Comparison of Pellet-Cladding Mechanical Interaction for Zircaloy and Silicon Carbide Clad Fuel Rods in Pressurized Water Reactors

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Abstract

Contact between the outer surface of a fuel pellet and the inner surface of the fuel rod cladding can result in several kinds of undesirable behavior. As the pellet swells outward and the cladding creeps down, large stresses are generated in both the pellet and the cladding. This contact may exacerbate chemical degradation of the cladding and the stress may enable rapid propagation of cracks. Because maintaining the cladding as a barrier to fission gas release is necessary for reactor operation, it is important to have accurate models of the pellet-cladding mechanical interaction. This work analyzes the predictions of a standard fuel rod modeling code utilizing a rigid pellet assumption, and compares these results to a model allowing elastic deformation of both the pellet and the cladding. These models are also used to compare two claddings with significantly different mechanical behavior: a traditional Zircaloy metal cladding and a silicon carbide ceramic cladding.

Introduction

As manufactured, a roughly axisymmetric gap exists between the outer radius of the cylindrical UO_2 fuel pellets and the inner surface of the cladding in current light water reactor fuel rods. This gap, usually several tenths of a millimeter thick, forestalls significant mechanical interaction between the fuel pellets and the cladding during normal reactor operation. However, it is important to be able to analyze the onset and consequences of pellet-cladding mechanical interaction (PCMI), should it occur, as it may severely challenge the integrity of the cladding.

The fuel pellet and cladding behavior is governed by time, the external reactor core environment, and the feedback between the state of the pellet and the cladding. Depending on the scale of system being considered, different models have been developed to predict pellet and cladding behavior, and in particular the consequences of PCMI. In the example studied here, the FRAPCON fuel rod modeling code is used to predict the behavior of a UO_2 fuelled fuel rod with either Zircaloy or silicon carbide (SiC) cladding.

The FRAPCON code is primarily concerned with a conservative analysis of the fuel rod, and therefore incorporates a simple model of pellet expansion and cladding response. In this study, the effects of stress feedback on pellet behavior are explored, and the results are compared to the FRAPCON predictions. The choice of cladding materials is interesting as Zircaloy is a ductile metal and SiC is a brittle ceramic, therefore the response to mechanical interaction with the cladding may be significantly different.

A particular point of interest is the change in calculated cladding strain when a more elaborate pellet deformation model is considered. This relates directly to failure of the cladding due to stress beyond the yield or fracture strength of the material. The state of stress in the cladding is also an important input into more complex PCMI issues, such as stress corrosion cracking of the cladding.

Causes of PCMI

The initial gap between the fuel pellets and the cladding is needed to allow loading of the pellet stack into the cladding tube during fuel rod manufacturing. Typical radial clearance between the oxide pellet and the cladding inner radius at this initial cold state is 200 to 400 microns. This gap space, connected to the plenum at the top of the fuel rod, is then back filled with helium to an initial pressure of 1 to 3 MPa for pressurized water reactor (PWR) fuel.

After insertion in the core and startup, both the pellet and cladding grow due to thermal expansion. This addition of heat has significant implications for both the cladding and the pellet. For a Zircaloy cladding with a typical service temperature of 600 K, creep becomes an important consideration. The external coolant pressure is about 15 MPa, therefore there is a significant stress driving cladding creep-down onto the pellet.

For the pellet, the initial heat up causes both thermal expansions leading to cracking as well as densification due to additional sintering of the pellet. The cracking is a consequence of the stresses induced in the pellet due to differential thermal expansion; the temperature is highest in the center of the pellet and decreases in a logarithmic manner towards the edges. Therefore, compared to some mid-radius cylinder of average temperature, the central region will try to expand more and will be held in compression, while the outer layers experience less thermal expansion and will be held in tension.

The thermal stress in the cylindrical pellet under elastic strain behavior can be related to q', the linear heat rate, by,

$$\sigma_t = \frac{E\alpha q'}{16\pi k(1-\nu)} \tag{1}$$

where E is the elastic modulus of the fuel, α is the thermal expansion coefficient, k is the thermal conductivity, and ν is Poisson's Ratio. This stress must be considered locally within the pellet as it applies to hoop, radial, and axial stresses, but in general, the stress intensity will be largest at the outer circumference. Typical beginning of life values for the quantities in (1) are given in Table 1 based on formulas given in FRAPCON sub-codes. Given these values and using the Tresca maximum shear strength theory of failure, the predicted stress intensity at the pellet outer surface is 140 MPa, compared to a fracture strength of 125 MPa predicted by the FRAPCON code. Therefore, pellet cracking will occur almost immediately after reactor startup.

Table 1. Parameters for calculating pellet thermal stresses from FRAPCON.

Property	Value (for BOL, T_{fo}= 800 K)
E	130 GPa
α	9.9 μm/m-K
q'	20 kW/m
k	5.4 W/m-K
ν	0.316

These fractures due to thermal expansion are predominantly radial cracks due to the circumferential stress, and they will tend to propagate towards the center of the pellet. These cracks also spread axially along the length of each pellet, eventually producing pie-shaped pellet fragments. It can be shown through analysis of the stresses caused by this cracking that the resultant pellet wedges will relocate radially outward. [1]

Simultaneously, due to the high temperatures at the center of the pellet, the pellet will undergo additional sintering, and its density will approach the theoretical UO_2 density from an initial manufactured density of about $0.95\rho_{TD}$. This densification, combined with the high thermal stresses at the center of the pellet, will tend to create a central void. These effects combine to relocate the pellet fragments outwards toward the cladding. Early in life these processes may result in closing of the fuel-cladding gap, however this is not considered a cause of significant PCMI.

