using a Ge detector, a lead collimator was designed to fit into the back of the hot cell. This collimator was designed to allow the entire diameter of the capsule to be viewed by the detector, but with a width of only 1/16 of an inch so that some axial resolution could be achieved. The expected count rate at the detector for various collimator thicknesses was calculated, and it was decided to use a collimator thickness of 20 inches. This gives a count rate of around 2500 counts per second at the detector for a 1.7 Ci source with less than 0.01% background-to-signal ratio [Yuan et al., 2004].

The collimator is a stepped plug with a slot in the center for the collimation window, as shown in Figure 6.2. The open slit was fabricated from a piece of rectangular steel bar, stepped to fit into a stepped hole in the lead outer collimator, but then cut in half down the long axis. The cut surface was polished and then welded back together along the edges leaving a 1/16-inch gap between the two sides. This created a uniform window that could be removed from the bulk of the collimator and modified later, should the count rate be found to be unacceptably high or low.
To support the capsule in front of the collimator window and allow simple but repeatable axial positioning, a support stand was designed. This stand, pictured in Figure 6.3 and Figure 6.4, takes advantage of the azimuthal alignment pegs present on each capsule with a series of slots, each 0.08 inches wide and 0.37 inches apart. Placing the capsule such that four of the pegs fit into the slots, the capsule can be moved from one side of the collimator window to the other in small, even intervals with the use of the manipulators.

Figure 6.2 Top View of Collimator and Detector Setup with Hot Box.

Figure 6.3 Model of an I&EC Annular Fuel Capsule on the Gamma Counting Stand.
The Ge detector, mounted to a liquid N\textsubscript{2} Dewar for cooling, was placed at the rear of the hot box aligned with the collimator window and shielded against background radiation. The detector connected to a data acquisition computer that used multi-channel analyzer hardware and software to process the signal.

### 6.2.3 Detector Calibration

The detector was energy-calibrated using Co-60 and Cs-137 calibration sources before being installed in the rear of the hot cell. Once installed this calibration was re-checked using a Cs-137 source that also served as an absolute activity calibration. In order to replicate the geometry and self-shielding effects, this second calibration used a small cylindrical source chamber attached to the end of a metered rod. This was then placed inside of a fueled but un-irradiated annular fuel capsule, which therefore contained significant amounts of uranium and lead to attenuate the gamma rays.
The source was moved through the capsule in 1/32-inch increments, with a six-minute count at each position. The results of this procedure are shown in Figure 6.5, where it is apparent the source is concentrated in a small volume. Based on this figure, it was assumed the 5.9 mCi source was uniformly distributed over a 3/32-inch length. If this is true, at the position where the entire width of the collimator window is exposed to the source the detector registered 141 net counts per second for the Cs-137 662 keV peak. This gives a detector absolute energy calibration of $3.59 \times 10^4$ counts/Ci-sec at this energy.

![Figure 6.5 Net Counts per Second from the Cs-137 Absolute Calibration Source.](image)

**6.2.4 Capsule Extraction and Handling**

Before being transferred to the MIT reactor spent fuel pool from the reactor core tank storage ring, the cover gas tubes on the two irradiated annular capsules were cut as shown in Figure 6.6. This allowed the capsules to fit more easily into the hot cell interior. The annular fuel capsules were then placed in a transfer container and moved from the core tank to the spent fuel pool.

Once the hot cell was prepared to receive the capsules they were moved one at a time from the pool into the hot cell. This was performed by removing the desired capsule from
the storage container in the spent fuel pool, then placing it into a basket that was remotely retracted into a transfer cask suspended above the pool. The cask was then moved onto the top of the hot cell where one of the manipulators had been removed, and the basket was lowered into the cask through that open port.

![Image](image.jpg)

Figure 6.6 I&EC Annular Fuel Capsule's Cover Gas Tube being Cut in MITR-II Core Tank.

6.3 Fuel Examination

6.3.1 Visual Inspection

The first irradiated capsule moved into the hot box for examination was Capsule 3, shown in Figure 6.7. The outer aluminum capsule, which was exposed to the reactor primary coolant, had dulled significantly and had dark hairline streaks running along the vertical axis. There is also a discolored band clearly visible along the middle of the capsule, corresponding to the location of the fuel within the inner cladding. This
discolored area, bounded by two darker bands, was uniform in texture, and did not feature the vertical streaks seen elsewhere. These effects were uniform around the circumference of the capsule.

The second I&EC annular fuel capsule loaded into the hot box, pictured in Figure 6.8, was Capsule 4. This capsule displayed the same features as Capsule 3, however there were added effects due to the presence of three steel shim stock bands that were spot welded to the outside of the capsule. These bands held in place two thermocouples used to measure the capsule external temperature during irradiation. Two more thermocouples were secured by two additional steel bands in approximately the same axial position on the inside of the capsule.

The discolored bands and streaks found on the first capsule are apparent to the same magnitude on Capsule 4, on both the inner and outer surfaces. Again, the discolored region approximately overlays the internal location of the fuel, and its shape and intensity
is affected by the presence of fin-like perturbations on the capsule surface that would have modified the local heat flux like the alignment pegs, steel bands, and thermocouple sheaths.

For a rough exposure rate estimate, each capsule was held at the back of the hot box against a measuring tape leading to the in-box radiation monitor. At 60 cm from the in-box detector, the exposure rate reading for Capsule 3 was about 20 R/hr. Capsule 4, which had resided below Capsule 3 in a slightly lower flux region of the core, was 12 R/hr.

Figure 6.8 I&EC Annular Fuel Capsule 4 after Irradiation.
6.3.2 Counting Results

A fuel capsule in place on the hot box stand during counting is shown in Figure 6.9, and with a view of the collimator face and storage area in Figure 6.10. Because of the high count rates encountered in the fueled regions of the capsules compared to the very low count rates at the edges, the counting time was varied to allow the large number of axial counts required for each capsule to be completed in a reasonable time. The live counting time varied between 3 and 30 minutes, with dead times over 94% in the fueled region.

About twenty radionuclides were identified in each capsule from the results of the gamma spectroscopy. The dominant elements were the same in both capsules, with zirconium being the dominant activity by at least an order of magnitude in both cases. The Zr-95 distribution is plotted for both capsules in Figure 6.11, where the fueled region is most apparent, followed by sharp drops in activity on either side. At the left end of the graph, the cladding sits on top of a ceramic spacer at the bottom of the aluminum capsule and the counts drop almost directly to zero. At the top, however, there is a clearly defined plenum space that is an order of magnitude lower in activity than the cladding adjacent to the fuel.

Some broadening of the shape apparent in this and later figures due to the finite width of the collimator window, which is significant given the size of the features being observed. The collimator window is 1/16 inch wide, and therefore is counting over an area about 1/3 of width of each position mark on the graph, where the indicated measurement is the center of that field.
Figure 6.9 Exterior of Hot Box during Capsule Counting.

Figure 6.10 View of Hot Box Interior from Right Viewing Port during Capsule Counting.
6.3.3 Fuel Distribution

The dominant radionuclides (besides zirconium) found in the fuel region were Ru-106 (622 and 1050 keV from Rh-106) and Pr-144 (697 keV), both fission fragments. The distribution of these isotopes in Capsule 3 is shown in Figure 6.12, and in Figure 6.13 for Capsule 4. These figures also display a cross section of the fuel rod to the appropriate scale; it is apparent that the detected nuclides correspond strongly to when the collimator window is viewing the fueled region.

