BAW-10247PA
Revision 0
Supplement 1Q1NP
Revision 0

Realistic Thermal-Mechanical Fuel Rod Methodology for Boiling Water Reactors Supplement 1: Qualification of RODEX4 for Recrystallized Zircaloy-2 Cladding

Responses to NRC Request for Additional Information

March 2013

AREVA NP Inc.

AREVA

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Nature of Changes

Item	Page	Description and Justification
1.		This is a new document.

Contents

Tables

Figures

This document contains a total of 46 pages.

Response to NRC Request for Additional Information – BAW-10247PA Revision 0 Supplement 1P Revision 0

Question 1

1. *The following relates to cladding corrosion and hydriding.*

    a. *Please provide the individual corrosion data along with cladding temperature (radial and axial location and fluence/time) and RXA Zr-2 model prediction and identify the fuel design the data was taken from. Also, include the maximum power level and burnup for each of the operating cycles for each rod. Do any of these data come from plants with power uprates? If so, please identify the data along with percentage uprate in power along with uprated core average power. This will help assess whether the data is applicable to today's fuel designs and operating envelopes.*

    b. *No model was provided for hydrogen pickup of RXA Zircaloy-2. NRC has developed cladding embrittlement criteria for loss-of coolant accident (LOCA) and reactivity initiated accident (RIA) based on hydrogen content. Does AREVA intend to include a hydrogen pickup model for accident analyses? If so, does AREVA intend to submit hydrogen pickup models for RXA Zircaloy-2 and CWSRA Zircaloy-2 as part of this review, also please provide supporting hydrogen concentration data for these models.*

Response 1

The response to Question 1 will be provided at a later time.

Question 2

The following are related to understanding the thermal and irradiated creep model coefficients and verify the calculation of thermal and irradiated creep.

Question 2a

Possible Typos – i) Should there be parenthesis around each of the following terms, the axial creep rate term and the axial creep strain term in Equation 24 and 25. ii) The last line on page 10 refers to H_6, should this be H_5 instead? iii) The first sentence on page 11 suggests that the data were fine tuned rather than the model coefficients, please modify if this is not true. If the data were fine tuned please explain in detail how this was done.

Response 2a

i) All equations pertaining to the explicit formulation of the creep rate relationship have been enclosed in proprietary brackets. As such, Equation 24 has proprietary parenthesis, while Equation 25 was not bracketed, because it was considered that the main terms contained in Equation 25 have been enclosed in proprietary brackets in the preceding equations.

ii) Yes, there is a typo on the last line of page 10, where H_6 will be replaced by H_5.

iii) The meaning of the first sentence on page 11 is indeed, that the model parameters H_1 and H_5 have been fine-tuned against the long-term creep data; the word "against" was omitted inadvertently from the first sentence on page 11. The sentence will be corrected to clarify by inserting the word, "against."

Question 2b

Fitting parameters for thermal creep, H_2, H_3, and H_4 are not provided. Please provide these values. Also provide some discussion of how the primary creep is initialized at time=0.

Response 2b

The parameters H_2, H_3 and H_4 have not been changed for the re-calibration of the RXA Zircaloy-2 material type. These model parameters are related to the temperature dependence of the creep rate of Zircaloy, which is not dependent on [ ]. Their derivation is described in the Theory Manual, EMF-2994(P) Rev. 0 and succinctly repeated in Section 2.1 of Supplement 1.

With regards to creep initialization at time zero, the divergence of the strain rate equation (Equation 6 and other similar equations) when the strain in the denominator is zero is handled by using the integral of the strain rate in the implementation in the code. Therefore, the code calculates creep strain increments, which are not subject to divergence at time zero. This was described in Appendix C of the Theory Manual, EMF-2994(P) Rev. 0.

Question 2c

Fitting parameters, H_6 and H_7 are defined differently on pages 11 and 13. Please specify which values are used and why they are different on these pages.

Response 2c

The values of H_6 and H_7 on page 13 are the final values, which are used in the code. The values on page 11 are intermediate values, which were derived in the first stage of irradiation hardening model development, [
]. This is explained in the second to last paragraph on Page 11 of BAW-10247PA Rev. 0 Supplement 1 Rev. 0

Question 2d

Fitting parameters for irradiation creep, L_2 and L_4 could not be found in the submittal for the revised RXA Zr-2 model. Please provide these values.

Response 2d

The parameters L_2 and L_4 were not changed during the re-calibration for RXA Zircaloy-2 (similar situation as for the model parameters in Question 2b). Their values are, therefore, unchanged from those presented in the Theory Manual EMF-2994(P) Rev. 0, namely: [
].

Question 2e

Please provide equivalent figures to Figures 7.26 – 7.30 in EMF-2994(P) for the re-calibrated thermal and irradiation creep models for RXA Zr-2 material. Also provide the creep rate versus fluence on the same figure for thermal and irradiation creep at different stress levels up to 120 MPa that demonstrates the fluence level where irradiation creep dominates.

Response 2e

The figures similar to Figures 7.26 – 7.30 in EMF-2994(P) are provided below, as Figures 2-1 – 2-5, respectively. The response to the request of plotting the thermal creep alongside the irradiation creep at different stress levels, up to 120 MPa, vs. fast fluence is presented in Figures 2-6 – 2-8. The creepdown loading condition was used with the thick-wall stress calculation, as in the response to Question 4. Three hoop stress levels have been studied, namely, -40 MPa, -80 MPa and -120 MPa. These figures clearly demonstrate that creepdown is dominated by irradiation creep after a fast fluence of about [ ]. In addition, an outward creep/tensile loading condition was calculated at a higher hoop stress level of 200 MPa; Figure 2-9 shows that the thermal creep overtakes the irradiation creep at this high stress level at the beginning of irradiation and only after a fast fluence of about [ ], when the irradiation hardening process is complete (which slows down the thermal creep) does the irradiation creep become dominant. However, during short-term tensile loadings, associated with power ramps, the thermal creep is dominant at all fast fluences.

