Cladding Stress during Extended Storage of High Burnup Spent Nuclear Fuel

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1 Disclaimer

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2 Abstract

In an effort to assess the potential for low temperature creep and delayed hydride cracking failures in high burnup spent fuel cladding during extended dry storage, the U.S. NRC analytical fuel performance tools were used to predict cladding stress during a 300 year dry storage period for UO₂ fuel burned up to 65 GWd/MTU. Analysis that considered only decay gas production and release during storage resulted in stresses too low to result in failure due to low temperature creep or delayed hydride cracking. As a result, fuel swelling correlations were developed to determine if further stress, in addition to that produced by decay gas production and release, was placed on the spent fuel cladding during storage. Fuel swelling was then taken into account together with decay gas production and release to produce circumferential average cladding stress predictions with the FRAPCON-3.5 fuel performance code. The resulting stresses did not result in cladding creep failures. When a more realistic (though still conservative) decay gas release fraction based on data for helium release was assumed, which is conservative, the maximum creep strains accumulated were on the order of 0.54 % to 1.04 %, but creep failures are not expected below at least 2 % strain. When a highly conservative 100 % decay gas release was assumed, the stresses did reach values high enough to potentially be of concern for hydride reorientation, but only at times when the temperature had dropped below 473 K and the hydride solid solubility was below ~30 wt.ppm. The potential for delayed hydride cracking was assessed by calculating the critical flaw size required to trigger this failure mechanism. The critical flaw size required to initiate delayed hydride cracking far exceeded any realistic flaw expected in spent fuel at end of reactor life. Nonetheless, if very conservative flaw sizes (120 µm) and 100 % decay gas release were assumed, BWR 9x9 and BWR 10x10 fuel was predicted to reach sufficient stresses to initiate delayed hydride cracking after 260 years of storage, at which point temperatures were around 380 K and cladding average stresses were around 200 MPa.

3 Introduction and background

Gap analyses by NRC [1] and DOE [2] have identified low temperature creep (LTC) and delayed hydride cracking (DHC) as potential cladding breach mechanisms after ~100 years of dry storage. Both of these

mechanisms require the presence of a stress in order to be active. Predicting the cladding stress over an extended storage period is necessary to evaluate the potential breach of the spent fuel cladding due to LTC and DHC during long term spent fuel storage, from 100 to 300 years.

Potential sources of stress are plenum gas pressure, phase change of the hydrides upon cooling from drying temperatures [3], and swelling of the fuel due to a buildup of helium decay product (resulting in pellet-cladding mechanical interaction - PCMI). Two reports describing LTC and DHC are "Review of Used Nuclear Fuel Storage and Transportation Technical Gap Analyses", from DOE [2], which discusses fuel pellet restructuring and swelling in Section 3.2.5 and cladding creep at low temperature in Section 3.3.4; and "Identification and Prioritization of the Technical Information Needs Affecting Potential Regulation of Extended Storage and Transportation of Spent Nuclear Fuel", from NRC [1], which discusses fuel pellet swelling in Section A2.3 and cladding creep at low temperature in Section A1.4.

Although the rod pressure at the end of irradiation may be too low to drive these cladding breach mechanisms, helium production due to alpha decay and PCMI induced by swelling of the pellets via a buildup of helium have been proposed as sources for cladding stress [4]. Until stress levels in the cladding are evaluated over the extended storage period, it is unclear whether LTC or DHC cause a regulatory concern. The present study consists of cladding stress predictions over a period of 300 years of spent fuel dry storage for fuel burned to 65 GWd/MTU for different fuel designs having different power histories. The predictions account for both gas production in spent fuel and fuel pellet swelling during storage.

The calculations performed in this study do not account for any local stress concentration that might be caused by pellet-pellet interfaces, pellet fragment interfaces, friction forces that may arise between pellet fragments as the fuel swells during storage, or any circumferential heterogeneity in pellet-to-cladding mechanical interaction. Consequently, the cladding stresses calculated in this study represent a circumferential average cladding stress. As discussed in Section 6.3, this limitation is not expected to have had a major impact on the results of the study, mainly due to the fact that significant pellet inter-fragment volume was predicted to exist during the dry storage period.

4 Phenomenology and Analytical Approach

The goal of this study was to determine whether sufficient cladding stress would develop in high burnup spent fuel over a period of long term dry storage that could lead to cladding failure due to creep or static overload. The first step of the study was to determine if gas production due to decay of plutonium and fission products would result in a sufficiently high stress to cause a cladding failure. A simple MS Excel analysis showed that gas pressure alone would likely not result in cladding failure. Consequently, the next steps consisted in developing fuel pellet swelling correlations and subsequently using them to determine the total cladding stress and associated critical flaw size for DHC initiation, as well as predict the cladding strain accumulated during 300 years in dry storage. A modified version of the NRC's steady-state fuel performance code FRAPCON-3.5 was used to perform this analytical study.

4.1 Modeled Sequence and Cladding Temperature History during Dry Storage

The sequence that was modeled in this study to determine cladding stress in high burnup fuel over an extended storage period of 300 years starts at reactor shutdown then includes pool storage followed by transfer to a dry storage canister, drying, and finally 295 years in dry storage. After irradiation to 65 GWd/MTU, the fuel rods are stored in the spent fuel pool for 5 years, where the decay heat produced

by the fuel rods is removed by active cooling, such that the temperature of the cladding remains at or below 353.15 K.

It is interesting to note that upon cooldown from reactor temperatures, FRAPCON predicted that the fuelto-cladding gap reopens because the thermal contraction of the pellet exceeds that of the cladding. In reality, the physical gap between the pellet and the cladding does not reopen, particularly for higher burnup fuel where the fuel is bonded to the cladding. Nonetheless, the differential thermal contraction of the pellet and cladding opens up some inter-fragment volume inside the fuel pellet. More details on this predicted phenomenon are provided in Section 6.3. It is also important to note that low temperature¹ decay gas production (predominantly helium) and fuel pellet swelling, both primarily due to alpha decay of transuranic species and fission product decay, begin as soon as the reactor is shut down and the fuel rods enter storage.

After 5 years in the spent fuel pool, the fuel is transferred to a Dry Storage Canister (DSC) and undergoes a short drying phase during which the temperature is limited to 673.15 K. Once dry, the storage canister is sealed and filled with helium gas prior to being moved to the Independent Spent Fuel Storage Installation (ISFSI). In the ISFSI, the DSC is stored either in a horizontal storage module primarily made of concrete, or in a vertical overpack either made of metal or concrete, or both. In both the horizontal and the vertical storage configurations, the outer surface of the DSC is exposed to ambient air and cooled by natural circulation of air through the overpack and around the DSC. The cladding temperature during dry storage is given by Equation 1 and shown on Figure 1, where the initial storage temperature is 673.15 K, which is the highest allowed per ISG-11, Revision 3 [5]. Equation 1 was derived from fitting the dry storage cladding temperature history used to evaluate radial hydride precipitation in Appendix D, pages 13 and 32 of reference [6].

$$T(K) = \begin{cases} 0.265714 \times t^2 - 12.3343 \times t + 673.15 & \text{if } t < 13.0306 \text{ years} \\ -59.2015 \times \ln(t) + 709.532 & \text{if } t \ge 13.0306 \text{ years} \end{cases}$$
(1)





Figure 1: Peak cladding temperature history used in this study for a 300 year storage period.

¹ 'low temperature' used in this context implies dry storage temperatures, as opposed to the high temperatures used in specimen annealing tests where gas release is measured as a function of time or temperature (typically between 1000 K and 2500 K)

During the time when the temperature in the DSC is above 573.15 K, the cladding rapidly creeps out and away from the pellet due to the rod gas pressure, resulting in a significant increase in the predicted gap size, and corresponding in reality to an increase in pellet inter-fragment volume, as described in more detail in Section 6.3. Once the temperature drops below ~523.15 K, cladding creep is close to zero and the cladding plastic deformation no longer evolves. This arrest in creep deformation could potentially result in a slow buildup of cladding stress due to decay gas production and release, combined with the reduction in void volume as the fuel pellets are swelling. However, this potential stress increase is counteracted by the temperature decrease during the storage period, such that the stress increase is only predicted if it is assumed that more than 10-15 % of the decay gas is released, which is highly conservative and unrealistic (see Section 6.2.3). Eventually, if the pellets swell sufficiently to come in contact with the cladding, a Pellet-to-Cladding Mechanical Interaction (PCMI) stress could also develop, although this was not predicted to occur in this study.

4.2 Cladding Stress due to Rod Internal Gas Pressure

The first phase of this study consisted of calculating average cladding stresses as a result of gas production alone for a period of 300 years of dry storage, to see if gas pressure alone was sufficient to cause a significant cladding stress.