The contact pressure between the relocated pellet fragments and the cladding is low, and in general as the cladding creeps down the pellet fragments will be compressed, closing some of the initial cracking. Parts of these cracks may eventually heal as a result of the high temperatures near the center of the pellet. PCMI will ultimately occur if cladding creep down continues for a long period of time, however other mechanisms, such as pellet expansion, may accelerate this process.

The main driver of pellet expansion after the startup period is fission product swelling. Solid and gaseous fission products accumulate within the fuel grains as a function of fluence. These initially constitute point defects in the crystal structure of the fuel, along with irradiation-induced vacancies and interstitials. Due to thermal diffusion and radiation assisted transport mechanisms, these defects tend to cluster. Large voids and agglomerations of fission products will start to swell the pellet. In PWR fuel, this swelling is typically manifest within 10 MWd/kg and continues at a rate roughly proportional to burnup. Gaseous fission products may escape the fuel as the gas bubbles grow and interconnect along the grain boundaries or due to thermal diffusion; however, this release does not a have a significant impact on the rate of swelling. [2]

Another typical initiator of PCMI in PWRs is sudden increase in fuel rod temperature, such as a power ramp during startup. The thermal expansion coefficient of Zircaloy is less than that of the UO_2 pellet, and expansion of accumulated solid and gaseous fission products will add to the growth. Significant contact pressure may accumulate before the cladding can creep outward to relieve the pressure. In addition, there is both an axial and radial dependence to the temperature in a fuel pellet, even in one that is cracked, that will cause special deformation. The central region will expand more than the edges, producing an “hourglass” or “wheat-sheaf” shape. The corners of the pellet then may impinge on the cladding before the outside surface of the pellet, as shown in Figure 1. [3]

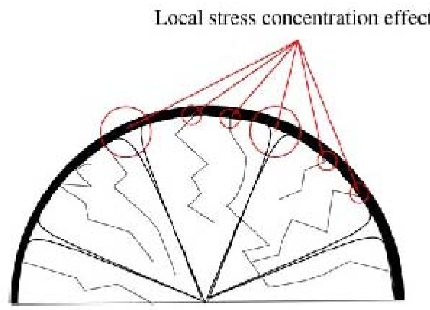

Figure 3. Hourglass distortion

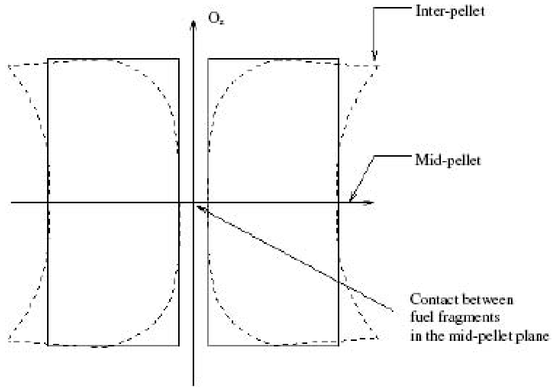

Figure 1. Mechanisms of stress concentration due to pellet radial cracking and hourglassing. [1]

PCMI Damage Effects

The consequences of hard contact between the fuel pellet and the cladding are not necessarily unacceptable; however, it can lead to excessive deformation or cracking of the cladding. In the most straightforward analysis, the contact pressure between the pellet and the cladding increases the cladding radial and hoop stresses, just as if the rod internal pressure had been increased. Because the initial fill pressure of the rod is much less than that of the external coolant, the cladding will be in compression before contact.

For some time after contact is likely the cladding will remain in compression, but the added pressure from the pellet will continue to reduce the cladding hoop stress. Because Zircaloy cladding creeps down towards the pellet during operation, the transition from compression to tension will take place at a smaller diameter than the cladding tube's original dimensions. After this point, the cladding will be in tension. This state favors the propagation of cracks, and may eventually cause yielding and failure of the cladding.

The hourglassing effect concentrates the contact force along small ridges, which correspondingly causes circumferential ridge deformations in the cladding. These stress concentrations will cause cladding failures before that predicted by a plane stress expansion model.

The pellets will also develop radial cracks that lead to pellet fragmentation into wedges. Each of these wedges then swells, which can produce additional hoop stresses at the cladding inner surface. Stress concentrations between the edges of these wedges are favorable points for cladding crack initiation. [3]

A significant mechanism of cladding damage, which increases the problems of mechanical stress discussed above, is chemical attack on the cladding inner surface. In particular, the mechanism of stress corrosion cracking (SCC), which occurs due to a combination of tensile stresses and chemical corrosion at the crack tip, is of significant concern. A schematic of the SCC mechanism during PCMI is shown in Figure 2.

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The fuel-cladding gap is initially filled with inert helium gas, and although this becomes contaminated with fission products such as krypton and xenon, it is not particularly corrosive. However, once there is good contact between the pellet and the cladding, solid fission products may migrate down the thermal gradient to the cladding. The most corrosive of these is iodine, and it is a major contributor to SCC. [5] In addition, there is oxygen released from the oxide pellet due to fission, and the cladding is particularly susceptible to oxidation.