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Figure 2-1 Thermal creep of non-irradiated RXA Zircaloy-2 vs. equivalent stress [similar to Figure 7.26 of EMF-2994(P)]

[

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Figure 2-2 Thermal creep of non-irradiated RXA Zircaloy-2 vs. temperature [similar to Figure 7.27 of EMF-2994(P)]

[

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Figure 2-3 RXA Zircaloy-2 thermal creep vs. fast fluence [similar to Figure 7.28 of EMF-2994(P)]

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Figure 2-4 RXA Zircaloy-2 irradiation creep vs. equivalent stress [similar to Figure 7.29 of EMF-2994(P)]

[

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Figure 2-5 RXA Zircaloy-2 irradiation creep vs. fast flux [similar to Figure 7.30 of EMF 2994(P)]

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Figure 2-6 Thermal and irradiation creep for RXA Zircaloy-2 at -40 MPa

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Figure 2-7 Thermal and irradiation creep for RXA Zircaloy-2 at -80 MPa

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Figure 2-8 Thermal and irradiation creep for RXA Zircaloy-2 at -120 MPa

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Figure 2-9 Thermal and irradiation creep for RXA Zircaloy-2 at 200 MPa

Question 3

The following are related to understanding the creep data and how this data is used to develop the coefficients to the creep model.

Question 3a

Please confirm whether the creep data is from fuel rods or from non-fueled tubes (see b and c below).

Response 3a

A variety of data was used to construct the RODEX4 unified creep model, which predicts both thermal creep and irradiation creep. The irradiation creep parameters are derived from creepdown data obtained by profilometry of fuel rods at pool-side.

The thermal creep component is based partly on mechanical tests, used to define irradiation hardening, which are relevant for creep modeling because the RODEX4 model is applicable to both slower strain rate creep and to faster strain rate so-called plastic straining. The samples employed for these hot-cell tests were taken from fuel rods pre-irradiated in a commercial reactor.

The other model parameters of the thermal creep were derived from non-fueled tubes cut from as-manufactured cladding.

Question 3b

If data from fuel rods at what fluence level is hard contact established for the fuel designs from which the irradiated data were taken? Please provide references for the irradiated creep data at fluences below which fuel-cladding hard contact is not experienced if creep data is from fuel rods. If these data are not publicly available please provide the data and model predictions identifying the maximum hoop stress/pressure, temperature and fluence (fuel rod data). How were the diameters of these fuel rods accurately measured axially and azimuthally prior to irradiation? An issue with fuel rod creep data is that it is difficult to separate primary and secondary creep quantitatively due to the limited amount of data versus time/ fluence.

Response 3b

As in the original submittal, for both CWSR and RXA cladding the creepdown database was used, which consists of measured diameters of fuel rods after a number of power reactor cycles. This is stated on p. 14 of Supplement 1 at the beginning of Section 3.2. It is also mentioned that the creepdown database contains both "pure" creepdown data (before the onset of pellet-cladding mechanical contact), as well as data from the "creepout" stage after hard pellet-cladding mechanical contact was established. The transition between the two stages occurs between **[** **]** fast fluence as shown in Figure 8 of Supplement 1.

With regards to the measurement of the as-fabricated cladding, profilometry is the method employed to characterize dimensionally the fuel rods before being loaded in the power reactors. The as-fabricated tubes are scanned longitudinally by ultrasound techniques at several azimuthal angles so that the average cladding outer diameter, cladding thickness and ovality are determined.

The data are acquired for each tube lot and the statistics are used in the methodology applications as approved in BAW-10247PA. The detailed description of the uncertainty analysis of creepdown benchmarking, which includes the as-fabricated uncertainty component, is part of Response 5b.

Question 3c

If data is from non-fueled tubes supply plots of hoop strain vs. fluence for a given temperature and stress (for pressurized tubes held at relatively constant temperature).

Response 3c

As mentioned in response to Question 3a all data on creepdown were acquired from fueled rods. The burst mechanical tests used to define irradiation hardening were performed after irradiation on cladding samples cut from fuel rods after the fuel was removed. These data are described in Section 2.3.2 and Figures 3 and 6 of Supplement 1.

Question 4

Several assumptions have been applied in developing this creep model including the following: 1) radial stress can be ignored in determining creep in the hoop direction (Halden data appears to suggest it cannot be ignored); 2) the yield strength decrease in anisotropy in hoop and axial direction also applies to the creep anisotropy; and 3) P can be used to quantitatively define how creep anisotropy changes with fluence. The creep data provided is very limited and appears to be only in the compressive stress direction that suggests it does not provide justification for the above assumptions. In order to demonstrate that these and other assumptions are valid please provide comparisons to the following RXA Zr-2 in-reactor creep data.

- *In-reactor creep data for RXA Zr-2 cladding tubes from the following Halden experiments (Halden reports): IFA-585 (HWR-471, HWR-413 and HWR-677); IFA-663 (HWR-755); and cladding liftoff experiment IFA-610 (HWR-877, HWR-919).*
- *Also provide comparisons to the creep database for RXA Zr-2 cladding in Franklin, D.G., G.E. Lucas, A.L. Bement. 1983. "Creep of Zirconium Alloys in Nuclear Reactors", ASTM STP 815, American Society for Testing and Materials, West Conshohocken, PA.*
- *These comparisons of creep predictions and data should be for both tensile and compressive stress states when available and gap estimates for cladding liftoff. These data will help determine if the assumptions for the creep model are valid and if primary and secondary irradiation creep are modeled correctly in terms of fluence, temperature and stress.*