The decay gas production inventory for 17x17 PWR fuel at a burnup of 65 GWd/MTU was obtained from an ORIGEN [7] calculation. The number of moles of hydrogen, helium, krypton, nitrogen, neon, radon, and xenon gas produced per MTU during storage (hereafter referred to as the decay gas) was calculated and added to the gas already inside the fuel rod (consisting of the mixture of the initial helium fill gas and the fission gas released during reactor operation). Results were obtained first assuming that none of the decay gas produced was released from the fuel material, and then assuming that all of the decay gas produced was released. These two extreme cases bound the range of possibilities, and were chosen because the gas release during storage was initially assumed to be unknown, and cannot be calculated with any degree of certainty using the fuel performance codes.

In order to capture the case leading to the highest rod internal pressure, and thus the highest cladding stress, the seven fuel designs shown in Table 1 were modeled and the rod internal pressure and rod free volume were compared between fuel designs and power histories. For any given power history, the final fuel burnup achieved was 65 GWd/MTU, and the fuel design resulting in the highest rod internal pressures was the 17x17 PWR fuel design. In addition, the power history that had the highest average linear heat generation for the duration of the irradiation resulted in the highest pressures for any given design, thus the 17x17 fuel design with the most aggressive power history resulted in the highest rod internal pressures. Coincidentally, this particular fuel design and power history also resulted in the smallest predicted rod free volume, which would contribute to obtaining higher rod internal pressure and higher cladding stress predictions. Furthermore, the cladding for the 17x17 fuel design was the thinnest, which would also result in a higher cladding stress as a result of rod internal pressure.

The ideal gas law was used to calculate the rod internal pressure as a function of time during storage. The rod free volume was assumed to be constant and equal to the value calculated by FRAPCON at the end of reactor irradiation. Using a constant volume leads to a conservatively high cladding stress, since it ignores the volume increase due to cladding creep during storage, which occurs while the canister temperature is above 200°C. The number of gas moles was equal to the sum of the number of moles present in the rod

at the end of reactor irradiation, as calculated by FRAPCON, to which was added the number of moles corresponding to the production and release of decay gases (either none, or all decay gases were assumed to be released into the rod free volume). Finally, the temperature of the gas in the fuel rod was assumed to be equal to the peak temperature of the cladding.

Fuel Design	Number of Power Histories
Westinghouse PWR 17x17	26
Westinghouse PWR 15x15	26
Combustion Engineering PWR 16x16	37
Combustion Engineering PWR 14x14	37
General Electric BWR 10x10	39
General Electric BWR 9x9	39
General Electric BWR 8x8	39
TOTAL	243

Table 1: Fuel designs and power histories modeled in this study

The cladding stresses were calculated based on the rod internal pressure and the stress relationships for a pressurized thick-wall tube. This resulted in the highest hoop stress at the inner wall of the cladding, and the lowest hoop stress at the outer wall of the cladding. Typically, the value of the hoop stress at the outer wall of the cladding. Typically, the value of the hoop stress at the outer wall of the cladding. The stress values reported in Section 6.1 are the maximum stress values, corresponding to those at the inner surface of the cladding. Importantly, this calculation did not explicitly model any stress concentration that would arise because of the presence of pellets in the cladding, and assumed that the cladding was a perfect cylinder, thus resulting in axial and circumferential average stresses.

4.3 Fuel Pellet Swelling during Extended Spent Fuel Storage

A literature survey was performed in search for information and data related to long term swelling of spent nuclear fuel (or surrogate materials). The fuel swelling is caused by alpha-decay of the radioactive elements in spent fuel, where the alpha particles knock on atoms in the UO_2 lattice and create Frenkel pairs, defects that ultimately result in lattice swelling. It should be noted here that the references provided in this paper do not constitute all the references surveyed as part of the literature review that was performed: only the references that contained useful data to develop a fuel swelling correlation for commercial spent fuel storage are listed. References containing data for actinides other than uranium and plutonium, such as curium or americium, are not listed here and were not used to develop the spent fuel correlations. Two swelling correlations were developed for future cladding stress analyses: a best-estimate (BE, or average) correlation, and an upper-limit (UL, or bounding) correlation.

The literature survey revealed two important pieces of information. First, there was overwhelming agreement in the literature that fuel swelling due to self-irradiation saturates over time (as dose and damage are accumulated) [4] [8] [9] [10] [11] [12] [13] [14]. This is likely due to defect cluster formation and because vacancy clusters eventually act as virtually infinite sinks for additional defects, as is the case for neutron irradiation of some metals. The assumption of saturation of fuel swelling was used in the development of the swelling correlations used in this study, as reflected in Equation 2. The second important point was that swelling behavior appears to be independent of dose rate [11], which supports the use of surrogates for accelerated aging of spent fuel.

Two types of swelling data were found in the literature survey. In both cases, the parameter measured by the experimentalists was the lattice relative expansion $\Delta a/a_0$. The relative lattice expansion parameter was either expressed as a function of displacements-per-atom (dpa) (this was usually the case for studies where only experimental data was provided, without any correlation developed to fit the data), or as a function of the cumulative α particle dose (expressed in α particles per atom: λt , where λ is the decay constant in α decays per atom per second, and t is time in seconds). The general form of the equations used to fit data is given in Equation 2. Whenever swelling was given in the literature as a function of dose instead of displacements per atom (dpa), a conversion from dose to dpa was performed using Equation 3, based on calculations reported in [15].

$$\frac{\Delta a}{a_0} = A \cdot \left(1 - e^{-B \cdot dpa}\right) \tag{2}$$

Where a is the lattice parameter, a_0 is the undeformed lattice parameter, and A and B are constants to be determined.

$$\frac{\text{dose(dpa)}}{\text{dose}(\alpha/g)} = \text{Constant} = 2.50 \times 10^{-19}$$
(3)

The swelling data gathered in the literature survey were analyzed and a best-estimate swelling correlation and an upper-limit swelling correlation were developed, given in Equation 4 and Equation 5. The parameters A and B for Equation 2 for the best estimate (average) and bounding (maximum) correlations are provided in Table 2 and shown along with all the data and curve fits relevant for extended storage of spent nuclear fuel in Figure 2. It should be noted that one data point is not bounded by the 'bounding' correlation (Rondinella's UO₂ with 10 % 238PuO₂), but this was deemed acceptable because that particular set of data then saturates at a level below the bounding swelling curve. The relationship between time and dpa (shown in Figure 2) was derived by fitting lines from [16] for UO₂ fuel at 60 GWd/MTU, and is given by Equation 6.

$$\frac{\Delta a}{a_0} = 3.528 \times 10^{-3} \cdot \left(1 - e^{-8.492 \cdot dpa}\right) \text{(Best-estimate)}$$
(4)

$$\frac{\Delta a}{a_0} = 4.642 \times 10^{-3} \cdot \left(1 - e^{-28.077 \cdot dpa}\right)$$
(Upper-limit) (5)

$$dpa(t) = 1.1742 \times 10^{-2} \times t^{7.2246 \times 10^{-1}}$$
(6)

It can easily be demonstrated analytically that the volumetric expansion is equal to three times the lattice expansion. Other phenomena that could lead to additional swelling include gas accumulation and the formation of bubbles. However, a recent experimental study by Wiss et al. [17] has shown that for a specimen of UO₂ doped with 10% of Plutonium-238 and observed after 16 years in storage (equivalent to 4 dpa damage), the lattice swelling was 1.3% in volume, with only an additional 0.01% volumetric swelling from bubbles in the lattice. Swelling due to gas bubble formation in the matrix is thus assumed to be negligible in UO₂ spent fuel in comparison with lattice swelling. Furthermore, studies by Ferry et al. [18] [19] have shown that the quantity of helium in the pellet required to achieve the critical bubble pressures (at which cracking would be expected) would only be reached after more than 10,000 years of storage. Consequently, additional pellet swelling due to crack formation is not expected to occur during the 300 year dry storage period, and was not taken into account in this study.

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Swelling Curve/Data	Reference	А	B (1/dpa)
Chilkalla-238PuO ₂	[8]	3.181E-03	16.800
Kato-MOX average	[20]	2.900E-03	1.977
Noe-238(99 %)PuO ₂	[10] 2.830E-03		1.722
Rand-239PuO ₂	[21] 3.900E-03		1.387
Rondinella-UO ₂ -10 %238PuO ₂ -α doped	[4]	4.642E-03	4.337
Roof-238PuO ₂	[13]	3.725E-03	5.384
Schmidt-238PuO ₂	[22]	3.200E-03	1.770
Turcotte-238(80 %)PuO ₂	[23]	3.200E-03	2.208
Weber-238PuO ₂ -self	[14]	3.200E-03	28.077
Weber-UO ₂ - α recoil damage model	[14]	4.500E-03	16.727
Average Swelling Correlation		3.528E-03	8.039
Bounding Swelling Correlation		4.642E-03	28.077

Table 2: Swelling correlation constants for Equation 2 for relevant references



Figure 2: Fuel swelling data and correlations as applicable to extended storage of spent nuclear fuel [4] [8] [9] [10] [11] [12] [13] [14] [20] [22] [23]. Also shown for comparison with data are the best-estimate and upper-limit fuel swelling correlations developed in this study. The dpa as a function of time are from reference [16] and can be used to indicate the amount of fuel swelling as a function of storage time.

