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Assessment of Failure Mechanisms for GFR Vented Fuel Pins Using Hexoloy Cladding

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Abstract

A near-term vented fuel pin concept as a back-up option for the gas-cooled fast reactor (GFR) system was evaluated. This work explored the feasibility of using mixed carbide fuel $(U_{0.85}P_{0.15})C$ with off-the-shelf monolithic SiC clad in order to meet requirements for GFR fuel with an average burn-up of 10%. The stress loading on the SiC cladding as a result of fuel swelling and thermal stress as a result of temperature gradient was estimated based on the data from the development of carbide fuels in the 1970's-1980's and the materials properties for SiC tubes. The fuel swelling at the goal burn-up (10%) is expected to produce a hoop stress of approximately 32 MPa in cladding, approaching the estimated maximum allowable hoop stress (~33 MPa) for a SiC cladding reliability of 99.99%. The estimated tensile thermal stress component (~121 MPa) near the outer surface of a monolithic SiC cladding is likely to limit its application at high temperatures.

1. Introduction

The concept of using a vented fuel pin for the Gas-cooled Fast Reactor (GFR) system was introduced in previous work at Argonne National Laboratory (ANL) on the "Generation IV Nuclear Energy System Initiative-Pin Core Subassembly Design"[1]. This pin core design is a back-up option to the 2400 MW_{th} CEA plate core design. General-Atomic® selected the vented fuel pin design for its GCFR 300 MWe demonstration plant 30 years ago, and had successful and limited operating experience with vented fuel pin elements in a gas-cooled thermal reactor (Peach Bottom unit 1)[2]. The current design from Argonne reports for GFR fuel pin elements proposed to use a $(U_{0.85}P_{0.15})C$ carbide fuel with monolithic SiC clad in order to meet the requirements of high temperatures for normal operation around 850ºC and loss of coolant accident (LOCA) up to 1600ºC, resistant to radiation damage and high performance on neutronics for fast neutrons. The use of a vented pin is to equalize the pressure on both sides of the clad wall to minimize the stress loading on the SiC ceramic clad from gas pressure differences across the cladding wall due to system helium pressure or fission gas buildup in the fuel pin. Note that there is a significant difference between the current design of the GFR vented fuel pin and the vented fuel element used in Peach Bottom unit 1. Carbide fuel programs in the United States in the 1960's-1970's for fast reactors provided a wealth of knowledge on carbide fuel property and performance through a series of carbide fuel tests in the Experimental Breeder Reactor II (EBR-II) for both Na-bonded and Helium-bonded fuel pins with a clad made mostly from 20% cold-worked 316 stainless steel (SS).

The purpose of this work on the preliminary scoping assessment of the cladding mechanical failure mechanisms is to evaluate the current design of the vented fuel pin with (U,P)C fuel pellets in SiC cladding. The approach is to analyze the available literature data and experiment results to extract information relevant to the GFR vented fuel pin operating conditions and identifying the failure mechanism for SiC cladding in the vented fuel pin design.

2. Current GFR Vented Fuel Pin Design

In the 2006 ANL report, a tall vented vertical single-segment pin was selected as a reference fuel

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pin design for a 2400 MW_{th} GFR with a total of 113,460 fuel pins in the core[1]. Figure 1 shows the schematic drawing with detailed parameters listed in Table 1. Note that the average linear power for the designed GFR fuel pin is ~15.8 kW/m, much lower than the typical ~75 kW/m used for carbide fuel tests in EBR-II in the 1970's. The reflector on the lower end has open channels for venting. The proposed cladding material is a sintered alpha-SiC (SA-SiC, 6H type) with its trade name Hexoloy® developed by Carborundum (St. Gobain) in the United States. For clarity, the term "HSA SiC" will be used for this special SiC ceramic throughout this paper. The manufacturer claims that HSA SiC cladding tubes with a length of ~2-3 meters, an inner diameter (ID) of ~6.5-7.0 mm, and a wall thickness of ~0.7-1.5 mm can be supplied with high confidence. Although a SiC/SiC composite by the nano-powder infiltration and transient eutectoid (NITE) process seems to yield a more attractive material option, this paper will only deal with the currently available monolithic SiC cladding. The general concerns for cladding failure include fuel-clad mechanical interaction (FCMI) from hoop stress as a result of fuel swelling, thermal stress as a result of temperature gradient, chemical incompatibility between cladding and coolant, and fuel-clad chemical interaction (FCCI).