Response 4

The calibration of the thermal creep model was performed by using long-term creep tests on unirradiated cladding, as described in Reference 4-1. The same thin-wall stress analysis was employed as used before for the initial and pre-submittal calibrations for both cold work stress relieved (CWSR) and RXA cladding types (see References 4-2 and 4-3). As mentioned in Section 2.1.3 of Supplement 1 (Reference 4-1), a detailed analysis was performed regarding the use of thick-wall stresses and it was concluded that using the thin-wall stresses has no impact on the calibration of the thermal creep model parameters (Section 2.2 of Reference 4-4)

The justification of this assertion is as follows. The same relationship exists between the deviatoric stress components for the thick-wall analysis (when the radial stress is accounted for), as for the thin wall approximation, namely:

$$\sigma_\theta - \sigma_r = 2(\sigma_\theta - \sigma_z) = 2(\sigma_z - \sigma_r)$$

Therefore, Equation 13 of Reference 4-1, which links the generalized stress and the hoop stress remains of the same form when the hoop stress is replaced by the hoop stress minus the radial stress, while the link between the hoop strain and the generalized strain of Equation 14 of Reference 4-1 does not change.

The radial stress at the mean fiber for an internally pressurized tube is equal to minus half the internal pressure and thus the only difference between relations obtained by the thin wall approximation and the thick wall relations that account for radial stress, is the replacement of the hoop stress by the hoop stress minus half the internal pressure.

Therefore, the determination of the H_1 and H_5 model parameters from the linear relationship between the logarithm of the hoop strain and the hoop stress (Equation 25 of Reference 4-1) is not affected by the small difference between thin-wall and thick-wall stresses.

The simulation of the requested Halden tests is presented below. The IFA-585 test was already simulated in response to Q 12f of the RODEX4 RAIs (BAW-10247Q1P) and there is no impact of the new model parameters (i.e. the difference is within rounding-off range).

Simulation of the B&W creepdown experiment

The first requested verification is the creepdown experiment performed by B&W under a cooperative program with EPRI (References 4-5 and 4-6). The RXA Zircaloy-4, S2 material type was pre-pressurized to two levels, which created two levels of compressive hoop stresses. The test rods were guide tubes filled with SS mandrel pellets, to limit the creep-collapse in case it happened. The irradiation took place in the Oconee 2 reactor and profilometry of the test rods was carried out after each cycle. The measured data were taken from Reference 4-7 together with the fast flux and time values.

In order to calculate all three principal stresses with the thick-wall relationships, Equations 1 through 3 of Reference 4-1, the inner rod pressure and the outer coolant pressure must be known. References 4-5 through 4-7 provide the initial RT fill gas pressure and the clad average hoop stresses. Therefore, the inner gas pressure in hot conditions (based on the clad temperature provided in References 4-6 and 4-7, as test rods did not have any heat-generating pellets inside and the gamma-heating of the mandrel pellets is negligible), was calculated according to the gas law between room temperature and the operating temperature of around 305°C.

Next, the thin-wall hoop stress formula was used to determine the inside-outside pressure differential and the outer coolant pressure was estimated. Comparable values were obtained

for all three hoop stress values, which are in agreement also with the typical PWR coolant pressure of around 15 MPa. Fine tuning of the coolant pressure was performed to match the given hoop stress with the thick-wall formulas.

The most recent AREVA model parameters for RXA Zircaloy-2 were used together with the varying P anisotropy coefficient. The calculations presented in Figure 4-1 below are comparable with measurements considering the uncertainty associated with the test conditions and measurements. It may be noted that the initial clad dimensions and the profilometry technique have expected uncertainties as previously described in the responses to RODEX4 RAI questions (Reference 4-3). The reported clad outer diameter variation for the S2 cladding (the smallest of all cladding types) is equivalent to a variation of [ ] hoop strain.

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Figure 4-1 B&W creepdown simulation with RODEX4 RXA creep model and anisotropy coefficients

Simulation of the IFA-663 test

This experiment was dedicated to in-pile determination of creep behavior of pre-irradiated clad materials consisting of modern Zircaloy alloys. It was performed in an instrumented rig where two test rods (one consisting of three segments) were each connected to a separate external gas pressurization system for control of cladding stress.

Of interest to the current analysis is the lower rod, consisting of two Zry-2 (GE BWR cladding) segments, cut from fuel irradiated to 58 MWd/kgU in a commercial reactor.

The necessary cladding dimensional characterization and test conditions needed for creep calculation were taken from Reference 4-8. The Zry-2 rod was subject to three periods of different stress levels, the first one almost zero, the second one in compression and the final one in tension.

The calculations are displayed in Figure 4-2 below, which show diameter changes for the two segments. The difference in the test conditions between the lower and the upper segments is the fast flux, which is much lower for the lower segment (as being below the fast flux booster).

Therefore, the total deformation at the end of the test is lower for the lower segment. The negative deformation during the shorter and lower stress second compression period is smaller in absolute value compared to the longer and higher stress third tensile period.

As shown in Figure 4-2, the elastic component of the diameter change is the same for the two segments. In particular the change from the compressive to tensile is about [ ]. This is in good agreement with the measurements, which are only illustrated in Figures 7 and 8 in Reference 4-8.

With regards to the creep component of the diameter change during the tensile period, the calculated values are [ ] for the upper and lower segments, respectively. These calculations are in good agreement with the measurements as they can be inferred from the above mentioned figures of Reference 4-8.

It is remarked that the measurements are affected by uncertainty, as seen from Figure 7 of Reference 4-8, where the up and down profilometry runs gave different results. In addition, the measuring device scanned the segments along the same axial line. Because the rods are also subject to ovalization, the measured values are most likely different from the average diameter change that is calculated. Even if ovalization was accounted for in the calculation, it is still unknown which section of the ellipse is measured.