4.4 Decay Gas Production and Release during Extended Spent Fuel Storage

During irradiation, fission gas production and release is the main source of additional gas in a fuel rod, but as soon as the reactor is shut down, decay gas production and release becomes the main source of additional gas in the fuel rod. The decay gas, mostly helium, can either be retained inside the fuel pellets, or released to the rod free volume. The specific decay gas production inventory for 17x17 PWR spent fuel at a burnup of 65 GWd/MTU was obtained from an ORIGEN [7] calculation. The number of moles of H, He, Kr, N, Ne, Ra, and Xe gas produced per MTU during storage (hereafter referred to as the decay gas)

was calculated and added to the gas in the fuel rod (consisting of the mixture of the initial helium fill gas and the fission gas released during reactor operation and storage). The same specific decay gas production was used for all fuel designs analyzed.

Cladding stresses were calculated using two different assumptions. First, it was assumed that 100 % of the decay gas produced during storage would be released to the rod free volume, regardless of whether the diffusion rates of decay gases in the spent fuel matrix were high enough to enable such a release. Although highly unrealistic, this assumption was made to obtain bounding cladding stresses for the most conservative case possible. The second calculation was more realistic, though still conservative, and used a decay gas release fraction based on results from a recent experimental study by Talip et al. [24].

The study by Talip et al. on Pu-238 doped UO_2 to simulate high burnup fuel [24] indicates that the release rate of helium atoms is 'too low to be measured at the temperatures of interest for nuclear waste disposal', with the release rate being virtually zero below 700 K, as shown in Figure 3(a). As a result, it was conservatively assumed that if the fuel temperature was above 650 K, all the decay gas produced since reactor shutdown and up to that time would be released, but that once the fuel temperature was below 650 K, no further release could occur. This assumption is conservative for several reasons. First, the temperature chosen as a threshold for decay gas release is 50 K lower than that reported in reference [24]. Second, assuming that all the decay gas is released is conservative because except for hydrogen, the other decays gases consist of nuclides whose atomic weight is larger than that of helium, and thus who will have a harder time diffusing through the fuel to be released. Since helium release cannot be measured under 700 K, the release of other decays gases is not expected either (except for hydrogen, which represents less than one thousandth of the total decay gas produced after irradiation). The conservatism applied in determining the fraction of decay gas release during storage is believed to be sufficient to also account for any potential further release at lower temperatures as a result of the creation of open pathways for gas release. Finally, fission gas release was also computed by FRAPCON and added to the rod gas inventory, although it should be pointed out that fission gas release after reactor shutdown was very close to zero (less than 0.01 %), and could have been neglected.

The fuel temperature only exceeded 650 K during the first ~2 years of dry storage, implying that all the gas produced from reactor shutdown until ~7 years into storage was released to the gap. Effectively, this was similar to assuming that approximately 5 % of the decay gas produced over the 300 year period of storage was released in the first 2 years of dry storage (years 5 to 7 of the total storage period, after the first 5 years in wet storage in the spent fuel pool), as shown in Figure 3(b-c). The influence of the decay gas release fraction was also studied for 2 select cases, as described in Section 6.2.3.



Figure 3: (a) Comparison of the experimental helium release rate as a function of temperature (grey dots) with the modelled release profile in solid lines for the 238Pu doped UO₂ sample (from [24]); Decay gas production and realistic decay gas release fraction (normalized to the total amount of gas produced in 300 years), as well as temperature, for (b) close up on the first 50 years of storage, and (c) the entire 300 years of high burnup spent fuel storage. The red boxes in (a) highlight the period during which the temperature is above 650 K and thus during which decay gas release occurs.

4.5 Cladding Stresses from Decay Gas Production and Release, and Fuel Pellet Swelling

After calculating the cladding stress due to decay gas production alone, and subsequently developing fuel swelling correlations for storage, the cladding stresses and strains as a result of the combination of decay gas production and fuel pellet swelling during storage were determined using FRAPCON-3.5 for high burnup fuel irradiated to 65 GWd/MTU stored for a period of 300 years.

FRAPCON-3.5 (see Section 5.1) was employed to model the release of decay gas produced during storage simultaneously with fuel pellet swelling using the correlations developed for best-estimate and for upperlimit swelling. First, 100 % release of decay gases was assumed, then a more realistic calculation was performed, based on release decay gas release rates from Talip et al. [24] In addition, for the two most common fuel types (PWR 17x17 and BWR 10x10), the power history resulting in the highest cladding stress during the 300 year storage period was further analyzed to determine the impact of the decay gas release fraction on the cladding stress. The temperature history applied was identical to that used when calculating the stresses due to decay gas alone, as given by Equation 1. The stresses calculated by FRAPCON-3.5 assume a perfectly cylindrical pellet within a perfectly cylindrical cladding tube at each axial node modeled, thus resulting in circumferential average stresses being calculated for each axial node modeled. The maximum hoop stresses calculated with FRAPCON-3.5 were then used as boundary conditions for a FRAPCON-DATING (see Section 5.2) calculation where both the temperature history and stress history were specified. An important goal of this step was to obtain an accurate calculation of cladding creep strain over the period of storage, since FRAPCON-DATING was specifically developed for this purpose, while the default creep models in FRAPCON -3.5 are specifically tuned to predict in-reactor creep.

5 Computational Tools

In order to predict spent fuel cladding stresses over an extended period in dry storage, the NRC steadystate fuel performance code FRAPCON was used to model the behavior of spent fuel. Two different versions of the FRAPCON code were used in this study: FRAPCON-3.5 [25], which can take user input values to model pellet swelling and gas production in spent fuel, and a modified version of FRAPCON-3.5, to which the FRAPCON-DATING code was added for spent fuel cladding creep calculations.

5.1 FRAPCON-3.5

In order to account for cladding stresses due to decay gas production and fuel pellet swelling during the extended dry storage period, the FRAPCON-3.5 code [25] is able to model additional gas production and fuel pellet swelling based on user input. Importantly, FRAPCON-3.5 contains models for cladding creep, and these were active at all times in the calculations performed for this study. However, the creep models in FRAPCON-3.5 were developed to provide best results under reactor irradiation conditions, as opposed to wet or dry storage conditions. As a result, it was decided that FRAPCON-3.5 could be used to obtain reliable predictions of cladding stress, which could then be used as boundary conditions for a FRAPCON-DATING calculation to obtain reliable cladding strain predictions as a result of creep during dry storage.

The default FRAPCON-3.5 mechanical model neglects the stress-induced deformation of the fuel, and is thus called the 'rigid pellet model'. The deformation analysis in FRAPCON consists of a small deformation analysis that includes stresses, strains, and displacements in the fuel and cladding for the entire fuel rod. This analysis is based on the assumption that the cladding retains its cylindrical shape during deformation, and includes the effects of (1) fuel thermal expansion, swelling, densification, and relocation; (2) cladding thermal expansion, creep, and plasticity; and (3) rod internal gas and external coolant pressures. All of the phenomena mentioned here are active at every time step in the FRAPCON calculations, including during the wet and dry storage periods, thus any prediction of gap width or stresses take into account these phenomena. The cladding deformation model in FRAPCON is based on the following assumptions: (1) incremental theory of plasticity; (2) Prandtl-Reuss flow rule; (3) isotropic work-hardening; (4) thick wall approximation; (5) negligible bending strains and stresses in cladding; and (6) axisymmetric loading and deformation of cladding. The fuel deformation model in FRAPCON is based on the following assumptions: (1) thermal expansion, swelling, and densification are the only sources for fuel deformation; (2) no resistance to expansion of fuel; (3) no creep deformation of fuel; and (4) isotropic fuel properties.

5.2 FRAPCON-DATING

The DATING code was developed in the late 1980s [26] and updated in the early 2000s [27]. Its original purpose was to calculate allowable temperatures for dry storage of spent nuclear fuel, based on the concept of life fraction for creep failure. DATING uses the DOE's Commercial Spent Fuel Management (CSFM) Rev.1 creep models [28]. CSFM Rev. 1 contains six creep mechanisms for steady-state creep: Coble creep, Nabarro-Herring creep, grain boundary sliding (GBS), athermal creep, low temperature climb (LTC), and high temperature climb (HTC). The active creep mechanism depends on the stress and temperature of the cladding. GBS, LTC, and HTC are the creep mechanisms that are active and significant within the range for dry cask storage. The CSFM Rev.1 creep models have been modified from Rev. 0 [29] to include the addition of a primary creep model, changes to the unirradiated creep coefficients for GBS, LTC and Coble creep mechanisms, and incorporation of a creep reduction factor due to irradiation damage for some creep mechanisms.