For ceramic cladding to be used in the GFR, crack initiation through pre-existing flaws in the SiC under tensile loading and the resulting rapid propagation becomes a major concern. The use of the vented fuel pin instead of the sealed fuel pin avoids the stress rapture due to buildup of the large pressure difference across the cladding wall. More details may be found in Ref. [1].

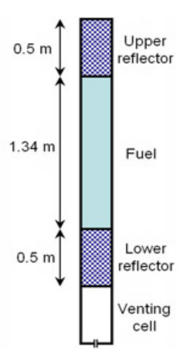

Figure 1. Schematic of a tall vented single segment pin.

Table 1. Key parameters of a reference design for vented single segment fuel pin for a 2400 MW_{th} pin core

Item	Parameters
Fuel composition	(U-15Pu)C
Pellet density*	81% theoretic density
Pellet diameter*	7.37 mm
Average burn-up	10 at%
Average fuel pin linear power	15.8 kW/m
Cladding material	HSA SiC
Cladding outer diameter	9.57 mm
Cladding wall thickness	1.0 mm
Cladding length	~ 2.60 m
Average fuel power density	451 MW/m^3
Core He in/out temperature	480/850 ºC
Core helium pressure	7.0 MPa

* The original design with an annular pellet and a 97%T.D. pellet density was modified for this work.

3. Summary of 1970's Carbide Fuel Work

The metal carbide fuel programs in the 1970's in the United States and Europe were mainly focused on two types of fuel pins, Na-bonded fuel pins and He-bonded fuel pins, with a large number of fuel pins being tested in the EBR-II sodium-cooled fast reactor. At the beginning of life (BOL), the peak centerline temperatures for a He-bonded fuel (linear power 70-80 kW/m, cladding OD ~ 7.87 mm) could reach up to 1850ºC with a fuel-cladding diametric gap of 0.25 mm. It would drop by 300-400ºC if a smaller diametric gap of 0.13 mm was used. After gap closure at burn-up less than ~2 at%, the calculation showed that the fuel centerline temperature decreased significantly to the range of 900-1000ºC. For Na-bonded fuel pins at a similar linear power at BOL, the fuel centerline temperatures were ~ 900ºC and remained so throughout the fuel life, since gap closure was not expected to occur.

The advantage of using carbide fuel is in its high thermal conductivity with a high power density and reduced doubling time. The major disadvantage of carbide fuels compared to other types of fuel is the high rate of fuel swelling. During testing, the swelling for carbide fuel increased rapidly with a temperature above 1000ºC[3]. The typical cladding material for both sodium-bond and He-bond fuel pins is a 20% cold-worked 316 SS with cladding operating temperatures in the range of 530-650ºC, depending on the fuel design. Cladding swelling played a significant role in the cladding diametric increase. Both types of fuel pins were successful, with significant numbers of the fuel pins reaching the goal burn-up of 12 at% without cladding breaches. Some He-bonded fuel pins even reached up to 16 at% burn-up without failure, with more than four years of immersion in the reactor sodium.

For Sodium-bonded fuel, a large fuel-cladding gap (0.254-0.380 mm) was used to accommodate the fuel swelling up to the goal burn-up. A large gap in Na-bonded fuel does not significantly affect the fuel temperature. Most of the swelling data generated from sodium-bonded fuel pins with a large gap could be considered as unrestrained free-swelling. The measured cladding diametric strain was mainly due to cladding swelling. For He-bonded fuel pins, the size of the fuel-cladding gap cannot be used to accommodate fuel swelling. A larger gap will increase the fuel temperature dramatically, leading to more aggressive fuel swelling. Swelling control in the He-bond fuel pin was achieved through the constrained swelling. A smaller fuel-cladding gap size (0.063-0.127 mm) and thicker cladding (~0.51 mm thickness) were used to provide effective constraint on fuel swelling. Before gap closure, the He-bonded fuel underwent a fast-rate free swelling at higher fuel temperatures up to 1850ºC. After gap closure, fuel swelled at a reduced rate under the constraint of cladding hoop stress at a significantly reduced fuel centerline temperature (900-1000ºC).