Based on the detector calibration, the average count rate over the fuel area, and the relative intensity of the detected gammas, an estimate for the activity of these isotopes was generated, and is given in Table 6.3. These correspond fairly well with the relative radioisotope yields that were predicted by the ORIGEN fuel burnup and decay code. The difference in the results may be an indication of errors in the assumptions made for the ORIGEN calculation or inaccuracy in the absolute energy calibration.
Figure 6.12 Net Count Rate of Dominant Radionuclides in Capsule 3 with to-scale Fuel Rod Cross-Section.

Figure 6.13 Net Count Rate of Dominant Radionuclides in Capsule 4 with to-scale Fuel Rod Cross-Section.
Table 6.3 Estimated Activity of Major Isotopes in Fuel.

<table>
<thead>
<tr>
<th>Isotope</th>
<th>Energy (keV)</th>
<th>Relative Intensity (%)</th>
<th>Estimated Activity (Ci)</th>
<th>Activity Calculated by ORIGEN (Ci)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td>Capsule 3</td>
<td>Capsule 4</td>
</tr>
<tr>
<td>Ru-106</td>
<td>622</td>
<td>9.93</td>
<td>5.0</td>
<td>4.1</td>
</tr>
<tr>
<td>Ru-106</td>
<td>1050</td>
<td>1.56</td>
<td>3.5</td>
<td>2.8</td>
</tr>
<tr>
<td>Pr-144</td>
<td>697</td>
<td>1.34</td>
<td>43</td>
<td>39</td>
</tr>
</tbody>
</table>

Based on the axial distribution of these peaks in both capsules, it appears that the fuel particles did not migrate through the top spacers into the plenum. Although it is noted that there are small peaks at the top weld in the fuel rod and the aluminum capsule, these are not well coordinated among the three energies found in the fuel, and may represent noise introduced by the larger amount of structural material in those cross sections.

Based on the count rates at the edges of the fuel region there is no reason to suspect any significant swelling or densification has occurred since the fuel rods were manufactured [AECL, 2003]. In addition, the distribution of fuel activity appears relatively uniform, although it does peak at both ends, and rises slightly towards the top of Capsule 3 and bottom of Capsule 4. These effects are consistent with the neutron flux distribution in the reactor core.

6.3.4 Burnup Measurement from Cs-137 Activity

Two methods were used to deduce the average fuel burnup, both depending on proportional relationships between cesium activity and burnup. The first involved calculating the Cs-137 activity in the fuel, which depends heavily on the accuracy of the detector calibration. The second method used the ratio of the count rates for gammas from Cs-137 and Cs-134, which eliminates the dependence on the detector activity calibration.

First the activity of Cs-137 was calculated for each capsule by averaging the count rate over the fuel area, adjusting for the relative intensity of the detected gamma (85.1% at 662 keV), and using the activity correlation determined previously. This experimental
result (corrected for one year of decay) is compared to the ORIGEN calculation in Table 6.4, which shows relative agreement, although the measured activities are higher than the predicted activities.

<table>
<thead>
<tr>
<th>Energy (keV)</th>
<th>Relative Intensity (%)</th>
<th>Estimated Activity (Ci)</th>
<th>Activity Calculated by ORIGEN (Ci)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Capsule 3</td>
<td>Capsule 4</td>
<td>Capsule 3</td>
<td>Capsule 4</td>
</tr>
<tr>
<td>662</td>
<td>85.1</td>
<td>4.7</td>
<td>3.99</td>
</tr>
<tr>
<td></td>
<td></td>
<td>2.53</td>
<td>1.86</td>
</tr>
</tbody>
</table>

Because of its long half-life (30.07 years), the activity of Cs-137 in the fuel, $A_7$, during the six-month irradiation was assumed dependent only on the rate of yield from fission. Thus, using a method described by Hawari [Hawari et al., 2001],

$$A_7 = \lambda_7 \cdot V_f \cdot Y_7 \cdot \Sigma_f \cdot \int \varphi(t) \cdot dt \quad (6.3)$$

where $\lambda_7$ is the Cs-137 decay constant, $V_f$ is the total volume of the fuel, $Y_7$ is the Cs-137 fission yield (accumulated), $\Sigma_f$ is the fission cross section, $\varphi$ is the thermal flux, and $t$ is the irradiation time. The fluence term can be related to the burnup, $B$, by,

$$B = \frac{1}{\rho} \cdot E_f \cdot \Sigma_f \cdot \int \varphi(t) \cdot dt \quad (6.4)$$

where $\rho$ is the fuel density and $E_f$ is the energy released per fission [Zhengpei and Wenfeng, 2002]. Combining equations (6.3) and (6.4),

$$B = \frac{1}{\rho} \cdot E_f \cdot \frac{A_7}{\lambda_7 \cdot V_f \cdot Y_7} \quad (6.5)$$

The values of the constants used in equation (6.5) are found in Table 6.5, and the two burnup estimates calculated using this equation are given in Table 6.6. One estimate is based on the experimentally measured Cs-137 activity and the other from the activity calculated by the ORIGEN code.
Because of the higher measured activity than that predicted by ORIGEN, the burnup calculated from measurement is higher than that predicted by ORIGEN. However, the similarity between the ORIGEN prediction and the burnup calculated from the MONTEBURNS code (coupling of MCNP and ORIGEN) suggests that the method itself is an accurate tool for calculating burnup from Cs-137 activity.

Table 6.5 Constants Used in Burnup Calculations [Chang, 2005].

<table>
<thead>
<tr>
<th>Variable</th>
<th>Value</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\lambda_7$</td>
<td>7.31x10^{-10} (1/s)</td>
<td>Cs-137 decay constant ($t_{1/2} = 30.07$ yr)</td>
</tr>
<tr>
<td>$\lambda_4$</td>
<td>1.06x10^{-8} (1/s)</td>
<td>Cs-134 decay constant ($t_{1/2} = 2.065$ yr)</td>
</tr>
<tr>
<td>$Y_7$</td>
<td>6.27x10^{-2}</td>
<td>Cs-137 accumulated fission yield</td>
</tr>
<tr>
<td>$Y_3$</td>
<td>6.70x10^{-2}</td>
<td>Cs-133 accumulated fission yield</td>
</tr>
<tr>
<td>$E_f$</td>
<td>0.365x10^{-21} (MWd)</td>
<td>Energy released per fission (197 MeV)</td>
</tr>
<tr>
<td>$\Sigma_f$</td>
<td>capsule 3 = 0.073 (1/cm) capsule 4 = 0.079 (1/cm)</td>
<td>Fission cross section</td>
</tr>
<tr>
<td>$RI_4$</td>
<td>0.976</td>
<td>Relative intensity of Cs-134 605 keV gamma</td>
</tr>
<tr>
<td>$RI_7$</td>
<td>0.851</td>
<td>Relative intensity of Cs-137 662 keV gamma</td>
</tr>
<tr>
<td>$\sigma_3$</td>
<td>capsule 3 = 12.3x10^{-24} (cm^2) capsule 4 = 12.4x10^{-24} (cm^2)</td>
<td>Cs-133 neutron absorption cross section</td>
</tr>
<tr>
<td>$V_f$</td>
<td>12.8 (cm^3)</td>
<td>Total UO$_2$ volume</td>
</tr>
<tr>
<td>$\rho$</td>
<td>7.93 (g/cm^3)</td>
<td>Uranium density</td>
</tr>
</tbody>
</table>

Table 6.6 Calculated Burnup from Cs-137 Activity.