Among other features, DATING is able to calculate cladding creep strains based on a known temperature history and either the calculated cladding stress based on the rod internal pressure evolution over time (used to compare code predictions in Section 5.3), or based on a known stress that is provided as input to the code (used for actual dry storage cladding strain predictions). Importantly, DATING is not capable of modeling additional gas production during storage, nor fuel pellet swelling during storage. As a result, the cladding strains predicted by FRAPCON-DATING are based entirely on the stress history calculated using the default models in FRAPCON-3.5, and subsequently provided as input to the DATING code. The DATING code was chosen over FRAPCON-3.5 for its ability to predict cladding creep strains (with an imposed stress as calculated by FRAPCON-3.5), mainly because the DATING creep models were developed for dry storage conditions, which is not the case for the default creep models used in FRAPCON-3.5.

For the purposes of this study, the DATING code was incorporated in to the NRC's steady-state fuel performance code FRAPCON-3.5 as a module, resulting in the so-called FRAPCON-DATING code. Specifically, the code was modified to add a flag to activate the DATING module if the user so chooses. If the DATING module is activated by the code user, an additional input block must be entered in the input file, containing all the variables used by the DATING module. When FRAPCON and DATING are coupled, the DATING cladding creep calculation takes place immediately after the dry storage time step modeled in FRAPCON. DATING uses cladding creep laws to predict the cladding creep strain rate, which is integrated over time to obtain the cladding strain. The temperature and stress histories used by the DATING module to calculate the cladding strain were user-specified. The temperature history was that described by Equation1, and the stress was imposed equal to that calculated by FRAPCON-3.5 for each of the fuel design and power history modeled (see Table 1).

5.3 FRAPCON-3.5 and FRAPCON-DATING Comparison

In order to validate the predictions made by FRAPCON-3.5 for dry storage conditions, a calculation was performed with FRAPCON-3.5 without any additional gas production and fuel pellet swelling, and the results were compared with those from FRAPCON-DATING. For this comparison, as for the rest of the calculations performed with FRAPCON-DATING and FRAPCON-3.5 in this study, seven different fuel designs were modeled, with a number of representative power histories for each fuel design, as shown in Table 1. Prior to performing calculations for spent fuel storage, it was confirmed that FRAPCON-DATING gave the same result as FRAPCON-3.5 for steady-state reactor irradiation. For the dry storage period,

Figure 4 and Figure 5 show comparisons of the maximum average cladding hoop stress and strain for BWR 10x10 and PWR 17x17 fuel (see Appendix A for other designs modeled).

For stress, the FRAPCON-DATING and FRAPCON-3.5 predictions were in very good agreement for the BWR cases (within 5MPa), while for the PWR cases, differences were in the range of 10-20 MPa in most cases, for the entire period of dry storage. Considering that the peak stresses predicted for BWR were around 35 MPa cases (for the more realistic decay gas release assumption), agreement within 5 MPa was deemed satisfactory. Similarly for PWR cases, since the maximum stresses predicted with the same assumptions was around 100 MPa, agreement within 20 MPa was also deemed acceptable.

For strain, the qualitative agreement was good, with strains accumulating only in the first 5 to 20 years of dry storage, and reaching a saturation value thereafter. For the BWR fuel designs, FRAPCON-3.5 generally predicted strains that were about 40 % lower than those predicted by FRAPCON-DATING. In contrast, for the P WR fuel designs, FRAPCON-3.5 generally predicted strains that were about 50-100 % higher than those predicted by FRAPCON-DATING. Such agreement within a factor of two was considered satisfactory given the code uncertainties and the complexity of the phenomena to be modeled.

In view of these comparisons, it was decided that FRAPCON-3.5 could be used to obtain reliable predictions of cladding stress, which could then be used as boundary conditions for a FRAPCON-DATING calculation to obtain reliable cladding strain predictions as a result of creep during dry storage. The FRAPCON-DATING code was chosen over FRAPCON-3.5 for its ability to predict cladding creep strains, mainly because the FRAPCON-DATING creep models were developed for dry storage conditions, which is not the case for the FRAPCON creep models used in FRAPCON-3.5.



Figure 4: 10x10 BWR (a) cladding stress and (b) cladding strain, as calculated by FRAPCON-DATING (green lines) and by FRAPCON-3.5 (red lines), for a period of 300 years of storage. The different lines of the same color correspond to different reactor irradiation power histories.



Figure 5: 17x17 PWR (a) cladding stress and (b) cladding strain, as calculated by FRAPCON-DATING (green lines) and by FRAPCON-3.5 (red lines), for a period of 300 years of storage. The different lines of the same color correspond to different reactor irradiation power histories.

6 Results and Discussion

6.1 Cladding Stress due to Rod Internal Gas Pressure

As described in Section 4.2, the cladding stress as a result of rod internal gas pressure was calculated for a 17x17 PWR fuel rod irradiated to 65 GWd/MTU with a very aggressive power history, corresponding to the case resulting in the highest rod internal pressures, and thus highest cladding stress as a result of rod internal pressure. Figure 6 shows the predicted hoop, axial, and radial cladding stress at the maximum hoop stress location (the cladding inner radius), for a period of 300 years of dry storage, assuming first that (a) 100 % of the decay gases are released, and then that (b) none are released. Importantly, this calculation did not account for any potential cladding creep or any other cladding deformation mechanism, such that the free volume of the fuel rod modeled was assumed to be constant. The value chosen for free volume was equal to the minimum free volume during in-reactor irradiation, which is a conservative value. During all of the storage period, including the drying phase when the fuel rod temperature is relatively high, the free volume predicted by FRAPCON was at least 25 % larger than the minimum value experienced during irradiation. Thus, using the minimum free volume during irradiation is a conservative choice that would lead to the highest cladding stress predictions for this analysis.

In both calculations, the initial maximum hoop stress is identical (~97 MPa) and initially decreases as the temperature rapidly decreases in the first ~50 years of storage. After the first 50 years of storage, in the case where 100 % of the decay gas is assumed to be released to the rod free volume, the pressure stabilizes, and thus so does the maximum hoop stress, at a value of ~73 MPa despite the fact that the temperature continues to drop. This is due to the increase in the number of moles of decay gases in the rod free volume. In contrast, if it is assumed that none of the decay gases are released, then the maximum hoop stress continues to decrease as the fuel rod temperature decreases, down to ~50 MPa after 300 years of storage. This decrease is dictated by the ideal gas law if the free volume and number of moles are constant and the temperature is decreasing.



Figure 6: Predicted cladding stresses assuming (a) 100 % release and (b) 0 % release of decay gases over a 300 year storage period.

The authors acknowledge that these two calculations present extreme cases, and that the reality is likely somewhere in between these two. The analysis tools used in this study did not have the ability to calculate the actual decay gas release during storage, and additionally, it is not clear that data is available to develop such models or to validate them. Nonetheless, in both cases analyzed, the maximum stress calculated (97 MPa) results in a ratio of cladding stress to yield stress (σ/σ_v) is less than 0.2 for high burnup zircaloy-2 or zircaloy-4 cladding at 400°C (for which σ_v ~500 MPa regardless of thermal mechanical treatment) [30]. Furthermore, both the stress and the temperature decrease rapidly in the first 10 years of storage, thus reducing the σ/σ_v ratio, such that the creep strain rate would decrease rapidly to zero. Such a low σ/σ_v ratio is not expected to result in large amounts of creep strain, and thus should not result in any creep failure. In addition, as discussed later in this paper and shown in Figure 8, the critical flaw size normalized to the cladding thickness for a stress of ~100 MPa for 17x17 PWR fuel is around 0.35 to 0.4, which is believed to be far too large to cause concern for DHC, since no such flaws are expected to exist in high burnup cladding (such a large flaw would have caused a cladding breach during in-reactor irradiation). Finally, much conservatism was introduced in the calculation (constant and minimal rod internal volume, largest initial rod internal pressure, and 100 % decay gas release), such that the stresses calculated in this analysis are likely significantly higher than those that would actually occur as a result of decay gas production and release alone. As a result, it was determined that decay gas alone will not be sufficient to cause cladding creep failures or delayed hydride cracking during a 300 year storage period. As a result, fuel swelling correlations were developed for spent fuel storage (see above), and stresses as a result of combined decay gases and fuel pellet swelling were evaluated (see below).

6.2 Combined Cladding Stresses and Strains

The analytical methods described above were used to calculate the combined stresses in the cladding of fuel rods irradiated to 65 GWd/MTU, as a result of both decay gas release and fuel pellet swelling. The calculations were performed first by conservatively assuming that 100 % of the decay gases were released to the rod free volume, and then assuming a more realistic decay gas release. In both cases, two sets of calculations were performed using FRAPCON-3.5 to calculate the cladding stress and FRAPCON-DATING to calculate the resulting strain: one for the best-estimate fuel swelling correlation, and one for the upper-limit fuel swelling correlation. In addition, for the PWR 17x17 and BWR 10x10 cases resulting in the highest stresses, a sensitivity study was performed to assess the impact of the decay gas release fraction.