The formation of a zone of chemical bonding between the fuel pellet and the cladding, facilitated by the mechanical interaction, tends to reduce ductility and increase local stresses due to the dissimilar thermal expansion and swelling of the joined structures. The corrosion and possibly irradiation assisted growth of cracks through the cladding, combined with increased tensile stresses at the inner cladding surface and the transition from compressive to tensile stress in the bulk cladding due to PCMI combine to increase the probability of through-wall cladding failure.

PCMI Prevention and Mitigation

Because it is not possible to monitor the fuel-cladding gap status during operation, contact between the pellet and the cladding must be either mitigated or prevented through choice of materials, construction, and operation. Some work has been done concerning mitigation of PCMI for boiling water reactors by adding a thin layer of zirconium on the inside of the cladding. However, this is expensive, reduces the available fuel volume, and increases the parasitic absorption of neutrons. Additionally, pellets can be manufactured with chamfered edges to increase the distance between the pellet and the clad if the pellet undergoes considerable hourglassing during operation. This also provides a larger contact surface area if contact does occur. [4]

More desirable than these mitigation techniques is prevention of PCMI altogether. The most obvious method of prevention is proper sizing of the initial pellet-cladding gap. Because of the low thermal conductance of the gap, and the fact that either the clad or pellet must be thinned to achieve a larger gap size, it is important to make the gap wide enough to prevent contact during the expected operating lifetime of the fuel rod. In this sense, the major concerns are the relative thermal expansions of the pellet and cladding, the creep down of the cladding during operation, and the irradiation-induced swelling of the pellet. The initial helium fill-gas pressure is also a relevant variable; higher gas pressure reduces cladding creep down and increases gap conductance, however it also significantly increases the internal pressure of the rod at end of life, therefore it is limited by the expected fission gas release during operation.

To prevent contact due to pellet hourglassing, reactors operate under restrictions governing the rate that the linear heat generation in the fuel can be increased. This allows time for the relaxation of stresses induced by the thermal expansion of the pellet and entrapped fission gases. [6]

PCMI in Zircaloy versus SiC

Zircaloy has been the nearly exclusive material of choice for fuel rod cladding since the first power reactors were constructed. It has favorable neutronic and mechanical properties compared to stainless steel, while remaining resistant to corrosion at normal reactor operating temperatures. However, Zircaloy does oxidize and in general loose ductility as core residence time increases. Currently the reliability of Zircaloy cladding is a major concern of power reactor operators, and it is a major limitation to extending fuel discharge burnup in the future. [7]

As the understanding and manufacturability of ceramics, such as SiC, has increased, so have considerations of using these materials in place of metal components. In particular, replacing Zircaloy cladding with SiC has several possible advantages. SiC is more resistant to chemical attack than Zircaloy, oxidizes less vigorously at high temperatures, has a higher yield strength, has a substantially lower creep rate, and has a lower neutron capture cross section.

One of the main difficulties when using SiC is its brittle behavior. There is essentially no plastic yielding in SiC, therefore it does not demonstrate the kind of graceful failure mechanisms that a metal does. In an effort to provide additional strength and flexibility for SiC structures, SiC composites are constructed using small, woven SiC fibers bonded together with a SiC matrix. In particular, a concept has been demonstrated that uses a SiC duplex fuel rod clad to hold UO_2 pellets in a standard PWR configuration. [8]

The duplex consists of a monolithic inner SiC tube surrounded by and bonded to a SiC composite. It is difficult to model this dual-layer cladding design in existing PWR fuel rod modeling codes; however, it is possible to get a general idea of the performance by approximating the construction as a single layer with the most limiting properties.

In addition to the mechanical stress and strain limitations, SCC mechanisms may need to be considered in SiC materials; however, there is currently limited information available on the susceptibility of SiC to SCC. SiC is vulnerable to oxidation under certain conditions, especially if there is free silicon within the matrix, therefore it is important the SiC materials be a stoichiometric as possible prior to irradiation. [9]

A conservative restriction for SiC is prevention of any PCMI during operation. This is based on both limitations of the codes used in the analysis of SiC cladding and based on experience with Zircaloy cladding. This analysis investigates this limitation in an attempt to gauge the range of the true response of the fuel rod if PCMI does occur.

FRAPCON PCMI Model

The pellet-cladding interface problem can be approached from a variety of levels, depending on the mechanism of interest. One- to three-dimensional models have been developed, however with greater geometric complexity, the size and time domains generally become more restricted based on computing power. An ideal model of the pellet incorporates not only the three-dimensional structure of a single pellet, as in Figure 3, but also the entire fuel rod. The temperature, pressure, neutron flux, and coolant flow conditions vary both axially and radially; these conditions also change in time between different steady-state conditions, while the residence time of the fuel rod in the core is on the order of years.