The relative evolution of the thermal and irradiation creep deformations is illustrated in Figures 4-3 and 4-4, for the upper and lowers segments, respectively. As mentioned, the lower segment was exposed to a much lower fast flux than the upper segment, which is reflected in the thermal creep being dominant for the lower segment, unlike the upper segment, for which the irradiation creep is dominant. This is consistent with the measured data presented in Figures 7 and 8 of Reference 4-8, where primary creep is noticed for the lower segment but not for the upper segment. As the calculation shows, the thermal primary creep is masked by the higher irradiation creep for the upper segment (see Figure 4-3); whereas the lower segment's dominant thermal creep provides a decreasing strain-rate, primary creep stage, when the stress value is changed from compressive to tensile.

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Figure 4-2 Benchmarking RODEX4 RXA creep model for IFA-663, GE segments

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Figure 4-3 Benchmarking RODEX4 RXA creep model for IFA-663, GE upper segment – evolution of thermal and irradiation creep components during the test

[

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Figure 4-4 Benchmarking RODEX4 RXA creep model for IFA-663, GE lower segment – evolution of thermal and irradiation creep components during the test

Simulation of the IFA-610.10 test

The objective of the test was to study the lift-off propensity of a high burn-up BWR rod. To that end the rod was instrumented with a central thermocouple and an elongation sensor while it was connected to a gas pressurization and gas circulation system. The clad deformation was inferred by calculation from the changes in the measured pellet centerline temperature (Reference 4-9 and 4-10) and this is the useful output to be compared with calculated cladding creep. The clad deformation is directly proportional to cold gap values, but the cold gap was only measured at the end of the irradiation. Therefore, comparing the clad deformation, as predicted by the RODEX4 model, to the Halden inferred clad deformation is equivalent to comparing to the final cold gap measurement and thus adequate for the simulation exercise requested by Question 4.

The internal rod pressure was set to five levels of over pressurization as described in Table 2 of Reference 4-9. The times at different over pressures were taken from Figure 4 of the same reference. The dimensions were found in Reference 4-10.

The measured (actually estimated from the cold gap measurements) creep strain at the end of the irradiation is a diameter change of [ ] (Figure 19 of Reference 4-9), while the calculated corresponding value is [ ] (Figure 4-5 below), which shows good agreement.

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Figure 4-5 Calculated diameter change during IFA-610.10 with RODEX4 creep model for RXA cladding

The axial elongation was also measured and the calculated values agree well with measurements. Comparing the final calculated elongation, illustrated in Figure 4-6 below with the measurement displayed in Figure 10 of Reference 4-9, an under-prediction can be noted. This under-prediction is due to the fact that the current modeling assumed an empty closed-end pressurized tube, while in the experiment pellet-to-cladding contact most likely occurred at pellet-pellet interfaces, which caused axial PCMI with the associated additional axial elongation.

The modeling of IFA-610.10 shows good agreement both in terms of evolution, as well as absolute values for both the hoop and axial deformations. This demonstrates the validity of the RODEX4 creep model for RXA Zircaloy-2 and especially its anisotropy model, which was able to reproduce the measured behavior, i.e., tensile hoop creep correlated with negative axial creep for the basically biaxial stress state (slightly different during periods of PCMI) with the 0.5 ratio between the axial stress and the hoop stress. It can be remarked that a CWSR material would have a positive axial creep under the same loading conditions.

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Figure 4-6 Calculated length change during IFA-610.10 with RODEX4 creep model for RXA cladding

References

4-1. "Realistic Thermal-Mechanical Fuel Rod Methodology for Boiling Water Reactors, Supplement 1: Qualification of RODEX4 for Recrystallized Zircaloy-2 Cladding," BAW-10247PA Revision 0 Supplement 1P Revision 0, AREVA NP Inc., Dec. 2009

4-2. "A Simple Zircaloy Viscoplastic Model and Its Validation against the Results of the ZODIAC Tests," EMF-2435, AREVA NP Inc., Feb. 2001

4-3. "Realistic Thermal-Mechanical Fuel Rod Methodology for Boiling Water Reactors," BAW-10247-PA, AREVA NP Inc., March 2008

4-4. "Qualification of the Thermal Creep Model for RXA Zry-2 of RODEX4 Thermal-Mechanical Fuel Rod Performance," 32-9117055-000, AREVA NP Inc., Nov. 2009

4-5. "EPRI/B&W Cooperative Program on PWR Fuel Rod Performance," EPRI NP-2848, March 1983

4-6. D. L. Batty, et al., "Deformation Characteristics of Cold-Worked and Recrystallized Zircaloy-2 Cladding," *Sixth International Symposium on Zirconium in the Nuclear Industry*, Vancouver, B.C., ASTM Code 04-824000-35, pp 306-339, 1982

4-7. D. G. Franklin, et al., "Creep of Zirconium Alloys in Nuclear Reactors," ASTM STP-815, Nov. 1983

4-8. H. Horn, "In-Reactor Creep of Various Fuel Claddings: Results from IFA-663," HWR-755, March 2004

4-9. S. Watanabe, "The lift-off Experiment IFA-610.10 with BWR Fuel Rod, In-Pile Data Evaluation," HWR-919, March 2010

4-10. M. Amaya, "The lift-off Experiment IFA-610.10 with a High Burn-up BWR UO_2 Fuel Rod: In-Pile Results During the First Irradiation Cycle," HWR-877, March 2008

Question 5

The following are related to understanding the application of the RXA Zr-2 creep model for licensing analyses and whether proposed application is justified.

Question 5a

Is RODEX4 creep calculated at mid-wall and if so is irradiated data adjusted for mid-wall creep? If not please explain how cladding creep is calculated?