The cladding stress reported here is the maximum average hoop stress for each fuel rod modeled. FRAPCON-3.5 does not take into account potential stress concentration due to heterogeneous PCMI at the pellet-pellet interfaces, or at cracks between pellet fragments. In addition, the friction that may occur between pellet fragments as the fuel pellet swells is not modeled in FRAPCON-3.5, thus any resulting heterogeneity in the PCMI stress is not taken into account in this study. Consequently, the stresses predicted in this study are azimuthal average stresses at each axial node of each fuel rod modeled, and the stress reported here is the maximum of these cladding hoop stresses.

Others have studied the effects of pellet-pellet interfaces and pellet cracking on the local distribution of stresses during in-reactor irradiation PCMI [31] [32] [33]. The typical stress concentration factors reported in the literature are approximately on the order of 1.3 to 4. In the present study, some degree of stress concentration due to cladding ridging and bambooing is likely. However, the magnitude of the stress concentration is unknown, and the stress concentration factors reported by others cannot be directly applied to the case of spent fuel during dry storage, because pellet swelling during storage is different from the thermal expansion mechanism that dominates in-reactor PCMI. Furthermore, the stress concentration that was studied for in-reactor PCMI was a result of direct contact between the pellets and the cladding, but in this study the gap is predicted to be open during wet storage because of the differential thermal expansion between the pellet and the cladding, and the gap width is predicted to increase in the early stages of dry storage due to the occurrence of outward cladding creep, as describe in more detail in Section 6.3. Consequently, the stress concentration factors resulting from direct contact between the pellet and the cladding are not applicable for the present study. Finally, it is important to point out that experimental validation of stress concentration predictions using stylized models as was done in the literature is extremely difficult, thus it is difficult to estimate the accuracy of the predictions. In conclusion, the stress concentration factors in this study are likely very low due to the lack of direct contact PCMI.

6.2.1 Highly Conservative Case: 100 % Decay Gas Release

The stresses calculated by FRAPCON-3.5 over a period of 300 years of dry storage assuming 100 % decay gas release are shown in Figure 7 and Figure 8 for BWR 10x10 and PWR 17x17 fuel, for both the bestestimate and the upper-limit fuel swelling correlations (see Appendix B.1 for all other fuel designs modeled). These calculated stresses were subsequently used as boundary conditions in FRAPCON-DATING for the calculations of cladding stress taking into account both decay gas release and fuel pellet swelling. Also shown on these figures is the corresponding cladding strain as calculated by FRAPCON-DATING. It can be seen that both fuel swelling correlations result in very similar stresses in the cladding, with the upper-limit fuel swelling generally resulting in slightly higher stresses in the cladding, as can be expected because more fuel swelling will simultaneously reduce the rod free volume, thus increasing rod gas pressure, and increase the contact stress if the gap is closed.



Figure 7: 10x10 BWR (a) cladding stress as calculated by FRAPCON-3.5 assuming 100 % decay gas release (with corresponding temperature, Terminal Solid Solubility for precipitation and for dissolution, TSSp and TSSd respectively, on secondary axis), and (b) resulting cladding strain as calculated by FRAPCON-DATING, for a period of 300 years of storage, using the best-estimate (BE) fuel swelling correlation (green lines), and the upper-limit (UL) swelling correlation (red lines). The different lines of the same color correspond to different reactor irradiation power histories.



Figure 8: 17x17 PWR (a) cladding stress as calculated by FRAPCON-3.5 assuming 100 % decay gas release (with corresponding temperature, Terminal Solid Solubility for precipitation and for dissolution, TSSp and TSSd respectively, on secondary axis and (b) resulting cladding strain as calculated by FRAPCON-DATING, for a period of 300 years of storage, using the best-estimate (BE) fuel swelling correlation (green lines), and the upper-limit (UL) swelling correlation (red lines). The different lines of the same color correspond to different reactor irradiation power histories.

In both the best-estimate swelling and upper-limit swelling cases, for BWR fuel designs, the stress stays approximately constant the first 5 years of storage; while for PWR designs, the stress decreases by about 15 % to 50 % on average during the first 10 to 20 years of storage. This initial decrease in stress is due to the high cladding creep rate that occurs during the first 10 to 20 years of storage and results in a very rapid increase in fuel rod volume and effective gap size, which in turn result in a stress decrease. The stress decrease is observed for PWR designs because cladding creep rate exceeds the fuel pellet swelling rate, thus resulting in a volume increase and the associated pressure decrease. Eventually, after the rapid cladding expansion in the hoop direction, fuel pellet swelling and decay gas release to the free volume (which both began as soon as the fuel was discharged from the reactor), as well as a decrease in the

cladding creep rate, all contribute to the cladding stress increase that is observed after 10-20 years of storage, for both PWR and BWR designs..

During the stress increase phase of the storage period (beyond 10-20 years), the cladding stress eventually reaches levels that could be high enough to cause hydride reorientation if the temperature is high enough to have significant hydride solubility [34]. Since hydride reorientation is a function of stress, temperature, and hydrogen solid solubility, Figure 7, Figure 8, and the figures in Appendix B.2 show the cladding stress, as well as the temperature and the hydrogen terminal solid solubility for dissolution and precipitation [35] (TSSd and TSSp, respectively).

The strains accumulated for the upper-limit fuel swelling correlation were larger than those accumulated for the best-estimate fuel swelling correlation by about 0.05 % (absolute) for the BWR fuel designs, and by about 0.02 % (absolute) for the PWR fuel. In all cases, the cladding creep strains were all accumulated in the first 25 to 50 years of storage. After this period, although the stress still increased due to decay gas production and fuel pellet swelling (see Figure 7, Figure 8, and Appendix B.1), the temperatures were too low to result in additional cladding creep. Low temperature creep (also called 'athermal creep') models are not programmed into FRAPCON-DATING, which is a limitation of the codes. However, the lack of cladding creep beyond 50 years results in smaller strains being predicted in these calculations. This in turn results in higher stress and smaller critical flaw size predictions, which is conservative. In fact, the cladding would accumulate larger positive hoop strains if cladding creep was permitted to occur beyond 50 years (corresponding to temperatures below ~200°C). The first consequence of a larger positive cladding hoop strain is that the rod internal volume would be larger, thus reducing the rod internal gas pressure. The second consequence would be that the fuel-to-cladding gap size would increase, thus reducing the likelihood of fuel swelling resulting in fuel-to-cladding contact, and consequently delaying the development of stresses due to PCMI. In both cases, assuming that no cladding creep occurs beyond 50 years results in conservative stress predictions.

Importantly, it should be noted that the FRAPCON-3.5 calculation predicted that in every single case modeled, the gap (or actually the equivalent gap, as described in Section 6.3) would be open after the first year or two of dry storage. This is due to the relatively large amounts of cladding hoop strain accumulated mostly due to creep at the high temperature experienced by the cladding in the early years of dry storage. For BWR designs, the hoop strain predictions from FRAPCON-DATING varied between 0.43 % and 0.49 %, while for PWR designs, they varied from 0.72 % to 1.00 %. Fuel and cladding differential thermal expansion also contributes to opening the gap, but to a lesser extent than cladding creep. It must thus be concluded that, after the initial drop in cladding stress that occurs in the first 5 to 15 years, the constant increase in cladding stress over the remainder of the 300 year dry storage period is due to a gradual reduction of the rod free volume as the fuel pellets swell, combined with the release of decay gas to the rod free volume. The fuel pellet swelling reaches 90 % of its saturation value at about 50 years for the upper-limit swelling and at about 150 years for the best-estimate swelling. After the time when fuel pellet swelling saturates, the cladding stress increase is mainly due to the release of decay gas.

6.2.2 Conservative Decay Gas Release Based on Helium Data

The stresses calculated by FRAPCON-3.5 and the corresponding cladding strain as calculated by FRAPCON-DATING over a period of 300 years of dry storage assuming realistic decay gas release are shown in Figure 9 and Figure 10 for BWR 10x10 and PWR 17x17 fuel, for both the best-estimate and the upper-limit fuel swelling correlations (see Appendix B.2 for all other fuel designs modeled). These calculated stresses were

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subsequently used as boundary conditions in FRAPCON-DATING for the calculations of cladding stress taking into account both decay gas release and fuel pellet swelling. It can be seen that both fuel swelling correlations result in very similar stresses in the cladding, with the upper-limit fuel swelling generally resulting in slightly higher stresses in the cladding, as can be expected because more fuel swelling will simultaneously reduce the rod free volume, thus increasing rod gas pressure, and increase the contact stress if the gap is closed. Interestingly, the cladding strains predicted for the more realistic decay gas release are slightly higher than those predicted with 100 % decay gas release. This difference is a result of the fact that the initial stress during the first ~2 years of dry storage is slightly higher in the case of the more realistic decay gas release.