<table>
<thead>
<tr>
<th>Source</th>
<th>Burnup (MWd/kgU)</th>
</tr>
</thead>
<tbody>
<tr>
<td>MONTEBURNS</td>
<td>Capsule 3</td>
</tr>
<tr>
<td>Measured Cs-137 activity</td>
<td>14</td>
</tr>
<tr>
<td>ORIGEN (Cs-137 activity)</td>
<td>7.3</td>
</tr>
</tbody>
</table>

6.3.5 Burnup Measurement from the Cesium Ratio

A second approach to measuring burnup is by comparing the Cs-134 and Cs-137 count rates. The advantage of this approach is that it does not require an energy calibration for the detector; however, it does require average fission and capture cross sections. An
approximate relation for the ratio of the count rates for low burnup in a thermal spectrum was given by Zhengpei and Wenfeng as,

\[
\frac{R_4}{R_7} = \frac{\lambda_4 \cdot Y_3 \cdot R_{I_4}}{2 \cdot \lambda_7 \cdot Y_7 \cdot R_{I_7}} \cdot \sigma_3 \cdot \int \varphi(t) \cdot dt
\]  

(6.6)

where \( R \) is the Cesium count rate, \( \lambda \) the decay constant, \( R_{I} \) the relative intensity of the measured gamma ray, and \( \sigma_3 \) the absorption cross section for Cs-133. Combining equations (6.4) and (6.6),

\[
B = \frac{1}{\rho} \cdot E_f \cdot \sum_i \cdot \frac{R_4 \cdot 2 \cdot \lambda_7 \cdot Y_7 \cdot R_{I_7}}{R_7 \cdot \lambda_4 \cdot Y_3 \cdot R_{I_4} \cdot \sigma_3}
\]  

(6.7)

The ratio of the count rates of the Cs-134 peak at 605 keV and the Cs-137 peak at 662 keV, corrected for one year of decay, is presented in Figure 6.14. The average value of these ratios over the fueled length (position 2.5 to position 9.5) is 0.165 for Capsule 3 and 0.128 for Capsule 4. The burnup estimates using equation (6.7) from both the experimentally derived ratios are found in Table 6.7 based on the constants in Table 6.5.

![Figure 6.14 Ratio of the Cs-134 to the Cs-137 Net Count Rate in the Fuel Region (position 2.5 to 9.5), Corrected for Decay.](image-url)
Table 6.7 Calculated Burnup from Cesium Ratio.

<table>
<thead>
<tr>
<th>Source</th>
<th>Capsule 3</th>
<th>Capsule 4</th>
</tr>
</thead>
<tbody>
<tr>
<td>MONTEBURNS</td>
<td>7.32</td>
<td>5.64</td>
</tr>
<tr>
<td>Measured Cs-134/Cs-137 ratio</td>
<td>6.9</td>
<td>5.7</td>
</tr>
</tbody>
</table>

The burnup calculated using the experimentally measured cesium ratio gives a value closer to the MONTEBURNS prediction than that from the Cs-137 activity in Table 6.6. First, this suggests that using the ratio of Cs-134 to Cs-137 count rates is a predictable method of measuring burnup. Second, it casts further doubt on the accuracy of the activity calibration of the detector.

6.3.6 Fission Gas Release

For a passive measurement of the fractional fission gas release, the best candidates for detection are Kr-85 and Xe-137. Kr-85 has a major gamma emission at 514 keV, whereas Xe-137 decays to Cs-137 whose major emission is at 662 keV. The fission gas release is estimated by comparing the relative activity of an isotope in the fuel region to the gas plenum.

However, because of the lack of a fuel-cladding gap in the VIPAC fuel rod, fission gases released from the fuel microstructure must migrate through the fuel by diffusion to the plenum. Due to the short half life of Xe-137 (3.82 minutes), it is likely that very little of the released gas escaped the fuel before decaying to Cs-137, which would have been solid at the operating temperature and pressure inside of the fuel rod. Therefore, it was considered unlikely that Cs-137 could be used as an accurate measure of fission gas release in this case.

The axial profile of the Kr-85 count rate at 514 keV for each capsule is presented in Figure 6.15. Because this peak was not distinguishable from the 511 keV peak caused by annihilation photons, there is some uncertainty in the magnitude. Although there were not a large number of high-energy gamma rays (able to cause pair production) detected, there
were up to several thousand counts per second above 1 MeV from activation of the spring and other capsule components near the plenum.

![Graph showing net count rate of Kr-85 (514 keV) for each capsule.](image)

Figure 6.15 Net Count Rate of Kr-85 (514 keV) for each Capsule.

Because it has a 10-year half-life, it was assumed that all Kr-85 that was released by fuel discharge had migrated to the plenum by the time of examination. Because the count rate is directly proportional to the atomic abundance, the fractional fission gas release was found using count rates and the length of the fuel and plenum,

\[ FGR = \frac{l_p \cdot c_p}{l_p \cdot c_p + l_f \cdot c_f} \]  

(6.8)

where \( l_p \) is plenum length, \( l_f \) is fuel length, and the average count rates for the plenum and fuel are \( c_p \) and \( c_f \), respectively.

The average net count rate (1/s) in the fuel (positions 2.5 to 9.5) was 757.7 for Capsule 3, and 653.3 for Capsule 4. The average net count rate in the plenum (positions 10.5 and 11) was 24.9 for Capsule 3, and 18.7 for Capsule 4. See Table 6.9 for the calculated fission gas release based on equation (6.8) and properties in Table 6.8.
Table 6.8 Fuel Rod Geometry Based on Fabrication Report [AECL, 2003].

<table>
<thead>
<tr>
<th>Variable</th>
<th>Value</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>$l_p$</td>
<td>capsule 3 = 1.334 (cm)</td>
<td>Plenum length</td>
</tr>
<tr>
<td></td>
<td>capsule 4 = 1.312 (cm)</td>
<td></td>
</tr>
<tr>
<td>$l_f$</td>
<td>capsule 3 = 7.066 (cm)</td>
<td>Fuel length</td>
</tr>
<tr>
<td></td>
<td>capsule 4 = 7.088 (cm)</td>
<td></td>
</tr>
</tbody>
</table>

Table 6.9 Data for Fission Gas Release Estimate Based on Kr-85.

<table>
<thead>
<tr>
<th></th>
<th>Capsule 3</th>
<th>Capsule 4</th>
</tr>
</thead>
<tbody>
<tr>
<td>Average fuel count rate (1/s)</td>
<td>757.7</td>
<td>653.3</td>
</tr>
<tr>
<td>Average plenum count rate (1/s)</td>
<td>24.9</td>
<td>18.7</td>
</tr>
<tr>
<td>Fission gas release (%)</td>
<td>0.617</td>
<td>0.527</td>
</tr>
</tbody>
</table>

The fission gas release estimates for both capsules are below 1%, as is expected in fuel at very low burnup (5-10 MWd/kgU) and irradiated at low temperature (peak fuel temperature below 350°C) [Kazimi et al., 2006]. Furthermore, this release compares well with the fission gas release that was predicted by FRAPCON-VA for this fuel of about 0.6% at 5 MWd/kgU [Yuan et al., 2004].