Response 5a

The cladding model of RODEX4 consists of a [ ] radial mesh and thus, the radial displacements and stresses are calculated as average values over the [ ] radial intervals of the mesh and the strains are available at the [ ] nodal points. The axial strain is constant across the cladding thickness according to the axial symmetry assumption of the cladding model.

Therefore, the strains are available at inner, outer and mid-wall cladding locations. When RODEX4 is benchmarked against measured creepdown (resulting from profilometry of irradiated rods), of course, the cladding outer strain values are used since the profilometry measurements provide the outer diameter of the cladding. The cladding outer strain is also used in methodology applications in the context of the 1% strain criterion. It is worth mentioning that the outer oxide layer is taken into account, by subtracting the metal consumed by oxidation from the thickness of the outermost radial interval.

Question 5b

Provide justification for the use of the multiplier on sigma (Section 5.0) to obtain an upper bound model for irradiated creep given the limited amount of irradiated creep data. The multiplier does not appear to provide a 95/95 upper tolerance.

Response 5b

The determination of the creep (irradiation and thermal components) uncertainty followed the same procedure as initially reported in BAW-10247PA. Due to the update of the database and the use of [ ], the uncertainty values have changed compared with the initial Supplement. The following three figures illustrate the best-estimate, the upper-bound and the lower-bound cases.

Figure 5-1 illustrates the best-estimate calibration of the RODEX4 irradiation creep model, which relies on the prior calibration of the thermal creep model, and consists in determining the best-estimate value of the L_1 model parameter. Afterwards, the uncertainty bounds of this model parameter are determined.

In order to derive the uncertainty range of the irradiation creep and thermal creep models' parameters, the same procedure as approved for the RODEX4 topical is used.

The procedure takes into account a conservative estimate [ ] of the measurement uncertainty of [ ]. This measurement uncertainty consists of two components, namely initial clad diameter uncertainty and the PIE measured diameter uncertainty [Table 2.10 of BAW-10247Q4(P)] , which are statistically combined by the SRSS rule to provide a value of [ ] for the measurement uncertainty.

The fuel rods in the creepdown database are of ATRIUM-10 fuel design; therefore, the outer clad diameter is approximately [ ]. Thus, the measurement uncertainty of [ ], converted to strain, becomes [ ].

It can be concluded from Figure 5-1 that the range of the (calculation-measurement) values is conservatively (i.e., more than 95/95) at [ ].

The disagreement range of the (calculation-measurement) values is the combination by SRSS of the measurement uncertainty and the creep model uncertainty, expressed first as a strain value. Therefore the latter value can be determined as follows: creep strain uncertainty = [ ].

In order to determine the equivalent creep model parameter uncertainty, the runs illustrated in Figures 5-2 and 5-3 are used, which showed that a symmetrized [ ] variation of the creep rate due to model parameters' variation, together with the measurement uncertainty bound 95/95 the measured data. Of course, these runs stacked up the two uncertainties and therefore the creep model parameter is insufficient for the SRSS combination of uncertainties analysis.

However, these runs can be used to convert from creep strain uncertainty to creep model parameter uncertainty. The runs show that a [ ] variation of the creep modeling parameters lead to a shift of about [ ] strain on the average (so that the data points are shifted above or below the perfect agreement line on a 95/95 basis). The average creepdown strain of the database is [ ] and therefore, the above mentioned variation of [ ] is equivalent to a creep strain variation of [ ].

Taking into account the power/fast flux uncertainty of [ ], an additional [ ] uncertainty can be added and the total creep strain uncertainty due to creep model parameter becomes [ ]. This is insufficient to provide the [ ] SRSS combined value with the measurement uncertainty and therefore, an augmentation factor, a, is calculated, as follows:

[ ]

Therefore, the uncertainty bounds determined from the initial analysis that stacked up uncertainties, must be multiplied by 4.5, leading to the following range for the creep model parameter uncertainty:

[ ]

The proper determination of the uncertainty bounds is demonstrated by the few data points (less the required number of points for the 95%/95% bounds for the specific sample size) below and above the best-estimate line in Figures 5-4 and 5-5, respectively.

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Figure 5-1 Best-estimate RXA creep model, (predicted-measurement) vs. fast fluence

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Figure 5-2 Lower-bound RXA creep model and measurement uncertainty, (predicted-measurement) vs. fast fluence

[

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Figure 5-3 Upper-bound RXA creep model and measurement uncertainty, (predicted-measurement) vs. fast fluence

[

]

Figure 5-4 Upper-bound RXA creep model, (predicted-measurement) vs. fast fluence

[

]

Figure 5-5 Lower-bound RXA creep model, (predicted-measurement) vs. fast fluence

Question 5c

What impact does the new RXA Zr-2 creep properties have on creep collapse (ovality) for AREVA BWR fuel designs with RXA Zr-2 cladding? Provide data that demonstrates the RXA Zr-2 creep model is acceptable for application to cladding creep collapse.

Response 5c

There was one RXA cladding fuel rod benchmarked in the ovality database, which was calibrated with very good agreement with the measurement [rod 2/3 on PP 4-25 to 4-27 of BAW-10247(P) Rev. 0]. As irradiation creep overshadows thermal creep during irradiation, and because the recalibrated L_1 irradiation creep model parameter value of **[ ]** is very close to the initial value reported in the RODEX4 Theory Manual (EMF-2994(P) Rev. 0], namely **[ ]**, the calibration of the ovality model remains valid. This is because the ovality rate depends on the irradiation creep rate, and the re-calibrated irradiation creep model parameter's value is very close to the value obtained in the initial RODEX4 calibration for the RXA cladding type.

Question 6

Does the new RXA Zr-2 creep model impact fission gas release analyses, e.g., does the code need to be recalibrated against release data? If not please provide justification for why recalibration is not necessary.