While the cladding strain for Na-bonded fuel pins was mainly due to the cladding swelling, the measured strain for He-bonded fuel pins were composed of strains from both cladding swelling and cladding mechanical strain due to fuel swelling[4]. As a result of gap closure, the actual diametrical strain of fuel pellets was greater than the measured cladding diametrical strain. Considering cladding swelling contribution to the cladding diametric strain, the actual cladding mechanical strain due to hoop stress from fuel swelling was less than the measured cladding strain. The profilometry data for a 9.4 mm OD fuel pin (peak power ~ 107 kW/m) indicated a peak cladding strain of 1.5% at 8 at% burn-up, with cladding swelling accounting for a third of the total strain[5]. This corresponds to approximately 4.7%, 4.4%, and 1.0% for cladding thickness swelling, fuel diametric swelling, and cladding mechanical strain, respectively. At the goal burn-up of 12 at%, the total cladding diametric strain was more than doubled (3.3%), with the cladding swelling accounting for nearly half of the total strain. The corresponding strain changed to 15.7%, 5.4%, and 2.2%, respectively. It was also calculated that the maximum hoop stress contribution from gas pressure and thermal stress alone for the fuel pin tested was approximately 50 MPa, for a peak cladding temperature around 595ºC[6,7]. At the goal burn-up of 12 at%, the peak radiation damage in displacement per atom (dpa) for cladding for fuel pins with 7.87 mm OD in K-7 fuel test was estimated to be ~ 60 dpa[8].

Note that the large fuel element (9.40 mm OD) tended to show greater cladding strain than the smaller diameter fuel elements. It was clearly demonstrated that the lower density fuel pellet (81%T.D.) swelled less aggressively than the higher density fuel (≥ 87%TD). This is likely due to the retention of fission gas in the porosity. According to the work by Dienst[9], the higher BOL fuel temperature for He-bonded type causes fuel densification to occur (reaching to 90%TD) right after irradiation started, equal to an increase in fuel-cladding gap. A comparison of fission gas released between Na-bonded and He-bonded carbide fuels indicates that fission gas release of Na-bonded (U,Pu)C fuel surpasses that of He-bonded fuel at a higher burn-up[3].

Most of the carbide fuels tested were hyperstoichiometric carbide, with approximately 5-10 vol. % of the second phase $(U,Pu)_2C_3$ in $(U\text{-}20Pu)C_{1+x}$. Hypostoichiometric carbide fuel was excluded because the free U metal forms intermetallic compounds of the type of UFe_2 or UNi_5. The swelling and fission gas release of the $UC_{1\pm x}$ as a function of burn-up and C/U ratio were investigated by Crane et al[10]. The hypostoichiometric carbide fuel showed more aggressive fuel swelling and fission gas release than the hyperstoichiometric fuel. On the mechanical property for both single crystal and polycrystal UC, hyperstoichiometric carbide is much stronger than hypostoichiometric carbide[11]. It was found that free U in UC_{1-x} melts and coagulates along the grain boundaries at temperatures greater than 1130ºC. The creep rate is higher for the carbide fuels with higher porosity[3].

Gilbert et al. reported the creep hoop strain as a function of hoop stress for several 20%CW 300 series of SS irradiated in EBR-II to approximately 20 dpa at 500ºC[12]. In a review by Garner on the effect of hoop stress and irradiation temperature on non-swelling hoop strain, it was shown that for a given hoop strain, the hoop stress at 600ºC is reduced by roughly 50% compared to that at 500ºC[13]. Note that the yield strength for the 20%CW 316 SS irradiated at 600ºC to 25 dpa is estimated to be approximately 330 MPa[14]. The plastic deformation by yielding requires much higher stress than that of creep deformation.

The evolution of the yield stress as a function of an irradiation dose at various temperatures for 316 SS indicates that the yield stress for 316 SS remains nearly unchanged between 5 to 42 dpa at irradiation temperatures around 600ºC[13]. These results will be used to estimate the hoop stress due to fuel swelling for the He-bonded (U, Pu)C fuels.