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The fuel rod analysis code used in this work is FRAPCON, a steady-state fuel rod code for light water reactors. It was developed for the U.S. Nuclear Regulatory Commission as a conservative tool for modeling the behavior of fuel rods under realistic operating conditions. FRAPCON tracks the thermo-mechanical behavior of a single fuel rod up to high burnup. It models the fuel using a series of axisymmetric radial rings at specified axial nodes, and uses a single-ring model for the cladding. It also has detailed correlations for calculating fission gas accumulation and release, fuel and cladding properties as a function of time, temperature, and fluence, and corrosion due to hydriding and oxidation. [2]

FRAPCON uses a rigid pellet model for PCMI calculations; after pellet-cladding contact, the pellet stresses and strains are calculated, then the cladding deformation is calculated. However, there is no feedback to the pellet mechanical model concerning the cladding behavior, unless it determines the pellet-cladding gap is no longer closed. FRAPCON also assumes that there is no axial slip of the pellet after the gap has closed, although in general it does track the axial deformation of the pellet stack and the cladding.

As discussed above, pellet cracking, relocation, and chemical attack are all important considerations when determining cladding integrity. FRAPCON does account for pellet cracking and relocation by adjusting the thermal model based on assumption of uniform "soft" closure of the gap. However, it does not account for the changes in circumferential stresses based on this cracking. After relocation due to cracking has occurred, the pellet swelling and densification continues, however special consideration is then given at the first instance of hard pellet-cladding contact. It is assumed that 50% of the relocation strain is recovered before true PCMI can occur; this implicitly accounts for the partial re-closing of the radial cracks.

After the maximum amount of surface relocation strain has been recovered, the fuel rod mechanical behavior will be dominated by the pellet swelling (assuming a constant or decreasing linear heat generation rate and low fission gas release). The swelling will be linear with burnup, and the cladding will expand outward to accommodate the pellet's radial strain. FRAPCON does not track the cladding for failure due to stresses or strains beyond the yield or ultimate stresses, nor does it perform any fracture analysis.

In general, given the aversion to PCMI during normal operation, and the strength and ductility of Zircaloy, the rigid pellet model may be an appropriate approximation. However, for extended periods of PCMI, or for a ceramic SiC cladding with an elastic modulus closer to that of the ceramic UO_2 pellet, this model may fail to capture important behavior, or misrepresent the available operational margin.

Improved PCMI Model: The "Deformable Pellet"

A simple three-dimensional model of PCMI was developed to compare to the predictions of FRAPCON. Like the FRAPCON analysis, this model treats the cladding as a single ring located far from the fuel rod ends. This is a reasonable assumption since the peak flux and temperature occurs just above the midplane of the fuel rod. The relation between the various pressures and the cladding stresses in the r, θ, and z directions is,

$$\begin{bmatrix} \sigma_r \\ \sigma_\theta \\ \sigma_z \end{bmatrix} = \begin{bmatrix} -\dfrac{r_{co}}{r_{ci}+r_{co}} & -\dfrac{r_{ci}}{r_{ci}+r_{co}} & 0 \\ -\dfrac{r_{co}}{r_{co}-a} & \dfrac{r_{ci}}{r_{co}-r_{ci}} & 0 \\ 0 & 0 & \dfrac{1}{\pi\left(r_{co}^2-r_{ci}^2\right)} \end{bmatrix} \begin{bmatrix} P_{ext} \\ P_{in} \\ F_z \end{bmatrix} \tag{2}$$

where r_{co} and r_{ci} are the cladding outer and inner radii, respectively, P_{ext} is the coolant pressure external to the fuel rod, P_{in} is the pressure exerted on the inner cladding surface, which includes gas and contact pressure, and F_z is the net axial force given by,

$$F_z = \pi\left(r_{ci}^2 P_g - r_{co}^2 P_{ext}\right) \tag{3}$$

where P_g is the fuel rod internal gas pressure. The stress-strain relations for the cladding are then given by,

$$\begin{bmatrix} \varepsilon_r \\ \varepsilon_\theta \\ \varepsilon_z \end{bmatrix} = \frac{1}{E} \begin{bmatrix} 1 & -\nu & -\nu \\ -\nu & 1 & -\nu \\ -\nu & -\nu & 1 \end{bmatrix} \begin{bmatrix} \sigma_r \\ \sigma_\theta \\ \sigma_z \end{bmatrix} \tag{4}$$

where E is the elastic modulus and ν is Poisson's Ratio.

The pellet is modeled as a solid, deformable right cylinder, where the axial and radial stresses are given by,

$$\sigma_z = -\left(P_g + P_s\right) \tag{5}$$

$$\sigma_r = -P_{in} \tag{6}$$

where P_s is the pressure due to the pellet stack hold-down spring. It is assumed that there is no friction between the pellet and the cladding, and properties are taken to be axisymmetric and uniform over the axial length under consideration. In addition, relaxation of the cladding stress due to creep is not considered. These approximations greatly simplify the physical situation, but should provide a bounding estimation of the cladding behavior when compared to the results of the rigid pellet model.