Response 6

The relatively small change in creep model parameters has a negligible effect on the calculated pellet temperature, which is the major parameter affecting the fission gas release. In order to illustrate that no re-calibration is needed, some new FGR measurements on the commercial cases are used, which have been acquired after the initial RODEX4 submittal. The benchmarking of these recent additional cases shows that the code with the new RXA creep model parameters provides a conservative prediction of fission gas release, as illustrated in Figure 6-1 below. Figure 6-2 illustrates that the measured FGR data span the high and very high exposure range, which bounds the approved burnup in BAW-10247PA.

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Figure 6-1 Fission gas release benchmarking of fuel rods with RXA Zircaloy-2 cladding

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Figure 6-2 Fission gas release of fuel rods with RXA Zircaloy-2 cladding vs. exposure

Question 7

It is the intent to place limits on the application of the corrosion and creep models. What is the cladding temperature and burnup/fluence limitation for the corrosion and creep models. Provide justification based on the data that supports these limitations.

Response 7

The RXA Zircaloy-2 corrosion and creep models have been validated by RODEX4 benchmarks against the corresponding corrosion and creepdown databases, which bound the operating conditions related to cladding temperature and burnup/fast fluence, up to and beyond the approved burnup in BAW-10247PA.

The illustration of the above statement is contained in Figures 8 and 12 of Supplement 1 for the creep and corrosion models, respectively. The fast fluence on the x-axis of Figure 8 of Supplement 1 is the rod average fast fluence and the maximum value of **[ ]** corresponds to **[ ]**. The burnup span of the corrosion data in Figure 12 of Supplement 1 is **[ ]**.

Therefore, AREVA's justification that the existing rod burnup limit is applicable to the corrosion and creep models is that the benchmarks include data which bound the operating domain limits.

Question 8

Does the ridging parameter K_{hg} in Equation 6.86 in EMF-2994 impact licensing analyses? If so, please explain why this ridging parameter is not impacted with the introduction of RXA Zr-2 cladding.

Response 8

The power ramp database, which was used to benchmark the strain increment during power transients, includes both CWSR and RXA cladding types. Therefore, the calibration of the ridging parameter is valid for both cladding types.

General theoretical considerations can also be invoked in order to justify the same ridging parameter for both cladding types. Cladding ridging occurs as an effect of the hourglassing deformation of the pellet during the power increase. The pellet imposes thus an additional cladding deformation at the pellet-pellet interface, which is initially fully elastic (at the typical power ramp rates in operation). Therefore, the amount of ridging depends almost solely on pellet properties and hence the same ridging parameter is valid for both metallurgical conditions of the cladding, namely RXA and CWSR.

The ridging strain is not directly taken into account in any licensing criterion. The ridge stresses that are calculated together with the ridge strains are used in the fatigue usage factor analysis. Any change to the ridging parameter has negligible impact on temperature, strain and internal

rod pressure (which includes fission gas release). This is because the ridging model is a separate calculation at pellet-pellet interface, which only impacts the onset of the axial PCMI, which in turn only affects the cladding axial elongation.

Question 9

The following relate to the axial growth model for RX Zircaloy-2.

Response 9

In order to respond to questions 9a, 9b and 9c, an introduction is presented first, to describe the two changes made in relation to the axial growth model of RXA Zircaloy-2. The text and figures in Supplement 1 will be updated prior to the issuance of the report. The changes, noted as C1 and C2, are as follows:

C1 [ ]

C2 [ ]

The change C1 consisted of replacing the model used before in RODEX4 for RXA cladding material with the model developed for RXA material in the frame [ ], which was approved by the NRC as part of BAW-10247PA.

The RXA free-stress irradiation growth model that existed in RODEX4 had the same relationship as for the CWSR model. This is in contradiction to the known different behavior of RXA material with respect to free-stress irradiation growth, which has a saturation plateau after the initial quasi-linear increase at the beginning of irradiation, followed by an enhancement at high fast fluence. The old model was amenable to calibration but was not consistent with known phenomenology and it was found that it does not work with newly introduced liner effect on axial PCMI.

Therefore, the RXA free-stress irradiation growth model was modified and the model developed for [ ] was implemented.

The RXA material shows a growth acceleration after [ ], with an asymptotic linear variation similar to that of CWSR material. Based on in-house data, the following non-linear model was developed for RX material:

[ ] (9-2)

with:

[ ]

[

]

and

[ ]

where,

T	: absolute temperature	
$\varepsilon_{z\,gr}$	: axial free-stress growth	%
Ψ	: fast fluence (> 1 MeV)	n/cm^2

The change C2 refers to the addition of a model to reflect the [ ] for both CWSR and RXA metallurgical conditions. The verification and validation of change C2 is reported herein only for the RXA metallurgical condition.

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Figure 9-1 CWSR Zircaloy cladding axial elongation with and without liner vs. fast fluence

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Figure 9-2 Benchmarking CWSR Zircaloy cladding axial elongation with and without liner

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Figure 9-3 Update of the axial elongation benchmarking for fuel rods with RXA Zircaloy-2 cladding (equivalent of Figure 9 of Supplement 1)

Question 9a

PNNL is unable to replicate model predictions of axial strain shown in Figure 9. It appears that the value for the c coefficient may be in error, please confirm or provide a discussion of why the coefficients in the submittal are correct. Please verify that the correct model parameters are given in the submittal.

Response 9a

The axial strain displayed in Figure 9, "Net axial growth benchmarking of RX Zr-2 database," of Supplement 1 is the net axial strain of the cladding, which is the combined result of the stress-free irradiation growth and the axial deformation caused by the axial PCMI (Pellet-to-Cladding Mechanical Interaction).