6.4 Summary

A post-irradiation examination of two prototype I&EC annular fuel rods was completed using facilities built at the MIT Nuclear Reactor Laboratory. A hot box facility was constructed that allowed for remote handling, visual examination, and gamma spectroscopy of the irradiated fuel capsules. After six months of irradiation in the MIT reactor and one year of cooling, the capsules were transferred individually into this facility and counted piecewise axially.

This scanning yielded spatial information on the distribution of radionuclides, and was used to determine the fuel disposition, burnup, and fission gas release. The fuel’s physical
condition appeared unchanged since fabrication; there was no noticeable swelling, densification, voiding, or relocation of fuel grains within the fuel rod.

Although results of the activity calibration of the detector are called into question due to disagreements with the computer predicted activities, burnup and fission gas release estimates based on ratio of count rates were reasonably precise. The experimentally estimated burnup based on the ratio of cesium isotopes was 6.9 MWd/kgU for Capsule 3 and 5.7 MWd/kgU for Capsule 4, compared to 7.32 and 5.64 predicted by the computer, respectively.

The results of the fission gas release estimate based on Kr-85 count rates in the fuel versus the plenum region yielded a release of 0.62% for Capsule 3 and 0.53% for Capsule 4, compared to a computer prediction of 0.6% at a burnup of 5 MWd/kgU. These results suggest that the simple non-destructive PIE was able to determine accurately burnup and fission gas release of irradiated fuel.
7. Silicon Carbide Cladding Irradiation Facility

7.1 Overview

In addition to the computer calculation performed as a part of this thesis, the MIT collaboration in the design of SiC LWR fuel cladding includes irradiation of SiC cladding samples in the MIT reactor (MITR-II). This irradiation facility, planned for insertion in the core in the spring of 2006, is designed to expose the cladding samples to PWR coolant and neutron flux conditions for at least three months before the samples are extracted for initial examination in an MIT hot cell facility. The samples will then be sent for further PIE, including mechanical stress testing, at ORNL.

7.2 Irradiation Loop Design

7.2.1 Irradiation Conditions

In order to provide the characteristic radiation environment that SiC cladding would be exposed to in an LWR core, the cladding samples will be inserted into the MITR-II, a 5 MWth light-water cooled, heavy water reflected thermal reactor. The neutron flux in the core approximates that of an LWR, with peak fast and thermal fluxes of $1 \times 10^{14}$ and $4 \times 10^{13}$ $\text{n/cm}^2\text{-s}$, respectively. The SiC irradiation loop will be inserted into a hollow dummy fuel element located at position B-3 in the middle of the core’s three rings; see Figure 7.1 for the layout of the core with the B-3 location highlighted. Figure 7.2 gives the flux energy profile for the SiC loop in the B-3 position as calculated by MCNP, and Figure 7.3 plots the anticipated axial flux profile based on standard operational experience.
Figure 7.1 Top View of MITR-II Core Showing SiC Loop In-Core Irradiation Location B-3.

Figure 7.2 Axially-Averaged Neutron Flux in Loop In-Core Section Calculated by MCNP.
Besides the appropriate flux, it is also the goal of this irradiation to simulate PWR coolant conditions. By running in a closed loop separate from the primary reactor coolant environment the pressure, temperature, and water chemistry can be adjusted at will to suit this purpose. In the loop, the inlet water temperature will be 300°C at a pressure of 1500 psi, and at a mass flow rate of 0.25 kg/s; the primary reactor coolant runs at a core outlet temperature of 50°C and at atmospheric pressure. Because the heated portion of the loop is insulated by a CO₂ gas gap, a temperature drop of not more than 20°C is expected over the length of the in-core section.

Any free oxygen in the water is removed by bubbling H₂ through the coolant makeup water tank, and the dissolved O₂ and H₂ levels are constantly monitored. In addition, to follow standard mid-cycle reactivity control practice, boric acid will be added to the water to achieve 800 ppm of boron. For pH control, 2.2 ppm of lithium is also included as lithium hydroxide.
7.2.2 Structural Design

The SiC irradiation facility consists of three main sections: the in-core assembly, the heating and flow control, and the let-down chemistry system. In addition, there is a data acquisition system that continuously records temperature, pressure, and chemistry indicators from the loop and the support equipment.

The in-core section consists of the sample assembly, autoclave, and thimble. The sample assembly is made up of any number of individual modules, each holding three to nine SiC cladding samples. An example of a three-layer module is shown in Figure 7.4. A majority of the specimens will be open-ended tubes like those shown this figure, and therefore those modules are held together by tie-rods running through the center of the specimen tubes. There are small radial holes in these tie-rods to allow some coolant to flow along the center of the samples in order to prevent coolant stagnation. For those samples with joined end caps, the modules are secured with a hollow central tie-rod.

![Figure 7.4 Sample Module Containing Three Layers with Three SiC Tubes in Each Layer.](image)

These modules stack onto a spine rod that hangs down from a fitting at the base of the water outlet pipe. Two feet above the in-core section is a second set of modules
containing more SiC samples. These tubes will be exposed to the same coolant conditions as the in-core samples but without the neutron flux.

Surrounding and in the space between each module are flow shrouds, as shown in Figure 7.5, which separate the flow into two channels. When the sample assembly is placed inside of the autoclave, a 1.57 inch OD titanium tube, coolant enters the top of the autoclave, flows down around the outside of the modules, then up through the inside of each module before returning through the reactor lid. A diagram of this flow pattern is shown in Figure 7.6.

![Diagram of in-core section of SiC irradiation loop with four modules.](image)

**Figure 7.5 In-Core Section of SiC Irradiation Loop with Four Modules.**
Outside of the autoclave is an aluminum thimble that provides separation between the reactor coolant and the autoclave surface. The in-core section of the thimble is sealed, and at the top is a large annular cylinder filled with lead into which the autoclave top bolts. There is a gap between the thimble and autoclave filled with CO₂ at about 1 psig. When fully inserted in the core, the thimble fits into a hollow dummy fuel assembly as shown in Figure 7.7, with the lead-filled end resting in a port in the reactor lid and supporting the entire weight of the experiment.

Because the experiment is not locked into the core as are ordinary fuel elements, in order to prevent ejection from the core tank the thimble is held down by steel “L” brackets.
These brackets attach to the surrounding modular lead shielding designed for this experiment. This shield, which weighs approximately 225 kg, is in turn bolted to the reactor lid, which is held in place with several large manual hold-down clamps.

![Diagram of the control system](https://via.placeholder.com/150)

Figure 7.7 SiC Irradiation In-Core Loop in Position B-3 in Simplified MITR-II Core Tank.

The out-of-core systems are consistent with those used in past experimental loops in the MITR-II for simulating PWR and BWR conditions. A diagram of the control system is given in Figure 7.8.
The inlet coolant feed to the loop runs from the top of the autoclave, across the reactor lid, and into the heater. In the heater, the pipe runs through a lead bath that heated by a series of electric strip heaters. The outlet pipe mirrors the inlet flow from the autoclave top until it reaches the heater box, where about 10% of the flow is diverted into the let-down loop. This let-down flow travels through a regenerative and then a non-regenerative heat exchanger (cooled by the reactor secondary coolant system) before moving through a pressure regulator. Now at atmospheric pressure, this flow travels to the chemistry monitoring system and hydrogen re-combiner before emptying into the makeup water tank, where \( \text{H}_2 \) is bubbling through. The outlet from this tank goes through a charging
pump and a regenerative heat exchanger before rejoinder the main loop flow just after the circulation pump. This re-combined flow then goes into the heater section. Some water is also constantly drawn from the makeup tank, filtered, and run through a secondary chemistry monitoring array before being returned to the tank.