Once the pellet-cladding gap has closed, the FRAPCON code continues to calculate the fuel strain outward due to irradiation-induced swelling without radial constraints. For this model, that strain is treated as a virtual cladding strain, and the stress required to deform the cladding is converted into the effective contact pressure between the pellet and the cladding using,

$$\Delta\varepsilon_{\theta c} + \varepsilon_{\theta c} = \frac{1}{E_c}\left[\left(-\frac{r_{co}}{r_{co}-r_{ci}}P_{ext} + \frac{r_{ci}}{r_{co}-r_{ci}}P_{in}\right) - \nu_c\left(-\frac{r_{co}}{r_{co}+r_{ci}}P_{ext} - \frac{r_{ci}}{r_{co}+r_{ci}}P_{in}\right) - \nu_c\left(\frac{r_{ci}^2 P_g - r_{co}^2 P_{ext}}{r_{co}^2 - r_{ci}^2}\right)\right] \tag{7}$$

where $\Delta\varepsilon_{\theta c}$ is the virtual cladding strain. Then a relation for radial pellet strain,

$$\varepsilon_{rf} = \frac{1}{E_f}\left(-P_{in} - \nu_f\left(\sigma_{\theta f} - \left(P_g + P_s\right)\right)\right) \tag{8}$$

is substituted into (7). The FRAPCON-predicted swelling of the fuel pellet will therefore result in an increase in the contact pressure between the pellet and the cladding. This model calculates the new steady state geometry, based on radial strain of the pellet-cladding interface and the resultant stresses in the pellet and cladding. The result will be a smaller radial deformation in the cladding than predicted by the rigid pellet model; however, the degree of difference may be small depending on the relative materials and physical condition of the pellet and the cladding. Comparing the modeling assumptions made in this model and those in the FRAPCON code, the true behavior of the fuel rod likely lies in between.

A parameter of particular interest in this study is the stress intensity in the cladding. The FRAPCON manual suggests use of the Von Mises Distortion Energy Theory to relate stresses to yield and failure modes. According to this theory, failure occurs when (for a cylindrical principal coordinate system),

$$\sigma_f \leq \frac{\sqrt{2}}{2}\left[\left(\sigma_r - \sigma_\theta\right)^2 + \left(\sigma_\theta - \sigma_z\right)^2 + \left(\sigma_z - \sigma_r\right)^2\right]^{1/2} \tag{9}$$

where σ_f is the fracture or yield strength of the material in question. It is therefore necessary to calculate the actual cladding stresses given the actual pellet strain. The pellet external surface displacement is given by,

$$u_{fo} = r_{fo}\varepsilon_{rf} \tag{10}$$

where r_{fo} is the pellet outer radius, which is equal to the cladding inner radius. Furthermore, if it is determined that the gap remains closed (pellet swelling is the predominate condition), then the deformation of the pellet outer surface equals that of the cladding inner surface,

$$u_{ci} = u_{fo} \tag{11}$$

and the relationship between cladding displacement and strain is given by,

$$\begin{bmatrix} \varepsilon_r \\ \varepsilon_\theta \\ \varepsilon_z \end{bmatrix} = \begin{bmatrix} \frac{1}{r_{co} - r_{ci}} & -\frac{1}{r_{co} - r_{ci}} & 0 \\ \frac{1}{r_{co} + r_{ci}} & \frac{1}{r_{co} + r_{ci}} & 0 \\ 0 & 0 & 1 \end{bmatrix} \begin{bmatrix} u_{co} \\ u_{ci} \\ \varepsilon_z \end{bmatrix} \tag{12}$$

Code Input Case

The FRAPCON input case for this study is based on a 15x15 PWR with constant external conditions (coolant and power). The input parameters for this case are given in Table 2. This case is for UO_2 fuel with Zircaloy cladding. The linear heat generation rate was chosen to ensure pellet-cladding contact during operation without excessive fission gas release. Therefore, this case models PCMI initiated by prolonged operation, allowing time for the cladding to creep down onto the pellet and for the pellet to swell outward significantly due to fission product buildup.

Table 2. Parameters for FRAPCON input file.
Geometry and fill gas pressure is for cold, as-manufactured fuel rod.

Property	Value
LHGR	19.7 kW/m
Coolant Pressure	15.2 MPa
Inlet Temperature	561 K
Local Peaking Factor	1.12
Fuel OD	0.932 cm
Cladding ID	0.958 cm
Cladding OD	1.09 cm
Fill Gas Pressure	3.31 MPa

This input case was chosen to evaluate both the Zircaloy and the SiC cladding response to PCMI both in order to ensure a fair comparison and because of limitations of the FRAPCON numerical solver. Even with the aggressive linear heat generation rate chosen for this study, maintaining the same cladding geometry and substituting the SiC cladding leads to an open gap at end of life. This is primarily due to the lack of creep in the SiC. This was compensated for by reducing the inner and outer radii of the SiC cladding until hard fuel-cladding contact occurred at the same time as in the Zircaloy cladding case (0.939 cm ID and 1.071 cm OD). The average fuel temperature is about 10 K higher due to the lower thermal conductivity of the SiC cladding, however it is not expected this will alter the properties of the fuel significantly in this study.

Analytical Results

The results of the preliminary FRAPCON analysis (with Zircaloy cladding) show the expected behavior. Figure 4 shows the fuel burnup proceeding linearly with fuel residence time, and a small fission gas release- less than 0.4% at discharge.

The initial heat up and relocation results in a slight closing of the gap, as shown in Figure 5, followed by fuel densification during the first 100 days (5 MWd/kg) of irradiation. After 200 days, there is a generally linear rate of gap closure as the cladding creeps down and the pellet swells outward. Between 700 and 1000 days there is soft contact between the pellet and the cladding, and the fuel fragment relocation strain is being recovered. Hard contact occurs soon thereafter, with the FRAPCON calculated contact pressure increasing rapidly as the pellet pushes the cladding outward.