4. Summary of HSA SiC Material Properties

The feasibility of using HSA SiC as cladding material for vented GFR fuel pins remains an open question, simply due to the lack of data for its performance under irradiation up to a high dose. The HSA SiC for GFR fuel cladding is a monolithic sintered hexagonal 6H-SiC with a mechanical strength approximately half of that for the SiC/SiC composite. Boron was used as a sintering agent, and residual Si and C had been found in the matrix. These elements in HSA SiC could precipitate to the grain boundaries, along with the production of helium through a thermal neutron reaction of $^{10}B(n, \alpha)^{7}Li$ under irradiation in the Advanced Test Reactor (ATR) at 1100ºC to doses up to 1 dpa[15]. The irradiation experiment with a holder made of HSA SiC irradiated at 400-700ºC to 0.2-0.6 dpa showed that the holder essentially maintained no integrity following irradiation[16]. There is an approximate 50% reduction in thermal diffusivity for SiC irradiated at 1100ºC to doses of 0.5~1 dpa[15]. Snead summarized the degradation on mechanical property of various SiC as a function of irradiation dose[16]. After irradiation at 740ºC to a dose of 23 dpa, the flexural strength for HSA SiC reduced from the unirradiated value of 400 MPa to 265 MPa, representing a 34% reduction[17].

The work by Munro provided the most comprehensive data on HSA SiC material properties, including thermal expansion, density, thermal conductivity, thermal diffusivity, flexural strength, tensile strength, and Weibull Modulus as a function of temperatures up to 1600ºC[18]. Contrary to SS cladding, the thermal expansion for HSA SiC is less than that of (U, Pu)C carbide, indicating a shrinkage of the fuel-cladding gap with temperature increase. The average tensile strength for HSA SiC remains nearly constant (~233 MPa at 25ºC and ~250 at 1400ºC) as a function of temperature with a large data scatter (±40 MPa). Weibull modulus is a measure of scatter in a mechanical test, and is an important materials parameter to show size effect for ceramics to be used as structural materials. Its average value does not change with temperatures up to 1500ºC.

As part of quality control for the fabrication of HSA SiC heat exchanger tubes, water pressure tests are performed for each tube produced. These tubes have a similar tube length of ~ 2.5 m with larger diameters (OD/ID = 14.0/11.0 mm) compared to the tube dimension specified for GFR cladding design (OD/ID = 9.57/7.57 mm). The tests are conducted at room temperature by filling the tube with water to the pressure of 18.6 MPa in 10-15 seconds, holding for ~3 seconds and then releasing the water pressure. The average failure rate is approximately 4% of all the tubes tested[19]. This result, combined with the Weibull modulus to account for the size effect, will be used to evaluate the reliability of HSA SiC cladding as a function of hoop stress.

5. Scooping of Failure Mechanism for SiC Cladding

With the background information provided in the previous two sections, the analysis in this section will be based largely on those experimental results. Knowing the difference between the He-bonded fuel pin and the vented fuel pin, the pressure build-up due to fission gas release is no longer a concern for the GFR fuel pin. However, the mechanical restraint of HSA SiC cladding on fuel swelling control imposes serious concern, since ceramic tubing will be under tensile loading from the hoop stress as a result of fuel swelling.

First, the threshold hoop stress for HSA SiC cladding will be investigated. This will involve the steps to determine the hoop stress in connection to the cladding failure probability evaluated from the pressure test for the heat exchanger tubes. The next step is to calculate the reliability as a function of hoop stress for GFR fuel cladding. Then, the hoop stress due to fuel swelling at ~ 10 at% burn-up on the 20% CW 316 SS cladding from the He-bonded carbide fuel tests will be evaluated from the stress and strain relationship. The comparison between the two hoop stresses will give a clear indication if the conceptual design for a vented fuel pin with HSA SiC cladding is feasible.