There is also a pressurizer attached to the let-down system after the heat exchangers to prevent boiling in the loop. The temperature in the pressurizer is maintained above that of the main loop, so in the event of sudden depressurization of the main loop the water will boil in the pressurizer first. This vaporization will slow the rate of pressure drop in the main loop as the heaters are deactivated.

Water in the loop is highly radioactive during reactor operation due to the O-16 (n,p) reaction, which forms N-16. This N-16 decays with a half-life of 7.1 seconds and emits a 6 MeV gamma, requiring lead shielding. The main coolant loop, including the circulation pump and heaters, is therefore enclosed in specially designed modular shielding. The let-down flow is first separated under this shielding, and travels through a looping section of pipe to allow time for the N-16 decay before exiting the shielding for the heat exchangers and chemistry monitoring equipment.

The loop shielding consists of two major sections: modular pieces to cover sections of pipe running across the reactor lid, and a permanent shield coffin surrounded by lead plates and bricks to house the heaters, circulation pump, and initial let-down line loops.

7.2.3 Safety Review
Following normal experimental procedure, an extensive safety review was conducted to evaluate the impact of the SiC loop on the operation of the MITR-II. This review included a description of the systems and their operation as described here, as well as calculations of the reactivity worth of the experiment and expected activation [MIT-NRL, 2006].
The reactivity worth of the SiC irradiation loop was calculated using a full core model of the MITR-II in MCNP. Based on this analysis, the total reactivity worth of the in-core section of the loop was determined to be -437 ±53 mβ (where error is quoted as one standard deviation) compared to a solid aluminum dummy fuel element in that core position. This is well below the single-experiment reactivity limit set by the reactor technical specifications at 2290 mβ.

There is also an accident scenario reactivity transient qualification. In case of voiding of all of the water initially present in the in-core section of the loop, followed immediately by re-flooding with room-temperature water at atmospheric pressure, the maximum reactivity change was calculated to be 96 ±103 mβ. This falls within the specified non-secured reactivity limit of 636 mβ, and therefore the SiC irradiation loop meets the in-core experiment reactivity safety guidelines.

Based on the expected elemental loading of the SiC irradiation loop, given in Table 7.1, the activation of the loop components was calculated using ORIGEN. This calculation was based on the planned 90-day irradiation at an anticipated 75% capacity factor. This capacity factor is based on the probability of unplanned shutdowns and expected refueling and maintenance outages. After shutdown, there is an additional three-day decay period required before the experiment can be manipulated; this is therefore the minimum decay time analyzed in the code. The results of this activation analysis are given in Table 7.2, where the total activity was found to be 33.9 Ci. This activity is primarily due to neutron capture in the aluminum and fast neutron reactions in the titanium.
Table 7.1 Elemental Loading the SiC Irradiation Loop.

<table>
<thead>
<tr>
<th>Element</th>
<th>Internals (g)</th>
<th>Thimble (g)</th>
<th>Autoclave (g)</th>
<th>Total (g)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Al</td>
<td>16.1</td>
<td>844</td>
<td>16.1</td>
<td>876</td>
</tr>
<tr>
<td>Cr</td>
<td>-</td>
<td>3.08</td>
<td>-</td>
<td>3.08</td>
</tr>
<tr>
<td>Cu</td>
<td>-</td>
<td>3.52</td>
<td>-</td>
<td>3.52</td>
</tr>
<tr>
<td>Fe</td>
<td>-</td>
<td>6.15</td>
<td>-</td>
<td>6.15</td>
</tr>
<tr>
<td>Mg</td>
<td>-</td>
<td>10.5</td>
<td>-</td>
<td>10.5</td>
</tr>
<tr>
<td>Mn</td>
<td>-</td>
<td>1.32</td>
<td>-</td>
<td>1.32</td>
</tr>
<tr>
<td>Si</td>
<td>70.7</td>
<td>7.03</td>
<td>-</td>
<td>77.7</td>
</tr>
<tr>
<td>Ti</td>
<td>507</td>
<td>1.32</td>
<td>506</td>
<td>1014</td>
</tr>
<tr>
<td>V</td>
<td>13.4</td>
<td>-</td>
<td>13.4</td>
<td>26.8</td>
</tr>
<tr>
<td>Zn</td>
<td>-</td>
<td>2.20</td>
<td>-</td>
<td>2.20</td>
</tr>
</tbody>
</table>

Table 7.2 Activities for SiC Irradiation Loop after 1620 Hours (90 EFPD with 75% Capacity Factor) and 3 Days of Cooling.

<table>
<thead>
<tr>
<th>Precursor</th>
<th>Abundance (%)</th>
<th>Reaction</th>
<th>Daughter Nuclide</th>
<th>T&lt;sub&gt;1/2&lt;/sub&gt;</th>
<th>After cooling (Ci)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Thermal neutron capture reaction</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Cr-50</td>
<td>4.35</td>
<td>(n,γ)</td>
<td>Cr-51</td>
<td>27.7 d</td>
<td>11.54</td>
</tr>
<tr>
<td>Cu-63</td>
<td>69.17</td>
<td>(n,γ)</td>
<td>Cu-64</td>
<td>12.7 h</td>
<td>1.23</td>
</tr>
<tr>
<td>Fe-54</td>
<td>5.85</td>
<td>(n,γ)</td>
<td>Fe-55</td>
<td>2.7 a</td>
<td>0.12</td>
</tr>
<tr>
<td>Fe-58</td>
<td>0.282</td>
<td>(n,γ)</td>
<td>Fe-59</td>
<td>44.5 d</td>
<td>0.09</td>
</tr>
<tr>
<td>Ti-50</td>
<td>5.18</td>
<td>(n,γ)</td>
<td>Ti-51</td>
<td>5.76 m</td>
<td>2.18</td>
</tr>
<tr>
<td>Zn-64</td>
<td>48.6</td>
<td>(n,γ)</td>
<td>Zn-65</td>
<td>244 d</td>
<td>0.78</td>
</tr>
<tr>
<td>Total activity from thermal capture reactions:</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td>15.94</td>
</tr>
</tbody>
</table>

| Fast neutron reactions |
|------------------------|----------|----------|------------------|----------|------------------|
| Al-27                  | 100      | (n,α)   | Na-24            | 14.99 h  | 0.84             |
| Fe-54                  | 5.85     | (n,p)   | Mn-54            | 312 d    | 0.07             |
| Ti-46                  | 8.25     | (n,p)   | Sc-46            | 83.8 d   | 5.35             |
| Ti-47                  | 7.44     | (n,p)   | Sc-47            | 3.4 d    | 10.11            |
| Ti-48                  | 74       | (n,p)   | Sc-48            | 162.7 d  | 1.54             |
| Total activity from fast capture reactions: | | | | | 17.91 |

Because hydrogen gas is bubbled through the makeup tank for oxygen control, it is necessary to minimize the explosive hazard. With this in mind, the level of the makeup tank is required to be maintained near full in order to minimize the gas pocket at the top of the tank. In addition, in order to minimize the amount of H<sub>2</sub> present within the reactor containment building at any time, a small gas cylinder is used at the reactor top. This ensures that, in addition to the H<sub>2</sub> in the loop, the experiment contains less than the
imposed limit of 10 SCF (based on safety analysis of previous in-core loops [Kohse et al., 1993]).