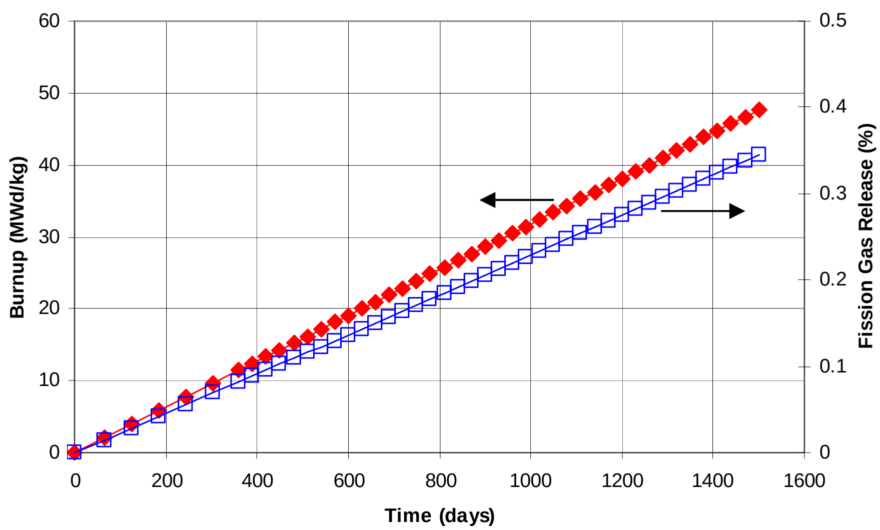

Figure 4. Burnup and fission gas release as a function of time for the FRAPCON case.

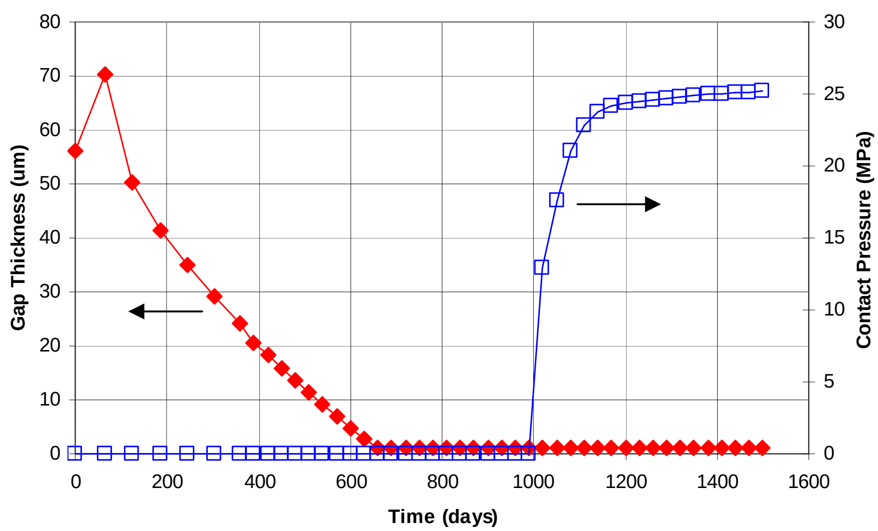

Figure 5. Pellet-cladding radial gap thickness and pellet-cladding contact pressure as a function of time for the FRAPCON case.

Comparing the results of the PCMI analysis for Zircaloy cladding between the FRAPCON model and the new deformable pellet model, it is apparent there is not a significant difference in deformation. As shown in Figure 6, there is only a 5% difference in the final cladding hoop strain between the two models.

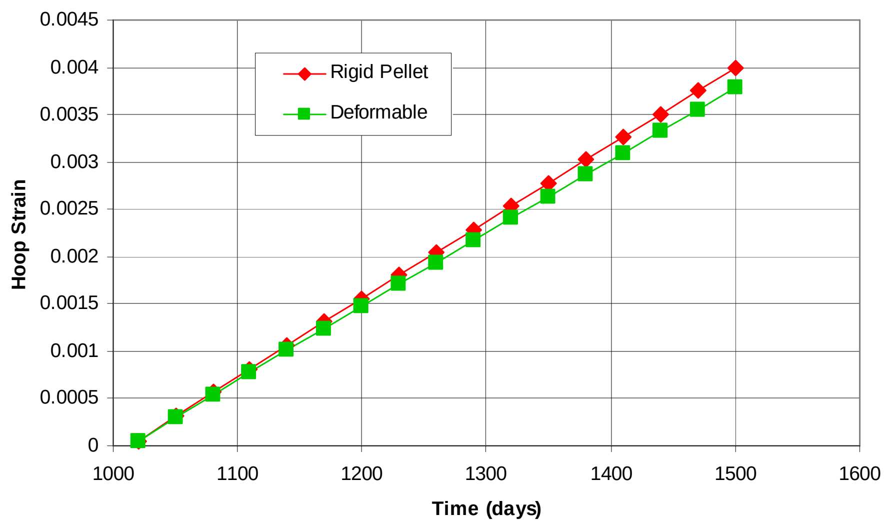

Figure 6. Zircaloy cladding hoop strain with and without a deformable pellet model.