5.1. Calculation of threshold hoop stress for SiC Cladding

From the pressure test of the HSA SiC tube, the corresponding hoop stress σ_h is calculated to be 77.5 MPa. For a mechanical test of a ceramic with a materials Weibull modulus of *m*, and a stress of σ, the reliability as a function of stress is given by ϕ:

$$\phi = \exp\left\{-\left(\frac{\sigma}{\sigma_0}\right)^m\right\} \quad (1)$$

where σ_0 is the stress where only 1/e (37%) of the samples survive. The Weibull modulus *m* for HSA SiC can be expressed as an average value of 11±3. Since the reliability of the SiC heat-exchange tubes under the hoop stress of 77.5 MPa was 96%, the value σ_0 can be calculated as 103 MPa using Eq.(1).

This gives the reliability of the SiC tube ϕ under an applied hoop stress of σ for the tube volume v_0 of the specified heat-exchange tube (D=12.5 mm, t=1.50 mm, L~2.50 m). Since the strength of brittle materials decreases with increasing sample volume under a fixed stress due to the probability of pre-existing flaws/micro-cracks increasing with the sample volume, the reliability of HSA SiC tube with a volume *v* under a hoop stress σ is given by:

$$\phi = \exp\left\{-\frac{\mathrm{v}}{\mathrm{v}_0}\left(\frac{\sigma}{103.6}\right)^{11}\right\} \quad (2).$$

Considering the size difference between the cladding tube (9.57 mm OD, 1.00 mm thickness, L ~ 2.50 m) and the heat-exchange tube, the ratio of v/v_0 is 0.457. Thus, the reliability of the HSA SiC cladding as a function of hoop stress σ (in MPa) can be calculated using Eq. (2).

For a reliability of 99.99%, the maximum allowed hoop stress is calculated to be approximately 50 MPa. This is significantly lower than the tensile strength of 233-250 MPa reported by Munro. Note that SiC mechanical strength remains nearly constant with temperature. Therefore, the estimated threshold hoop stress can be applied to temperatures up to 1400ºC. However, the calculation using Eq. (2) did not account for the mechanical property degradation due to irradiation damage. A 34% reduction in flexural strength due to irradiation at 740ºC to 23 dpa was identified for HSA SiC[[17]]. It is assumed that a similar amount of degradation on mechanical strength will apply to the threshold hoop stress. Therefore, the estimated hoop stress threshold for HAS SiC cladding with a Weibull modulus *m* of 11 drops to approximately 33 MPa for a reliability of 99.99%.

5.2. Estimate of Pressure on HSA SiC Cladding due to Fuel Swelling at 10% burn-up

Cladding hoop stress due to fuel swelling can be determined from the carbide fuel experimental results in the 1970's to the goal burn-up of 10 at%. As discussed earlier, for a He-bonded (U-20Pu)C fuel at goal burn-up of 8 at% and 12 at% with a maximum peak cladding temperature of 600ºC, the cladding mechanical strain was estimated to be 1.0% and 2.2%, respectively. It is reasonable to assume an average cladding mechanical strain of 1.6% at 10 at% burn-up (~ 50 dpa). At this cladding mechanical strain, the corresponding hoop stress for a 20% CW 316 SS at 500ºC and 20 dpa is estimated to be 175 MPa[[12]]. Note that the strain data for He-bonded (U-20Pu)C fuel was generated at a higher peak cladding irradiation temperature and irradiation dose (600°C and 50 dpa). For the same hoop strain (~ 0.3%) and irradiation dose (~ 6.0 dpa) in a 20%Cw 316 SS, the hoop stress of 152 MPa at 490°C dropped by 34% to 100 MPa at 600°C[[13]]. Assuming a similar percentage reduction in hoop stress due to temperature increase, the hoop stress for a 1.6% mechanical strain drops by 34% from 175 MPa at 500°C to ~115 MPa at 600°C. Since the actual irradiation dose associated with the 1.6% cladding mechanical strain is ~ 50 dpa, if deformation mechanism is dominant by irradiation creep, the hoop stress required to reach 1.6% hoop strain at ~ 50 dpa should be lower than 115 MPa.