7.2.4 Procedures

The assembled stack of sample modules is attached via the spine rod and coolant outlet fitting to the autoclave top. This stack is then inserted into the body of the autoclave, and it forms a high-pressure seal at the top utilizing a C-ring. The autoclave is then inserted into the thimble and is bolted down to the lead annulus with a gasket in between to form a low-pressure seal for the CO$_2$ gas gap.

The reactor is prepared by placing the hollow dummy element in position B-3 and rotating the proper lid plug into position over this element. After the autoclave is filled with water, the assembled experiment is lifted using a crane into position above the open lid port and lowered into the dummy assembly in the core. The gas and coolant feed lines are attached and the system is then brought up to the operating pressure. After checking for leaks at the reactor top, the exposed piping is insulated and the system is brought up to temperature; the loop is allowed to operate for at least 18 hours before the reactor is brought back online.

Before reactor operation, the modular shielding is put into place around the piping and additional shielding is added around ion filters and pumps. Reactor power is then raised in small steps with additional shielding is added as necessary.

On-line monitoring of the loop is accomplished with several sets of independent controllers and alarm boxes. A data acquisition computer at the reactor top logs the temperature of the system at eight locations, pressure in the loop as well as the pressure drop across the circulation pump, the dissolved O$_2$ and H$_2$ concentrations, and the water’s electrical conductivity. Additional remote controllers monitoring separate feeds of the temperature of the loop’s core outlet flow, the lead bath temperature, and the pressurizer temperature will automatically shut off the heaters due to high temperature.
Pressure switches on the loop indicating low or high system pressure will activate dedicated alarms in the reactor control room, as will low pressure drop across the circulation pump, high or low loop core outlet temperature, low makeup tank water level, low flow or high temperature in the loop secondary cooling system, or loss of pressure in the CO\textsubscript{2} gap.

After the irradiation is complete and the system is cooled and depressurized, the loop is removed from the core in two steps. First, the autoclave top is detached from the autoclave tube and is moved, along with the sample modules, in a cask to the hot cell adjacent the reactor. The sample stack, suspended from the autoclave top by the spine rod and coolant tube are then lowered into the cell. The autoclave top rests in a special bracket secured above the cell to the wall of the reactor containment building. The autoclave and thimble can then be removed from the core and placed in storage either in a cask or into another port in the hot cell top adjacent to the sample assembly.

The hot cell, shown in Figure 7.9, is a larger facility than the hot box used for the I&EC annular fuel capsule examination. It provides ports on top so that the sample assembly may be suspended in the cell, provides a tabletop area for working, and has wall ports for removing samples. Once in the hot cell, the modules will be removed from the spine rod and sorted. If irradiation of this experiment will continue, those modules slated for continued exposure will be put back in place with the addition of new modules with fresh SiC samples. The other modules will be disassembled in the cell, and the SiC tubes examined.
The specimens will be photographed and weighed, then baked to remove moisture. After baking, the samples will be re-weighed to determine if any weight loss or gain has occurred. Once no further weight loss can be achieved by baking, the final weight of the specimens will be recorded. At the conclusion of the physical examination, and after allowing sufficient time for decay, these SiC tubes will be packaged and shipped to ORNL for mechanical testing.

7.3 Rig Construction and Startup

7.3.1 Sample Characterization and Loop Assembly
Gamma Engineering sent twenty-four samples to MIT for irradiation in the SiC loop. This includes seventeen duplex tubes with several different fiber, matrix, and monolith constructions, three duplex tubes with joined end caps, two tubes with only the SiC monolith layer, and two Zircaloy tubes for comparison.
Prior to assembly, each sample was measured, weighed, and compared to the as-manufactured data provided by Gamma Engineering. This test showed good agreement for all the samples. They were then baked in batches of eight at $120^\circ$C for one hour and then re-weighed. For the first batch, no weight change was measured after the first hour, and a second hour of baking also produced no discernable change. Therefore, all other specimens were baked for one hour, then weighed and assigned their official pre-irradiation weight. The results of these measurements are given in Appendix A.

A sample matrix, provided in Appendix A, was prescribed by Gamma Engineering that assigned the axial position of each sample. Because of slight variations in the sample lengths, they were also sorted by height such that given three samples per layer, each axial column of samples within a module would have a nearly equivalent height. Because the height of the entire module (generally containing multiple samples stacked on each other) is set by the flow shroud height, this prevents excess contact with the titanium platters supporting either end of the specimens. During irradiation, this contact may cause damage to the ends of the tubes. A partially assembled module exhibiting the various common components is pictured in Figure 7.10.
Prior to assembly of the loop, four tube fragments representing a cross section of the materials within the SiC specimens were studied using neutron activation analysis. The purpose of this testing was to identify any unexpected contaminants and to provide an estimate of the activity of the actual SiC samples after irradiation. This information is important for determining proper radiological controls as well as for setting a schedule for examination and shipping.

These specimens were irradiated, together with a NIST standard, in a pneumatic rabbit positioned in the reflector region near the MITR-II core centerline for ten hours. After several days to allow for decay of short-lived isotopes, the fragments underwent gamma
spectroscopy with a three-hour count for each specimen. Following an addition two-week decay to reduce the background, each fragment was counted again for twenty-four hours. The results of this spectroscopy analysis are presented in Appendix B.

### 7.3.2 Loop Installation and Startup

The loop was successfully pressure tested up to 1700 psi at room temperature before being installed in the reactor. The loop inserted in the dummy fuel element in the MITR-II core is shown in Figure 7.11; the thimble and autoclave top seated in the reactor lid port is shown in Figure 7.12. It was then connected to the inlet and outlet coolant pipes, the system was brought to the operating pressure of 1500 psi, and the circulation pump was started.

![Figure 7.11 SiC Irradiation Loop Installed in the MITR-II Core.](image-url)
During this initial testing, helium gas was substituted for the H₂ in the bubbling system. The reactor top pressure and water chemistry monitors are shown in Figure 7.13. After several days, the oxygen content of the coolant was reduced from an initial value of about 6 ppm to less than 0.4 ppb. At this point, the heaters and pressurizer were activated from the controller pictured in Figure 7.14. The temperature was then raised in increments by increasing the power output to the heaters towards the operation temperature of 300°C.
Figure 7.13  SiC Irradiation Facility Pressure and Water Chemistry Monitors.
However, when the coolant temperature reached $230^\circ$C, the circulation pump, which utilizes a magnetic coupling between the motor and the pump impeller, de-coupled and the loop coolant stopped circulating. Temperature was decreased slightly and the pump was restarted, however it was found to de-couple consistently at this temperature. Through subsequent testing and rebuilding of the pump, it was determined this was likely due to thermal expansion of the graphite bearings on the impeller shaft, which was causing the shaft to jam in its housing. By raising the temperature just below the decoupling point and running for several hours, it was found the maximum temperature could then be increased further as the bearings experienced mechanical wear.