There is a more significant difference, however, between the predicted cladding hoop stresses, as shown in Figure 7. The FRAPCON rigid pellet model predicts that the stress will approach a maximum near 70 MPa by discharge, while in the deformable pellet model the stress increases to over 200 MPa at discharge. The maximum stress intensity in the cladding with the deformable pellet model is 240 MPa, which is still below the Zircaloy yield stress of 270 MPa calculated by FRAPCON.

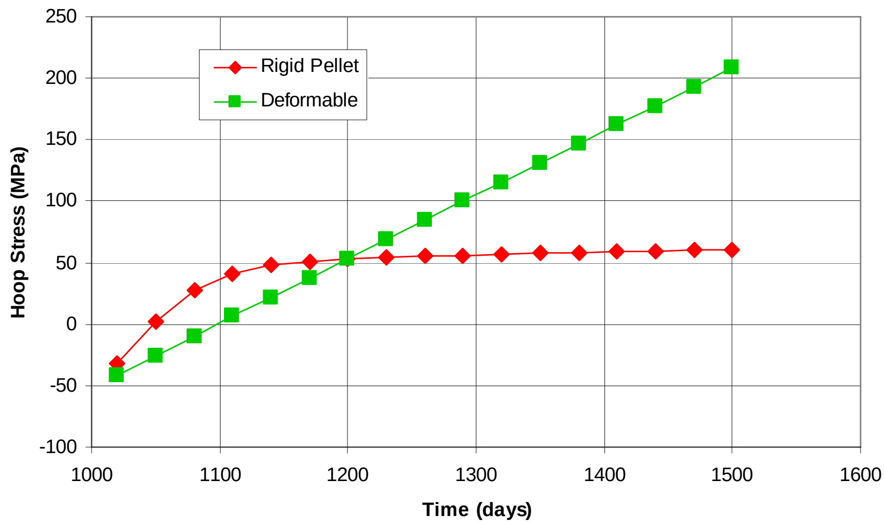

Figure 7. Zircaloy cladding hoop stress with and without a deformable pellet model.

Replacing the Zircaloy cladding with the average SiC layer, the primary difference in the PCMI analysis is the increase in elastic modulus and decrease in the fracture strength. As with the Zircaloy cladding, the difference in the cladding deformation predicted by the two models is small. The difference in hoop strain between the models is about 8% at end of life, as shown in Figure 8.

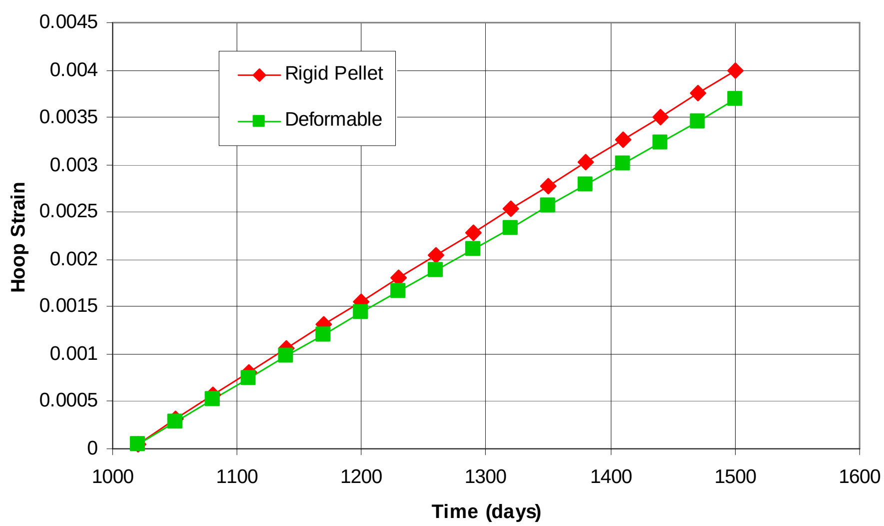

Figure 8. SiC cladding hoop strain with and without deformable pellet model.

Compared to the Zircaloy cases, the cladding stress accumulation for SiC cladding, shown in Figure 9, is much more significant. The rigid pellet model predicts a rapid increase in stress since there is no creep relief (up to 700 MPa at discharge), whereas the deformable pellet model predicts a more modest but also linear increase up to 325 MPa at discharge.

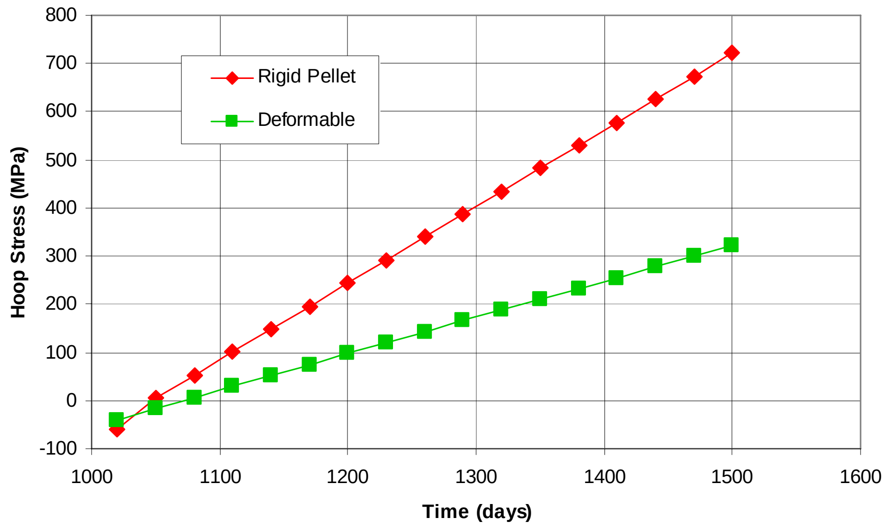

Figure 9. SiC cladding hoop stress and without a deformable pellet model.