Recall that for a He-bonded carbide fuel, the calculated maximum hoop stress contribution from gas pressure and thermal stress alone for the fuel pin at ~ 600ºC is approximately 50 MPa[[7]]. The contribution of the cladding hoop stress from fuel swelling alone can be estimated as 65 MPa by subtracting 50 MPa from 115 MPa. The hoop stress, σ_h, for a given pressure loading, *p*, can be calculated by $\sigma_h = (p.D)/(2t)$, where *D* and *t* are the diameter and the cladding wall thickness, respectively. The corresponding pressure loading on the steel cladding (OD/ID=9.40/8.38 mm) due to fuel swelling (~ 600ºC up to 10 at% burn-up) is then calculated to be 7.46 MPa. For the HSA SiC cladding (OD/ID = 9.57/7.57 mm), the same amount of pressure loading (7.46 MPa) from fuel swelling is assumed, which will produce a hoop stress of 32 MPa, reaching the maximum allowed hoop stress (~ 33 MPa) for a cladding reliability of 99.99%.

If the total hoop stress in 20%CW 316 SS exceeds the cladding yield stress (> 330 MPa) at cladding temperatures of 600ºC, plastic strain by yielding could occur and the total mechanical strain will be the sum of yield strain and creep strain. This is less likely to be the case, since the corresponding hoop strain is expected to be much higher than 1.6% under the hoop stress greater than yield stress (> 330 MPa) for a 10 at% burn-up.

5.3. Thermal Stress Effect on HSA SiC Cladding

The effect of thermal stress on the hoop stress for a cladding tube can be calculated. For HAS SiC cladding at gap closure, the calculated axial temperature profile for hot channel is shown in Figure 2.

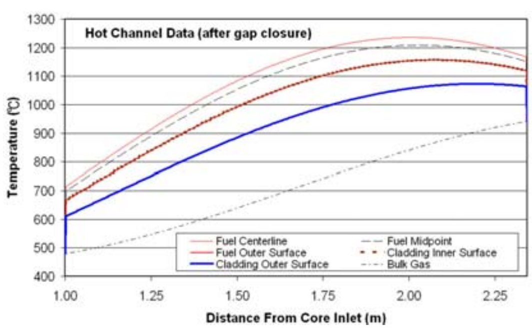

Figure 2. Calculated axial temperature profile for hot channels for the vented fuel pin with HAS SiC cladding.

For cladding, the temperature gradient in radial direction is much greater than in the axial direction. Note that the largest temperature gradient across the cladding wall, therefore the highest thermal stress, is approximately 111°C at hot channels located at the core middle plane. The thermal stresses in the cladding wall can be calculated for a given temperature radial distribution $T(r)$. Considering three thermal stress components in a hollow cylinder with a radial temperature distribution T(r), the radial $\sigma_r(r)$, tangential $\sigma_\theta(r)$, and axial $\sigma_z(r)$ thermal stress components at a location r in the tube wall can be calculated using the equations in Ref. [20]. From Figure 2 at core middle plane, the cladding temperature at inner surface (T_i) and outer surface (T_o) is 1072°C and 961°C, respectively. The components of the thermal stress as a function of radial position are shown in Figure 3.

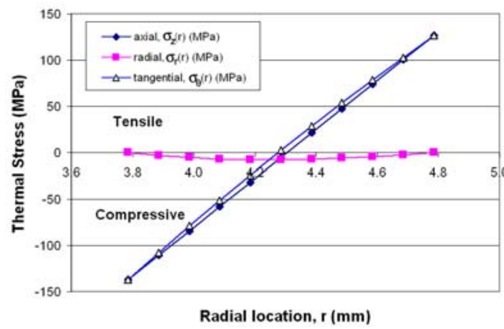

Figure 3. The calculated thermal stress components in HSA SiC cladding at core middle plane.

Both tangential and axial thermal stresses are compressive near the inner surface and tensile near the outer surface. The difference in the magnitude between the two is minor. Although the net effect of thermal stress tangential component on hoop strain is expected to be small as a result of the change from compression on the inner side to tension on the outer side, the large tensile stress may lead to ceramic cladding failure. The impact of tensile thermal stress between cladding mid-wall and the outer surface on ceramic cladding must be addressed in the fuel pin design, particularly for high temperature application. The calculated maximum radial component of the thermal stress is significantly lower than the maximum tangential and axial components.