Therefore, the stress-free irradiation growth is only one component of the net axial strain; it is the dominant component at the beginning of irradiation while the pellet-to-cladding gap is still open (see details in the response to Question 9b) and afterwards is overtaken by the axial PCMI permanent axial strain component at medium and at high burnup. The relative contributions of the two axial strain components are dependent on the particular power histories and a discrepancy calculation/measurement can be due to either or both strain components.

However, it was recognized that the initial stress-free irradiation growth model was not fully consistent with the known phenomenology of RXA stress-free irradiation growth, especially at medium and high fast fluences. Therefore, the change described above in the introductory part of the response to Question 9 was implemented in RODEX4.

Question 9b

It appears that the slope of the growth model versus fluence should be lower because growth is underpredicted at low fluence and overpredicted on average at high fluence. At what burnup/fluence level is hard contact experienced for these fuel designs based on; 1) RODEX4 calculations, and 2) data analysis. To illustrate the adequacy of the growth model dependence on fluence and temperature please provide plots of predicted minus measured axial strain as a function of fast fluence and cladding temperature. If there are under or overpredictions on average please explain why this is acceptable for each of the licensing analyses of rod internal pressure, fuel temperature (melting and stored energy) and cladding strain. Identify the fuel design for each set of growth data. Have additional growth data become available since the submittal to better determine the accuracy of the growth model? If so, please provide this data.

Response 9b

The model of axial PCMI in RODEX4 is based on the pellet-to-cladding contact status at [ ]. Therefore, the axial PCMI is usually triggered earlier than the radial PCMI because [ ].

The radial PCMI is established in the [ ] (E > 1 MeV) fast fluence range, which corresponds to about [ ] burnup range. This is illustrated in Figure 8 of Supplement 1. The axial PCMI is established earlier, typically between [ ] (E > 1 MeV). The cladding temperature is practically constant for BWR fuel rods and the discrepancy between calculations and measurements is described below.

However, the onset of axial PCMI is strongly dependent on the fuel densification behavior. An increased densification delays the onset of axial PCMI, while the opposite is true for reduced densification. This aspect is relevant in order to explain [ ].

[

]

Figure 9-4 (Predicted-measured) axial strain versus fast fluence

The majority of the data points in the low fast fluence range of [ ] (E > 1 MeV) came from measuring fuel rods [

].

In reality, there is an alternate axial PCMI component even when the radial gap at the pellet-pellet interface is open, namely the axial elongation of the fuel pellet stack is partially transmitted to the cladding through the plenum spring. Initially the plenum spring is rigid and absorbs little of the axial expansion of the fuel pellet stack; as irradiation progresses, the plenum spring becomes softer because of annealing stress relaxation and less of the pellet stack axial elongation is transmitted to the cladding; in addition axial PCMI sets in and overtakes the interaction through the plenum spring.

The axial interaction between fuel pellet stack and cladding through the plenum spring [ ], but it is part of the calibration of the stress-free irradiation growth model. [

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The updated benchmarking of the Zircaloy-2 RXA axial elongation database is presented in Figure 9-3, where both the calculations and the measurements are plotted versus fast fluence. Therefore, the discrepancy between calculations and measurements is clearly illustrated and further discussion is given below. Note that the growth data included in Figures 9-1 through 9-3 is an updated version of the elongation data in the Supplement. The changes are minor as two points were removed due to legal restrictions and two new points were added. The overall change is not significant.

At the high end of the fast fluence domain, a slight over-prediction of the net axial strain is observed. While the magnitude of the discrepancy between predictions and measurements is again small enough to be considered as typical prediction uncertainty, a possible explanation for the over-prediction is as follows: the fuel creep is enhanced at high burnup and the axial clad elongation induced by the trapped bottom part of the fuel pellet stack is over-estimated by the code.

Therefore, the slight over-prediction at high fast fluences has minimal effect on the rod internal free volume, as most likely the fuel column and cladding are in PCMI state, except perhaps a small portion at the bottom of the rod. The fast fluence at the design burnup is not yet in the domain of full accelerated stress-free irradiation growth. As the low and mid-burnup net growth data are in good agreement, it is more likely that the PCMI-related axial elongation is the reason

for over-prediction at high burnup; however, this does not affect the plenum volume as the cladding follows the pellet axial elongation when axial PCMI is active.

The above statement is further confirmed by the agreement of calculated free-volumes to measurements, as shown in the response to Q10.

As for the fuel temperature and cladding strain (in the circumferential direction) analyses, the slight under-prediction at low burnup and over-prediction at high burnup, respectively, have no effects because they only impact on the plenum volume. The free-stress irradiation growth does not affect cladding deformation in the transverse direction and, therefore, has no impact on the fuel-to-cladding gap. Therefore the cladding creepdown is not affected. In addition, the fuel-to-cladding heat transfer and fuel temperatures are also not affected,

Question 9c

Axial rod growth is also dependent on axial stresses on the fuel rods which is dependent on spacer spring loads (Section 4.0) and PCI. Please identify differences in spacer spring design and loads between those designs from which the data were taken and those from current fuel designs. Please identify any other axial loads on the fuel rods besides PCI. Based on the near linear dependence with fluence it appears that there is little growth due to PCI. Please discuss this further. Were these data from fuel assemblies utilizing tie rods?

Response 9c

The new growth model in combination with the **[ ]** on axial PCMI, which was described above, leads to a good overall agreement with measurements. However, some under-prediction exists at low burnup and some over-prediction at high burnup. The investigation, described in the response to the previous Question 9b, concluded that the axial interaction transmitted through the plenum spring in combination with much lower densification of the Gad fuel sub-set is the cause of the under-prediction at low burnup.

Therefore, the main effect on cladding axial elongation is due to the plenum spring and not to spacers. The friction force induced by the spacers is one of the factors influencing rod bow; however, these forces are small for ATRIUM-10 and following BWR designs, and the impact on rod growth is not significant.