Analysis

The deformable pellet model consistently predicts less cladding deformation than the rigid pellet model, as was expected. Due to the radial constraint, the fuel pellets experience greater axial deformation and less radial expansion with time. Given the model assumptions, the ability of the cladding to resist the pellet expansion is probably overestimated; therefore, the true deformation state should lie between the two models.

As a consequence of the deformation of the pellet, the deformable pellet model also generally predicts lower stresses in the cladding. Note that this is violated in the Zircaloy analysis after creep becomes important (greater than 1200 days, shown in Figure 7). After this point, the cladding creep rate is sufficient to maintain a nearly stable level of stress in the cladding, however the deformable pellet model neglects creep and predicts an ever increasing stress with imposed strain. For the Zircaloy cladding, even when neglecting creep, this stress is not predicted to exceed the yield strength.

The fracture strain of SiC is around 0.15%. Both models are in general agreement that this strain occurs around 1200 days. The fracture strength for the SiC composite is given by,

$$\sigma_f = \left(26600T + 2x10^8\right)\left(1 - 0.4\left(1 - e^{-\frac{3\Phi}{\left(1x10^{25}\right)20}}\right)\right) \quad (13)$$

where T is the cladding temperature in Kelvin and Φ is the neutron fluence in n/m^2. A calculation of the Von Mises stress intensity shows that the hoop stress necessary to surpass the fracture strength of the SiC is around 120 MPa. [8] The deformable pellet model predicts exceeding this fracture strength around 1230 days, giving a failure strain of 0.17%. This is a reasonable number and still in agreement with other studies of SiC composites. FRAPCON predicts fracture around 1110 days and therefore a strain of 0.075%.

The FRAPCON rigid pellet model predicts cladding strain within reasonable agreement of the deformable pellet model. The predictions of cladding stresses vary more substantially depending on the material. For the Zircaloy cladding, the deformable pellet model initially predicts lower stresses, as is realistically expected. When creep becomes significant at high stresses this is taken into account in the rigid pellet model, and therefore the limiting stresses in that case are more realistic. No failures are predicted in either model due to excessive stress or strain, however the impact of the lower initial stresses would reduce the predicted SCC and critical stress intensities for crack propagation.

For the SiC cladding, the distinctions between the models are more straightforward. The new deformable pellet model consistently predicts lower stresses in the cladding as a function of time since creep is not an issue. Use of the FRAPCON model alone, therefore, can provide reasonable prediction of failure when failure criteria are presented in terms of limiting strains, but not of limiting stresses.

The deformable pellet model also gives insight into the aggravation of SCC phenomena predicted the FRAPCON. Because the hoop stresses predicted by the deformable pellet model are significantly lower than those calculated by FRAPCON after initial contact in both claddings, stresses will be lower around the cracks. While the magnitude of this effect is not calculated here, it is probable that the limiting parameter for the Zircaloy cladding after PCMI will be SCC, since it is otherwise well below the yield strength. Using stress information from FRAPCON would lead to over-predictions of the cladding tensile stress, and therefore the rate of SCC damage. If SCC becomes an important consideration for SiC cladding, this analysis indicates that the FRAPCON overestimation of the cladding tensile stress would be even greater.

Conclusions

Pellet-cladding contact is initiated due to either cladding creep down, prolonged fuel pellet swelling, or sudden thermal expansion of the pellet due to power ramping. Mechanical interactions are avoided in practice by providing sufficient initial margin between the pellet and cladding surfaces, shaping the pellet to avoid protrusions following deformation, and limiting reactor power ramp rates. A different choice of cladding, such as SiC, reduces the parameter space by removing cladding creep as a PCMI initiation mechanism, but also as a source of post-contact stress relief.

PCMI damage may occur due to stressing the cladding beyond the yield or fracture strength at a point or over a region, producing bulges, ridges, or breaks in the cladding.

PCMI may also aggravate SCC by providing additional tensile stress to the cladding inner surface and a convenient transport pathway for corrosive chemical species.

A complete treatment of PCMI, from onset to failure, requires three-dimensional treatment of the fuel rod and the thermo-mechanical, chemical, and nuclear phenomena over large timescales. Simpler models, such as the rigid pellet model used in FRAPCON, provide insight into the condition of the pellet and cladding during PCMI, but may be less appropriate for different material choices.

An analysis of PCMI using the rigid pellet model, contrasted against a model of a simple deformable pellet, highlights the limitations of each approach. The rigid pellet is computationally simpler than a full implementation of the deformable pellet model, and it provides reasonably precise estimates of the pellet and cladding strains in time for both a ductile metal (Zircaloy) and a brittle ceramic (SiC) cladding. However, it likely significantly overestimates the stresses induced in the cladding, namely the tensile hoop stresses that may drive SCC failure mechanisms.

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