By repeating this process, the loop was brought up to the correct temperature for operation. However, because this situation prevented normal operation of the experiment
and might expose the samples to unusual temperature and pressure effects, the in-core
section was removed and placed in storage in the hot cell until the pump was properly
conditioned. With the mechanical systems now functioning properly, the in-core section
will be re-inserted in the reactor at the next scheduled shutdown so the irradiation may
begin.
8. Summary and Recommendations for Future Work

8.1 SiC Cladding Modeling in FRAPCON

SiC-based materials are being considered for use as fuel rod cladding for LWRs. The proposed form is a duplex, with a solid SiC monolith tube as the inner cladding layer to retain fission gas, and a woven SiC fiber-matrix composite forming the outer layer for strength.

The SiC has advantages over traditional Zircaloy cladding that include a very high melting point, negligible creep at LWR temperatures, good irradiation stability, a low oxidation rate, and high strength. These properties may allow SiC-clad fuel rods to be run at higher power densities and to higher fuel burnup than is achievable with current Zircaloy-clad fuel rods.

As a result of this work, the FRAPCON steady-state fuel modeling code was equipped with appropriate properties to simulate a fuel rod with SiC composite cladding. The first version that was produced, FRAPCON-SiC, combined previous work addressing the FRAPCON very high burnup fission gas release relations with new models for the cladding. These models described the thermo-physical properties of SiC composite cladding in relation to pertinent conditions such as neutron fluence, temperature, and stress.
The new models are concerned with the following SiC composite properties:

- Thermal Conductivity
- Thermal Expansion
- Irradiation Swelling
- Young's Modulus
- Yield Strength
- Shear Modulus
- Creep
- Oxidation
- Emissivity
- Phase Transitions
- Crud Accumulation

Each of these models was based on data available in the literature for SiC composites, and those composites using new, radiation-resistant fiber types when available.

This new code was then run using input cases representing normal burnup at constant power, and very high burnup (up to 100 MWd/kgU) starting at a high LHGR. These cases highlighted the performance characteristics of SiC and how they contrast with those of traditional Zircaloy cladding. In general, the lower thermal conductivity and absence of creep are the most significant properties. The un-irradiated SiC thermal conductivity may be large compared to Zircaloy, however it quickly degrades with radiation exposure to a saturation value around 4 W/m-K. This lower thermal conductivity increases the temperature drop across the cladding, and thereby increases the fuel temperature.

Because the SiC cladding does not creep down onto the fuel, there is no pellet-cladding interaction even at high burnup. However, this also implies the fuel-cladding gap remains open longer. This gap creates an additional thermal resistance that further increases the fuel temperature. The primary consequence of the higher fuel temperatures is increased fission gas release leading to increasing pressurization of the fuel rod. However, because
SiC does not creep at typical operating temperatures and maintains high strength even after a significant amount of radiation damage, the SiC-clad fuel rod performs better than Zircaloy when the internal pressure exceeds the external coolant pressure.

The next step was the application of SiC cladding to a new fuel geometry, the I&EC annular fuel developed at MIT. Previously, two versions of the FRAPCON code had been created to model this fuel rod geometry with either sintered annular fuel pellets or VIPAC fuel. The VIPAC fuel consists of sintered UO₂ grains of various sizes that are packed into the fuel rod, thus eliminating fuel-cladding gaps and allowing simplified fabrication, especially for non-standard geometries. These codes were modified with very high burnup fission gas release models incorporated into FRAPCON-SiC, and then the SiC cladding properties were included as well to create two new versions of the code, one for each fuel type.

The SiC-clad I&EC annular fuel was then evaluated under normal PWR conditions and at 150% normal power to very high fuel burnup (100 MWd/kgU). The primary differences in behavior between these fuel forms are the potential gap asymmetries present in a sintered annular pellet opposed to the zero gap in VIPAC fuel rods. However, the VIPAC fuel is in constant mechanical contact with the cladding, and experiences somewhat higher fission gas release due to the large surface area of the fuel grains.

8.2 Fuel Performance Enhancement

8.2.1 Possibilities for Power Density Increase
One of the possible advantages of SiC cladding is the ability to increase the fuel power density. Increasing power density is desirable because it permits an increase the power output of a reactor while maintaining the existing fuel assembly and core geometry. For transmutation fuel, this is equivalent to increasing the rate of actinide destruction. Comparing the performance of SiC and Zircaloy cladding in the normal pin-type fuel rod
geometry at high power densities indicates that higher fuel temperatures may be the limiting factor, since thermal safety margins must be maintained.

This issue could be mitigated by several different approaches, two of which were considered here: changing the initial fill pressure, and using annular fuel pellets. With only a 5 v/o central void, annular pellets provide a significant reduction in peak fuel temperature (by almost 200 K at beginning of life in the high power density case studied), which can be increased with increasing void. The combination of reduced peak fuel temperature and added void space in these rods also greatly reduces the pressurization due to fission gas release.

Changing the initial fill gas pressure may also provide some benefit. Increasing the initial pressure improves the gap thermal conductance, important for SiC because of the lack of creep. However, this benefit must be balanced by accepting a higher internal pressure over the operating life of the fuel rod.

Replacing the UO₂ with MOX fuel, temperature remains the dominant issue. The thermal conductivity of the MOX fuel is lower than the UO₂, therefore peak fuel temperatures tended to be higher. Because of this, the MOX fuel rods experienced significantly higher fission gas release and plenum pressurization than UO₂ fuel rods under the same operating conditions.

Applying SiC cladding to I&EC annular fuel presents a new opportunity for power density increases. The average cladding and fuel temperatures are 100-150 K higher with SiC instead of Zircaloy cladding, however the overall performance is identical or better, especially in the case of sintered annular pellets. Because the SiC cladding is more rigid than the Zircaloy, the fuel-cladding gaps remain more symmetric and the heat flux through the inner and outer surfaces remains balanced, an important safety consideration.

Even at 150% of nominal PWR power, the maximum fuel temperature with the I&EC annular fuel with SiC cladding is half that of the comparable solid pin with Zircaloy
cladding. This leads to very low fission gas release even at very high burnup (< 10% at 100 MWd/kgU), little fuel rod pressurization, and excellent thermal safety margins.

8.2.2 Achieving High Fuel Burnup

Increasing the fuel burnup maximizes the amount of energy that is extracted from the fuel before it is discharged from the reactor, which minimizes the mass of actinides that must be disposed of or reprocessed. The challenges of reaching high burnup include preventing over-pressurization of the fuel rod, excessive cladding strain, excessive fuel-cladding mechanical interaction, and maintaining acceptable thermal margins. It is difficult to use Zircaloy cladding at high fuel burnup because the swelling fuel strains and wears at the cladding, and high fission gas release at very high burnup can cause bloating of the fuel rod.

As discussed previously, SiC cladding with the traditional solid fuel rod geometry reduces the fuel-cladding interaction even to very high burnup because the fuel-cladding gap only closes as the fuel pellets swell outwards. In addition, within the stress limitations, pressurization of the SiC cladding beyond the external coolant pressure does not result in outward strain. The major issue with this cladding remains the peak fuel temperature, although it is reduced considerably at high burnup as the gap closes and the fuel pin LHGR is decreased.