6. Discussion and Conclusion

From the above analysis, it is clear that even for the most relaxed case by assuming cladding mechanical strain due only to creep, the amount of hoop stress due to pressure loading from fuel swelling at 10% burn-up will be approximately equal to the maximum allowed hoop stress for HSA SiC cladding for a fuel pin reliability of 99.99%.

The situation worsens by including the contribution from thermal stress which makes the outer half of the cladding tube under a total hoop stress between 32-153 MPa, depending on the radial location. This preliminary result disqualifies the monolithic SiC cladding to be used for GFR vented fuel pins with current design parameters.

The main challenges to using any ceramic material for GFR vented fuel claddings are the radiation degradations on thermal conductivity and mechanical property, the hoop stress from fuel swelling, and the tensile stress from thermal stress components (σ_θ and σ_z). To improve the radiation resistance, the levels of boron and other impurities must be reduced significantly from the current HSA SiC. For a given mechanical loading from fuel swelling, a thicker cladding wall may lower the hoop stress. However, an increase in cladding wall thickness increases the fuel temperature, which leads to more aggressive fuel swelling and possibly an increase in stress loading on the cladding. The relation between ceramic volume and its reliability also indicates that for a given stress, an increase in ceramic volume results in a decrease in reliability. From a quality control standpoint, a more sophisticated examination to identify the micro cracks or cavities in addition to the water pressure test is required to completely exclude tubes with any pre-existing flaws. To reduce the thermal stress in ceramic cladding, a thermal barrier coating on the outer surface may be used to significantly reduce the temperature drop across the cladding wall. However, the use of a thermal barrier will result in degradation in cladding overall thermal conductance.

On the properties of (U, Pu)C fuel behavior under irradiation, options to modify the carbide fuel are also very limited. A "softer" fuel at the relevant irradiation temperature may be used to reduce the stress loading from fuel swelling. The use of a slightly carbon-deficient fuel can significantly soften the fuel at a cost of enhanced fuel swelling [[10],[11]]. However, the experience from carbide fuel experiments in the 1970's are all based on tests of hyperstoichometric carbide fuels, due to its improved swelling behavior, good mechanical property, and matured fabrication practice. The feasibility of using a hypostoichiometric (U, Pu)C for a GFR vented fuel pin may be worth exploration since the volume increase from fuel swelling can be easily accommodated into axial direction for a vented fuel, as long as stress loading on cladding is acceptable.

Another alternative to reduce the stress loading on ceramic cladding is to enhance the creep of the carbide fuel. Dienst found that the irradiation creep rate in (U, PU)C fuels with a fuel pellet density of 85%TD is a factor of 2.2 higher than that of a more dense fuel of 95%TD[[9]]. He concluded that at temperatures greater than 1200°C, (U, Pu)C fuel can be considered unable to bear any appreciable mechanical load resulting from the cladding restraint in fuel pins. Therefore, the fuel swelling rates at lower temperatures are more important with regard to mechanical interaction between fuel and cladding. At a temperature of 1000°C, irradiation creep rate for 85%TD fuel was slightly less than 10^{-5} hr^{-1}, similar to its thermal creep rate. At 1200°C, thermal creep in carbide fuels surpassed the irradiation creep and reached approximately 10^{-4} hr^{-1} [[9]]. From the calculated temperature profile shown in Figure 2, most of the fuel elements are operated at a fuel midpoint temperature below 1200°C. Since the low end of the fuel pins near the core inlet are operated at a much lower fuel midpoint temperature below 700°C, the idea to raise the fuel temperature above 1200°C for enhanced creep to mitigate the fuel swelling-induced hoop stress loading on Hexoloy cladding seems impractical.

To summarize from this preliminary study, the fuel swelling of a mixed carbide fuel (U, Pu)C in He-bonded fuel at a goal burn-up of 10 at% is expected to produce an equivalent pressure loading of 7.46 MPa on the cladding inner wall. This will produce a hoop stress of 32 MPa in HSA SiC cladding very close to the maximum allowed hoop stress ~ 33 MPa with a required reliability of 99.99% for the current design parameter. The calculated thermal stress tensile component up to 121 MPa results in a total hoop stress of approximately 153 MPa in tensile at the cladding outer surface. The large tensile component of the thermal stress loading for the thick ceramic cladding is identified as the limiting factor for its application at high temperature.

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