Figure 9: 10x10 BWR (a) cladding stress as calculated by FRAPCON-3.5 assuming realistic decay gas release (with corresponding temperature, Terminal Solid Solubility for precipitation and for dissolution, TSSp and TSSd respectively, on secondary axis), and (b) resulting cladding strain as calculated by FRAPCON-DATING, for a period of 300 years of storage, using the best-estimate (BE) fuel swelling correlation (green lines), and the upper-limit (UL) swelling correlation (red lines). The different lines of the same color correspond to different reactor irradiation power histories.



Figure 10: 17x17 PWR (a) cladding stress as calculated by FRAPCON-3.5 assuming realistic decay gas release (with corresponding temperature, Terminal Solid Solubility for precipitation and for dissolution, TSSp and TSSd respectively, on secondary axis), and (b) resulting cladding strain as calculated by FRAPCON-DATING, for a period of 300 years of storage, using the best-estimate (BE) fuel swelling correlation (green lines), and the upper-limit (UL) swelling correlation (red lines). The different lines of the same color correspond to different reactor irradiation power histories.

In both the best-estimate swelling and upper-limit swelling cases, the cladding stress decreases over the 300 year period of storage. The stress decrease is more pronounced for PWR designs because the rod internal pressure is higher, thus leading to faster cladding creep and a larger amount of cladding hoop strain due to creep, as well as a longer time during which the cladding experiences this rapid creep deformation. Because decay gas release stops after ~2 years of dry storage, once the temperature decreases below 650 K, the stress increase that was observed for the case of 100 % decay gas release is not predicted to occur for realistic decay gas release. As shown in Figure 9 and Figure 10, as well as Appendix B.2, the final stress levels after 300 years of storage are around 25 MPa for BWR fuel designs, and between 30 and 35 MPa for PWR fuel designs.

The strains accumulated for the upper-limit fuel swelling correlation were larger than those accumulated for the best-estimate fuel swelling correlation by about 0.05 % (absolute) for the BWR fuel designs, and by about 0.02 % (absolute) for the PWR fuel. In all cases, the cladding creep strains were all accumulated in the first 25 to 50 years of storage. After this period, the stress and the temperatures were too low to result in additional cladding creep (see Figure 9, Figure 10, and Appendix B.2). As was mentioned above, Low temperature creep (also called 'athermal creep') models are not programmed into FRAPCON-DATING, which is a limitation of the codes. However, the lack of cladding creep beyond 50 years results in smaller strains being predicted in these calculations. This in turn results in higher stress and smaller critical flaw size predictions. Finally, the FRAPCON-3.5 calculation predicted that in every single case modeled, the gap would reopen when the fuel is put into wet storage, and significantly increase in width after the first year or two of dry storage. This is due to the relatively large amounts of cladding hoop strain accumulated mostly due to creep at the high temperature experienced by the cladding in the early years of dry storage.

Importantly, it should be noted that, as was the case for the 100 % decay gas release assumption, the FRAPCON-3.5 calculation predicted that in every single case modeled, the gap (or actually the equivalent gap, as described in Section 6.3) would be open after the first year or two of dry storage. For BWR designs, the hoop strain predictions from FRAPCON-DATING varied between 0.46 % and 0.54 %, while for PWR designs, they varied from 0.78 % to 1.04 %.

6.2.3 Sensitivity Study on Decay Gas Release

In order to understand the impact of the decay gas release fraction chosen for this study, it was decided to study the influence of the decay gas release fraction on the high burnup cladding stresses and strains. This sensitivity study was performed for the power history producing the highest stresses during dry storage for 10x10 BWR fuel and 17x17 PWR fuel. In addition, in an effort to maximize stresses, the upper-limit fuel swelling correlation was chosen. The results for cladding stress and strain are shown in Figure 11 for the 10x10 BWR fuel design, and in Figure 12 for the 17x17 PWR fuel design.

For the 10x10 BWR design, a decay gas release fraction of around 10 % over the 300 year period of storage is required in order to have an increasing stress trend. For the 17x17 PWR fuel design, a decay gas release fraction of about 20 % is required to observe any increase in stress over the 300 year storage period. For decay gas release fractions smaller than those mentioned here, the cladding stress decreases over time, because the increase in the number of gas moles is not sufficiently large to overcome the decrease in pressure due to the decrease in temperature in the dry storage canister. This sensitivity study reveals that the conservative decay gas release fraction used in this study (which is around 5 %, as described in Section 4.4) can at least be doubled before any cladding stress increase would be observed for the 300 year period



of storage. Said in another way, there is a factor of at least two in margin for decay gas release fraction, with regards to increasing the cladding stress during storage.

Figure 11: (a) cladding stress as calculated by FRAPCON-3.5 assuming various fractions of decay gas release (DGR), and (b) resulting cladding strain as calculated by FRAPCON-DATING, for a period of 300 years of storage. The fuel design is 10x10 BWR fuel, and the power history is that resulting in the highest stresses for this fuel design.



Figure 12: (a) cladding stress as calculated by FRAPCON-3.5 assuming various fractions of decay gas release (DGR), and (b) resulting cladding strain as calculated by FRAPCON-DATING, for a period of 300 years of storage. The fuel design is 17x17 PWR fuel, and the power history is that resulting in the highest stresses for this fuel design.

Increasing the decay gas release fraction from 0 % to 100 % has a bigger impact on the resulting cladding strain for the 10x10 BWR design than for the 17x17 PWR design: 0.14 % increase versus 0.04 % increase. This is likely due to the fact that the relative pressure increase is higher for the BWR fuel designs, because of the fact that the initial rod internal pressure is lower in BWR fuel rods than it is in PWR fuel rods.

6.3 Gap Size and Pellet Inter-Fragment Free Volume

During irradiation in the reactor, as the fuel temperature increases, the extreme stresses resulting from the large temperature gradients in the fuel cause the pellets to crack and relocate. Cracks can be circumferential or radial, but are predominantly radial. The free volume in the fuel rod cross-section, which is originally in the fuel-cladding gap, is relocated into the fuel as fragments of fuel move outwardly into the fuel-cladding gap. During irradiation, because of thermal expansion and swelling, the fuel expands over time, filling some of the inter-fragment free volume that was created when the pellet initially cracked and relocated radially within the fuel-cladding gap. However, irregular pellet fragments do not align exactly, and thus some of the inter-fragment free volume remains as the pellet expands. This causes the fuel diameter to appear larger than if fragments lined up perfectly, and causes the fuel to interact with the cladding at a lower power or burnup than that expected due to normal expansion (or contraction) mechanisms, including thermal expansion, swelling, and densification. FRAPCON takes this phenomenon into account by only allowing 50 percent of the original fuel radial relocation to gradually be recovered upon soft contact between fuel and cladding. Once 50 % of the relocation has been recovered, hard contact is established between the fuel and the cladding. It should be noted here that since FRAPCON is 1.5D fuel performance code where symmetry along the central axis of the fuel rod assumed, no azimuthal variations are modeled in the code. As a result, the gap width is constant for a given axial elevation, and partial gap reopening is impossible in FRAPCON.

In this study, the code calculations predicted that the combination of differential thermal expansion, outward cladding creep, and fuel pellet swelling resulted in the fuel-to-cladding gap reopening as soon as the fuel is placed in wet storage. The prediction of an open gap is an artifact resulting from the mechanical models in the code used in this study (as described in detail in Section 5 and in the previous paragraph), and is typically not observed in actual micrographs of spent fuel. In reality, the free volume resulting from the prediction of gap reopening is distributed within the pellet and consists of pellet inter-fragment volume. In contrast, in FRAPCON, when the gap interfacial pressure is removed as the gap reopens, it is first assumed that the pellet expands radially from the 50 % relocation value that was achieved when hard contact between the pellet and the cladding occurred, back to the full extent of radial relocation that was predicted prior to pellet-cladding contact. This process effectively adds free volume within the pellet, and can be thought of as inter-fragment free volume. Any additional free volume that is created as a result of differential expansion or contraction between the pellet and the cladding is then attributed to an actual pellet-to-cladding gap. Upon moving the fuel to wet storage, FRAPCON predicted that the reopened gap was between 14 and 47 microns wide for BWR fuel designs, decreasing to between 8 and 45 microns due to pellet swelling during the 5 years of wet storage. Similarly, the predicted width of the reopened gap was between 10 and 49 microns for PWR fuel designs, decreasing to between 1 and 48 microns due to pellet swelling during the 5 years of wet storage.