Using I&EC annular fuel rods with SiC cladding gives the additional advantage of very low fission gas release, which maintains the SiC hoop stress well below the limit up to very high burnup. This annular design also keeps the peak fuel temperature significantly lower than solid fuel at a comparable LHGR, which could address concerns for transmutation fuels like MOX, which have lower thermal conductivity than UO₂.
8.3 I&EC Annular Fuel Irradiation Results

Two prototype I&EC VIPAC annular fuel rods were irradiated in the MIT reactor for six months, then examined in a hot box facility at the MIT Nuclear Reactor Laboratory. These non-destructive examinations included evaluation of the fuel disposition and estimates of the fuel burnup and fission gas release.

From the results of the gamma spectroscopy performed in piecewise intervals along the axis of each fuel rod, the fuel form appeared to retain its physical dimensions. There was no evidence of migration, densification, or voiding within the fuel rod, and the burnup appeared uniform along the length of the fuel. Although attempts to calculate the activity of various isotopes based on spectroscopy yielded questionable results, fuel burnup calculations relying on the ratio of cesium isotopes were more consistent with previous simulations. For Capsule 3, the burnup estimated by the ratio method was 6.9 MWd/kgU compared to 7.32 MWd/kgU calculated by MONTEBURNS; for Capsule 4 the estimated fuel burnup was 5.7 MWd/kgU compared to 5.64 MWd/kgU calculated. These experimentally derived results are within 6% of the values calculated by MONTEBURNS.

The fission gas release calculation, which used the Kr-85 count rates in the fuel and plenum region, yielded similarly consistent results. The measured fission gas was 0.62% for Capsule 3 and 0.53% for Capsule 4, whereas the release calculated by FRAPCON for a burnup of 6 MWd/kgU is 0.6%.

Overall, the results of the examination verified the integrity of the I&EC annular fuel rods, and confirmed the anticipated behavior of the VIPAC fuel. Furthermore, the good agreement between the calculated and measured fuel burnup and fractional fission gas release demonstrates the ability to use non-destructive techniques to characterize this irradiated fuel form appropriately.
8.4 Silicon Carbide Cladding Irradiation

An experiment has been planned at MIT to investigate the performance of SiC duplex cladding under PWR conditions. Small samples of SiC duplex cladding tubes have been placed in an irradiation loop that will be installed in the core of the MITR-II. The reactor will provide a typical PWR flux spectrum, while controls on the loop will ensure appropriate pressure, temperature, and water chemistry conditions during the irradiation.

The SiC samples will be irradiated for a few months followed by several days for cooling and decay before they are transferred into a hot cell at the MIT Nuclear Reactor Laboratory. This facility will allow some of the samples to be removed from the loop and examined. Other SiC tubes, along with new samples, may be re-installed in the experiment and returned to the core for additional irradiation.

After irradiation, these samples will undergo PIE first at MIT before being sent to ORNL for mechanical testing. At MIT, the samples will be examined for dimensional and weight changes as well as any other significant macroscopic effects. Because these samples represent several different SiC fabrication and assembly techniques, it is hoped this irradiation will provide information on which type to pursue for further development and testing.

8.5 Possibilities for Future Work

8.5.1 Additional FRAPCON Development and Other Computer Modeling

Based on the SiC cladding performance modeling so far, it is important to refine the empirical relations that have been developed, especially concerning the thermal conductivity and the strength of the composite. In particular, the models should include the effects of particular fiber and matrix compositions and assemblies.

The effect of oxidation and other chemical interactions on the SiC is also of particular interest; it was omitted from this investigation due to inconclusive information. However,
as more data becomes available through experiments on new SiC materials, such as the irradiation at MIT, a more informed conclusion can be reached as to whether the effect is significant enough to include in this code.

A significant advancement to the current modeling scheme would involve including explicitly both the monolith and the composite layers of the SiC duplex cladding. This involves the complexity of the mechanical bonding between the two layers, heat transfer at the interface, and distribution of stresses. This will also involve developing models to explicitly deal with the monolith material properties.

Additional work is also needed to explore the incorporation of SiC cladding with different transmutation fuels. SiC cladding in combination with I&EC annular fuel rods demonstrated the ability to achieve higher power densities and very high fuel burnup than current fuel rods, and therefore this construction may be advantageous for increasing the efficiency of thermal transmutation. It would be valuable to first extend the MOX fuel model into this geometry, since the MOX fuel models that have already been developed for FRAPCON could then provide an indication of the performance of other actinide-bearing fuel.

Because the simplified fabrication process for VIPAC fuel makes it a preferred form for reprocessed fuel, a VIPAC annular fuel simulation with SiC cladding and MOX fuel would be an instructive FRAPCON model to explore. Other transmutation fuel types under development, such as inert matrix fuels, may also be included by incorporating their properties into the FRAPCON.

In addition, the evaluations thus far have concerned steady-state and long-term thermomechanical behavior, but transient analysis will be equally important. In the same way FRAPCON has been modified for SiC cladding, transient codes such as FRAPTRAN may be utilized to perform analysis of events such as reactivity insertion accidents. Thermal-hydraulic and neutronic analysis of SiC cladding will also be necessary for
steady-state and transients, especially in exploring the results of the differences in cross sections, temperature, strain behavior, and surface texture.

### 8.5.2 Experimental Studies

In order to validate the predictions of FRAPCON, it will be important to perform more irradiations on duplex cladding samples under LWR conditions. Although irradiation studies have been conducted on the fiber, monolith, and matrix material independently, observing their interplay under irradiation and stress will provide important experimental data.

Future irradiations should also provide the internal pressure and heat flux that will test the SiC cladding performance in more realistic conditions. The pressure and heat flux may exacerbate conditions that have gone unnoticed until now, especially in consideration of the desire to achieve very high burnup with this material. Studies will need to evaluate the behavior after an effective irradiation time of at least six years, in which case behavior such as crack propagation, fretting, and thermal cycling become more apparent.

Experimental testing will also be needed to study the transient behavior of the cladding, such as exploring the effects of over-pressurization, vibration, and thermal shock. A study of the chemical reactions between the parts of the SiC cladding and the LWR coolant and the fuel pellet under normal and accident conditions will also be needed to determine the effect these processes have on the cladding's mechanical performance and heat transfer.
REFERENCES


Chang, J., KAERI Table of Nuclides, Korea Atomic Energy Research Institute, Online, 2005, http://atom.kaeri.re.kr/ton


Zhao, Jiyun, Personal Communication, 2005.


## APPENDIX A:
SiC Irradiation Sample Data

Table A.1 Description of SiC Irradiation Loop Specimens.

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Table A.3 Pre-Irradiation Specimen Dimensions.
Table A.4 Pre-Irradiation Specimen Weights.

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APPENDIX B:
SiC Neutron Activation Analysis Data

Table B.1 Neutron Activation Analysis Sample Descriptions.

<table>
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<tr>
<th>Sample</th>
<th>Monolith</th>
<th>Fiber Type</th>
<th>Weight (g)</th>
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<td>C1</td>
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<td>Hi-Nicalon Type-S</td>
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<td>G2</td>
<td>CoorsTek</td>
<td>Hi-Nicalon Type-S</td>
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Table B.2 Elements Identified in Sample C1.

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<td>Concentration (µg/g)</td>
<td>1 σ (µg/g)</td>
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<tr>
<td>Fe</td>
<td>212.2960</td>
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</tr>
<tr>
<td>Ba</td>
<td>195.0690</td>
<td>11.9050</td>
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<tr>
<td>Na</td>
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<td>Zn</td>
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Table B.4 Elements Identified in Sample F1.

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