Some of the rods in the axial elongation database were part of assemblies with tie rods, but this has no impact on rod axial growth. The net cladding axial growth is the combination stress-free irradiation growth and axial permanent strain due to axial PCMI. Another component could be the radial PCMI, which occurs at high burnup and which decreases the cladding axial deformation by the Poisson effect. The apparent linear trend of the net cladding axial elongation is the effect of the three contributing mechanisms mentioned above.

Question 10

The following relate to the free volume predictions with RODEX4 for RXA Zircaloy-2.

Question 10a

Please provide the free volume data (Section 7.0) (also as-fabricated volume along with how this is determined) along with burnup and fuel design from which data were taken. There appears to be a small overprediction **[ ]** *of void volume at burnup greater than* **[ ]** *(Figure 14). Is this related to the overprediction of growth at high fluences? Please provide a discussion of why this acceptable.*

Response 10a

The free volume dataset is comprised of rods irradiated in three power reactors, which are of either 9x9 or ATRIUM-10 design; also while most of the rods are full-length fuel rods (FLFRs), some are part-length fuel rods (PLFRs). The rod fill gas pressure was practically identical in all rods, at a value of **[ ]**. The initial free volume is not directly measured, unlike the fill gas pressure. The value of the initial rod free volume is calculated based on the pellet and cladding dimensional data, which allow the determination of all components of the initial free volume: pellet-to-cladding gap, pellet dishing and plenum. Because of differences in design and FLFR or PLFR rod type, the initial free volume ranged from **[ ]**. The details of the free-volume data requested by Q10a are provided in Table 10-1 below.

Figure 10-1 is an update of the predicted free volume comparison to measurements following the model updates reported in response to Q9. The burnups are indicated in Figure 14 of Supplement 1, which is repeated below as Figure 10-2.

The last part of the question refers to the group of data at high burnup, around **[ ]**, for which a slight over-prediction exists. All these rods come from the same assembly and it is considered that the most likely reason for the consistent over-prediction of these 5 rods is the uncertainty of the pellet and cladding initial dimensions. The difference between calculations and measurements for these rods, as well as for the other rods of the free volume dataset, is consistent with the uncertainty of the pellet-to-cladding gap and pellet dish volumes and is therefore acceptable.

Table 10-1 Free-volume database

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[

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Figure 10-1 Benchmarking of free volume for fuel rods with RXA Zircaloy-2 cladding

[

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Figure 10-2 Comparison of predicted and measured free volume vs. burnup

Question 10b

In addition, the free volume model appears to be based on only 19 data points. Have any additional data been collected since this submittal and if available provide these data?

Response 10b

No additional free volume data have been acquired after the RXA Supplement submittal, but it is considered that the existing data cover the whole burnup range, design and fuel rod type, as described above in response to Question 10a. Therefore, the existing free volume database is relevant and suffices for the verification purpose of this global code benchmarking exercise.

Question 11

Please provide the rod pressure limit for BWR fuel rods with RXA Zr-2 cladding. Also justify this limit based on the upper bound creep model for RXA Zr-2 and lower bound fuel swelling model.

Response 11

The approved RODEX4 methodology includes the rod pressure criterion of [ ] above the coolant pressure. This is based on demonstrating that the [ ] over pressure has significant margin to the internal rod over pressure that would cause lift-off, as characterized by pellet-to-clad opening.

A previous calculation was performed to determine the conservative value of the lift-off rod over pressure, which is based on the conservative assumptions formulated in the question, namely, upper bound creep model and lower bound fuel swelling model. This calculation was repeated for the RXA Zircaloy-2 creep model, as re-calibrated in the Supplement and updated as presented in response to Q9.

The results of the analysis, illustrated in Figure 11-1, show that the lift-off over pressure limit for RXA Zircaloy-2 is at least [ ]; this is well above the approved [ ] over pressure.

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Figure 11-1 Fuel-swelling/Cladding-creep ratio dependency to LHGR for RXA Zircaloy cladding

Question 12

1. *Please provide sample calculations for the following safety analyses using the approved RODEX-4 methodology for RXA Zircaloy-2 cladding. For each sample calculation, provide discussion on how power histories are selected and how the uncertainties are perturbed, and plots of the selected power histories. The uncertainties (values and parameter perturbed) and how they are perturbed need to be identified such that similar analyses can be performed with the FRAPCON-3.4 code with statistical analysis sampling capabilities.*
    a. *Maximum rod internal pressure*
    b. *Fuel melting calculation*
    c. *Maximum cladding hoop strain increment*

Response 12

The sample calculations requested in Question 12 have been prepared by using the latest code version, which incorporates the final multi-node calibration, the stress-free irradiation growth model change and the new [ ] model (described in response to Question 9). A recent BWR equilibrium cycle using AREVA's current ATRIUM 10XM fuel design was selected

for the sample cases. The calculations were performed by turning off and on the new [ ] model option flag. The results of the sample cases are presented in Table 12-1, below, where the results of the CWSR Zircaloy runs are included for comparison.

The calculations for these sample cases have followed the methodology fully described in BAW-10247PA. For each of the three cases identified in Table 12-1, separate analyses of UO2 and Gadolinia-bearing rods were performed for both normal operation runs with and without AOO transients (CRWE in this case). The results reported in Table 12-1 are the minimum margin values from all the analyses described above.

As agreed during a phone call, the full set of RODEX4 input files will be provided on a CD-ROM. These input files contain the power history adjusted for all power uncertainty factors included in the methodology. Also, all other input manufacturing and modeling parameters that are varied in the methodology, are identified in the input file and the nominal value for each parameter is provided. A "readme" file on the CD-ROM describes the structure of the folders and files for the sample cases provided in response to Question 12.

Table 12-1 Sample calculations for a typical BWR Equilibrium Cycle of ATRIUM 10XM

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