In this study, the combination of differential thermal expansion, outward cladding creep, and fuel pellet swelling over the course of 300 years of dry storage never resulted in a closed gap prediction. These predictions indicate that significant amounts of inter-fragment free volume remain within the fuel rod cross section, such that did not predict any contact between the pellet and the cladding, resulting in no PCMI stress. Because of the lack of PCMI predicted in the code, the lack of accounting for heterogeneities within the fuel rod is not believed to have had a large impact on the predicted cladding stresses. At the end of wet storage and just prior to dry storage, the code predicted gap sizes between 8 and 45 microns for BWR fuel designs, and between 1 and 48 microns in PWR designs. Upon placement in dry storage, the relatively high initial temperature and the pressure differential across the cladding wall always resulted in the prediction of a relatively large amount of outward cladding creep within the first few years of dry storage, such that after 5 years of dry storage, the predicted gap width was between 25 and 70 microns for BWR fuel designs, and between 40 and 80 microns for PWR fuel designs. Furthermore, fuel swelling over time during dry storage was not sufficient to close the gap, as it only reduced the gap size by approximately 5 to 10 microns over a period of 300 years of dry storage.

6.4 Critical Flaw Size Calculations for Delayed Hydride Cracking

In addition to being used as boundary conditions for cladding creep strain predictions with FRAPCON-DATING, the stresses predicted by FRAPCON-3.5 when considering decay gas release and fuel pellet swelling were used to calculate a critical flaw size that could result in delayed hydride cracking (DHC) in high burnup fuel irradiated to 65 GWd/MTU. The critical flaw size for delayed hydride cracking can be calculated by solving Equation 7.

$$a_{\rm DHC}^{\rm crit} = \frac{1}{\pi} \times \left(\frac{K_{\rm H}}{\sigma \cdot Y(a_{\rm DHC}^{\rm crit})}\right)^2 \tag{7}$$

Where a_{DHC}^{crit} is the critical flaw size for DHC to occur, K_H is the stress intensity factor corresponding to the onset of DHC, σ is the cladding hoop stress, and $Y(a_{DHC}^{crit})$ is the geometry factor used to calculate the stress intensity factor. The value for K_H was conservatively chosen as a constant equal to 5 MPaVm [36]. The equation for the form factor $Y(a_{DHC}^{crit})$ was taken from [37] for a single edge crack under tension. Importantly, because of the form of Equation 7, the normalized DHC critical flaw sizes calculated for the stresses predicted by FRAPCON-3.5 are inversely proportional to the cladding stress for which they are calculated.

For the assumption of 100 % decay gas release fraction, since the cladding stress is predicted to increase during the 300 year period of dry storage, the critical flaw size is predicted to decrease over the same period of time, as shown in Figure 13(a) and Figure 14(a). For the BWR fuel designs, the DHC critical flaw size is predicted to be around ~16-18 % of the cladding thickness after 300 years of storage, while for the PWR designs it remains around 22-26 % of the cladding thickness after 300 years of storage. In both cases, assuming existing flaw sizes roughly equal to the oxide thickness at end of reactor irradiation, the proprietary licensing limits placed on cladding alloys for oxide thickness (to avoid oxide spallation and hydride blister formation), hydrogen content, or both, are such that the calculated DHC critical flaw sizes for 300 years of dry storage are significantly larger than that which are likely to exist in spent fuel cladding. This implies that DHC will not occur during the 300 years period of dry storage. Nonetheless, Table 3 shows the time, temperature, and stress at which the DHC critical flaw size is predicted to reach a highly conservative existing flaw size of 120 µm. It can be seen that none of the fuel designs reach a DHC critical flaw size of 120 µm prior to the end of the 300 year dry storage period, though the BWR designs are close to this threshold. A final observation is that although the calculations in this study were not performed beyond 300 years of dry storage, the cladding stress appears to continue to increase beyond this period, such that the critical flaw size would continue to decrease after 300 of dry storage, and could begin to be a cause for concern because of the increase likelihood of cracks deep enough to cause DHC initiation after 300 years. That said, these results consider that 100 % of the decay gases are released to the rod free volume, which is highly conservative and is an important contribution to the increasing cladding stress.

Fuel Design	Normalized flaw size at 120 μm	Time to reach 120 μm critical flaw size (years)	Temperature at 120 μm critical flaw size (K)	Stress at 120 μm critical flaw size (MPa)
BWR 8x8	0.1476	>300	<370	>185
BWR 9x9	0.1688	>300	<370	>188
BWR 10x10	0.1818	>300	<370	>192
PWR 14x14	0.1628	>300	<370	>153
PWR 15x15	0.1688	>300	<370	>146
PWR 16x16	0.1890	>300	<370	>153
PWR 17x17	0.1967	>300	<370	>145

Table 3: Temperature at conservative hydride reorientation threshold stress of 110 MPa; and time, temperature, and stress when critical flaw size reaches 120 µm, for all 7 fuel designs modeled.

In contrast with the highly conservative case of 100 % decay gas release, assuming a more realistic (though still conservative) decay gas release fraction results in a stress decrease over the 300 year period of storage. The stress decrease translates into an increase in the critical flaw size for DHC, as shown in Figure 13(b) and Figure 14(b). As a result, the critical flaw size is larger than 50 % of the cladding thickness for the entire duration of storage for BWR fuel designs (Figure 13 and Appendix C). For PWR fuel designs, the critical flaw size for DHC is larger than 35 % of the cladding thickness for the first 5 years of storage, and larger than 50 % of the cladding thickness beyond the first 5-7 years in dry storage (Figure 14 and Appendix C). Such large critical flaw sizes for the realistic decay gas release fraction indicate that DHC will not occur during the 300 year period of storage under these conditions. In fact, such large incipient flaws would have resulted in cladding failure during normal operation, and these failed fuel rods would have been detected prior to loading into a dry storage canister.



Figure 13: 10x10 BWR. Critical flaw size for DHC, normalized to the cladding thickness, for best-estimate fuel swelling (green lines) and for upper-limit fuel swelling (red lines), for a period of 300 years of storage, assuming (a) 100 % decay gas release, and (b) realistic decay gas release. The different lines of the same color correspond to different reactor irradiation power histories.



Figure 14: 17x17 PWR Critical flaw size for DHC, normalized to the cladding thickness, for best-estimate fuel swelling (green lines) and for upper-limit fuel swelling (red lines), for a period of 300 years of storage, assuming (a) 100 % decay gas release, and (b) realistic decay gas release. The different lines of the same color correspond to different reactor irradiation power histories.

7 Conclusions

To predict cladding stress in high burnup fuel during an extended period of 300 years in dry cask storage, a large number of calculations were performed with Excel and with modified versions of the NRC's steadystate fuel performance code FRAPCON-3.5, including FRAPCON-DATING. These calculations predicted cladding creep and the resulting stresses and strains during 300 years of storage. Fuel pellet swelling, as well as decay gas production and release during storage, were taken into account to produce the cladding stress predictions.

Taking into account only decay gas production and release into the rod free volume during storage, while ignoring cladding creep and fuel pellet swelling, did not produce sufficient stress to cause failure by low temperature creep or delayed hydride cracking.

Fuel swelling correlations were developed for spent fuel storage. A literature survey showed overwhelming agreement that fuel swelling saturates by the time 1 dpa is accumulated in the fuel matrix. The saturation values for fuel swelling during storage were approximately 1 % and 1.5 % volumetric swelling for the best-estimate and upper-limit correlations, respectively.

A more realistic, yet still conservative, decay gas release fraction as a function of temperature resulted in a little less than 5 % of the decay gas produced over 300 years to be released to the gap. A sensitivity study was done considering potential slow release of decay gas at a lower temperature over a longer period of time. A decay gas release fraction of at least 10 % or 20 %, for BWR and PWR fuel designs respectively, was needed to result in any stress increase over the 300 year period of storage.

The cladding stresses and cladding creep strains were calculated for high burnup fuel during 300 years of dry storage assuming 100 % decay gas release, and then assuming a more realistic (still conservative) decay gas release fraction. In both cases, the stress either stays constant or decreases due to cladding creep in the first decade or two of dry storage. In the case of 100 % decay gas release, this initial stress

decrease is followed by a steady increase in stress due to fuel pellet swelling as well as decay gas generation and release, while for the more realistic decay gas release, the stress continues to decrease.

Even when assuming 100 % decay gas release, the average cladding hoop stress did not reach levels that could potentially lead to hydride reorientation if the temperature and hydrogen solid solubility were sufficiently high. However, for BWR fuel designs, high enough stresses for hydride reorientation could be reached shortly after 300 years of storage, at a time when the temperature would be below 100°C, and the hydrogen solid solubility would be below ~20 wt.ppm.

The relatively large initial creep strains (0.46 % to 1.04 %) accumulated during the first few years of dry storage resulted in the fuel-to-cladding gap opening for every fuel design and power history modeled, regardless of the decay gas release fraction. Consequently, it must be concluded that any subsequent stress increase would be due to pellet swelling (which saturates between 50and 150 years), as well as decay gas production and release (for cases when the release fraction is high enough to result in a stress increase). These phenomena would respectively decrease rod internal volume and increase the number of rod gas moles, resulting in a pressure increase inside the rod, and thus increasing the cladding stress.

The critical flaw size for DHC to occur was calculated for the entire period of dry storage for all the cases modeled. In every occurrence, the DHC critical flaw size significantly exceeded any realistic flaw size that could be expected to exist in high burnup cladding at the end of reactor irradiation. As a result, DHC is not realistically expected to occur during a 300 year period of dry storage.

120 μ m can be chosen as a conservative value for the maximum flaw size expected to exist in spent nuclear fuel cladding at the end of irradiation. Even when assuming 100 % decay gas release, which is highly conservative, the stress required for the DHC critical flaw size to be smaller than 120 μ m would only be reached beyond the 300 year period of dry storage.

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A. Appendix: Cladding Stress and Strain Comparison between FRAPCON-DATING and FRAPCON-3.5 for no Gas Generation and no Pellet Swelling



Figure A- 1: 8x8 BWR (a) cladding stress and (b) cladding strain, as calculated by FRAPCON-DATING (green lines) and by FRAPCON-3.5 (red lines), for a period of 300 years of storage. The different lines of the same color correspond to different reactor irradiation power histories.



Figure A- 2: 9x9 BWR (a) cladding stress and (b) cladding strain, as calculated by FRAPCON-DATING (green lines) and by FRAPCON-3.5 (red lines), for a period of 300 years of storage. The different lines of the same color correspond to different reactor irradiation power histories.



Figure A- 3: 14x14 PWR (a) cladding stress and (b) cladding strain, as calculated by FRAPCON-DATING (green lines) and by FRAPCON-3.5 (red lines), for a period of 300 years of storage. The different lines of the same color correspond to different reactor irradiation power histories.



Figure A- 4: 15x15 PWR (a) cladding stress and (b) cladding strain, as calculated by FRAPCON-DATING (green lines) and by FRAPCON-3.5 (red lines), for a period of 300 years of storage. The different lines of the same color correspond to different reactor irradiation power histories.



Figure A- 5: 16x16 PWR (a) cladding stress and (b) cladding strain, as calculated by FRAPCON-DATING (green lines) and by FRAPCON-3.5 (red lines), for a period of 300 years of storage. The different lines of the same color correspond to different reactor irradiation power histories.

B. Appendix: Cladding Stress and Strain Predictions for Combined Cladding Stresses with Decay Gas Release and Pellet Swelling Taken Into Account



B.1 Highly Conservative Case: 100 % Decay Gas Release





Figure B- 2: 9x9 BWR (a) cladding stress as calculated by FRAPCON-3.5 assuming 100 % decay gas release (with corresponding temperature, Terminal Solid Solubility for precipitation and for dissolution, TSSp and TSSd respectively, on secondary axis), and (b) resulting cladding strain as calculated by FRAPCON-DATING, for a period of 300 years of storage, using the best-estimate (BE) fuel swelling correlation (green lines), and the upper-limit (UL) swelling correlation (red lines). The different lines of the same color correspond to different reactor irradiation power histories.



Figure B- 3: 14x14 PWR (a) cladding stress as calculated by FRAPCON-3.5 assuming 100 % decay gas release (with corresponding temperature, Terminal Solid Solubility for precipitation and for dissolution, TSSp and TSSd respectively, on secondary axis), and (b) resulting cladding strain as calculated by FRAPCON-DATING, for a period of 300 years of storage, using the best-estimate (BE) fuel swelling correlation (green lines), and the upper-limit (UL) swelling correlation (red lines). The different lines of the same color correspond to different reactor irradiation power histories.



Figure B- 4: 15x15 PWR (a) cladding stress as calculated by FRAPCON-3.5 assuming 100 % decay gas release (with corresponding temperature, Terminal Solid Solubility for precipitation and for dissolution, TSSp and TSSd respectively, on secondary axis), and (b) resulting cladding strain as calculated by FRAPCON-DATING, for a period of 300 years of storage, using the best-estimate (BE) fuel swelling correlation (green lines), and the upper-limit (UL) swelling correlation (red lines). The different lines of the same color correspond to different reactor irradiation power histories.



Figure B- 5: 16x16 PWR (a) cladding stress as calculated by FRAPCON-3.5 assuming 100 % decay gas release (with corresponding temperature, Terminal Solid Solubility for precipitation and for dissolution, TSSp and TSSd respectively, on secondary axis), and (b) resulting cladding strain as calculated by FRAPCON-DATING, for a period of 300 years of storage, using the best-estimate (BE) fuel swelling correlation (green lines), and the upper-limit (UL) swelling correlation (red lines). The different lines of the same color correspond to different reactor irradiation power histories.

B.2 Conservative Decay Gas Release Based on Helium Data



Figure B- 6: 8x8 BWR (a) cladding stress as calculated by FRAPCON-3.5 assuming 100 % decay gas release (with corresponding temperature, Terminal Solid Solubility for precipitation and for dissolution, TSSp and TSSd respectively, on secondary axis), and (b) resulting cladding strain as calculated by FRAPCON-DATING, for a period of 300 years of storage, using the best-estimate (BE) fuel swelling correlation (green lines), and the upper-limit (UL) swelling correlation (red lines). The different lines of the same color correspond to different reactor irradiation power histories.



Figure B- 7: 9x9 BWR (a) cladding stress as calculated by FRAPCON-3.5 assuming 100 % decay gas release (with corresponding temperature, Terminal Solid Solubility for precipitation and for dissolution, TSSp and TSSd respectively, on secondary axis), and (b) resulting cladding strain as calculated by FRAPCON-DATING, for a period of 300 years of storage, using the best-estimate (BE) fuel swelling correlation (green lines), and the upper-limit (UL) swelling correlation (red lines). The different lines of the same color correspond to different reactor irradiation power histories.



Figure B- 8: 14x14 PWR (a) cladding stress as calculated by FRAPCON-3.5 assuming 100 % decay gas release (with corresponding temperature, Terminal Solid Solubility for precipitation and for dissolution, TSSp and TSSd respectively, on secondary axis), and (b) resulting cladding strain as calculated by FRAPCON-DATING, for a period of 300 years of storage, using the best-estimate (BE) fuel swelling correlation (green lines), and the upper-limit (UL) swelling correlation (red lines). The different lines of the same color correspond to different reactor irradiation power histories.



Figure B- 9: 15x15 PWR (a) cladding stress as calculated by FRAPCON-3.5 assuming 100 % decay gas release (with corresponding temperature, Terminal Solid Solubility for precipitation and for dissolution, TSSp and TSSd respectively, on secondary axis), and (b) resulting cladding strain as calculated by FRAPCON-DATING, for a period of 300 years of storage, using the best-estimate (BE) fuel swelling correlation (green lines), and the upper-limit (UL) swelling correlation (red lines). The different lines of the same color correspond to different reactor irradiation power histories.



Figure B- 10: 16x16 PWR (a) cladding stress as calculated by FRAPCON-3.5 assuming 100 % decay gas release (with corresponding temperature, Terminal Solid Solubility for precipitation and for dissolution, TSSp and TSSd respectively, on secondary axis), and (b) resulting cladding strain as calculated by FRAPCON-DATING, for a period of 300 years of storage, using the best-estimate (BE) fuel swelling correlation (green lines), and the upper-limit (UL) swelling correlation (red lines). The different lines of the same color correspond to different reactor irradiation power histories.



C. Appendix: Critical Flaw Size Calculations





Figure C- 2: 9x9 BWR Critical flaw size for DHC, normalized to the cladding thickness, for best-estimate fuel swelling (green lines) and for upper-limit fuel swelling (red lines), for a period of 300 years of storage, assuming (a) 100 % decay gas release, and (b) realistic decay gas release. The different lines of the same color correspond to different reactor irradiation power histories.



Figure C- 3: 14x14 PWR Critical flaw size for DHC, normalized to the cladding thickness, for best-estimate fuel swelling (green lines) and for upper-limit fuel swelling (red lines), for a period of 300 years of storage, assuming (a) 100 % decay gas release, and (b) realistic decay gas release. The different lines of the same color correspond to different reactor irradiation power histories.



Figure C- 4: 15x15 PWR Critical flaw size for DHC, normalized to the cladding thickness, for best-estimate fuel swelling (green lines) and for upper-limit fuel swelling (red lines), for a period of 300 years of storage, assuming (a) 100 % decay gas release, and (b) realistic decay gas release. The different lines of the same color correspond to different reactor irradiation power histories.



Figure C- 5: 16x16 PWR Critical flaw size for DHC, normalized to the cladding thickness, for best-estimate fuel swelling (green lines) and for upper-limit fuel swelling (red lines), for a period of 300 years of storage, assuming (a) 100 % decay gas release, and (b) realistic decay gas release. The different lines of the same color correspond to different reactor irradiation power histories.