Table of Responses to Public Comments on Draft NUREG-2195

	PWROG Public Comment	**RES Response**	**PUT IN NUREG?**
A.1	USE OF MELCOR Over the past decade there have been ongoing discussions in the technical community regarding the ability of SCDAP/RELAP5 [1] to model the thermally induced SGTR issue. These efforts included comparisons of SCDAP/RELAP5 with various versions of MAAP. During that time, many improvements were made to SCDAP/RELAP5. It was believed that these improvements impacted the ability to correctly predict the outcome of the failure rate between the hot leg/surge line and the flawed SG tube. While MELCOR predictions are similar to that of SCDAP/RELAP5, differences between the codes can impact relative timing assessments of thermally induced failures (See Reference 1). This may impact event timings, the structural failure sequence and potentially C-SGTR failure probabilities. Thus, using MELCOR introduces unnecessary complications in interpreting TI-SGTR outcomes across the plant designs.	The choice of code to use was a major decision at the start of the project. The project participants held meetings on whether to use the SCDAP/RELAP5 code that was used for NUREG/CR-6995 or to switch to the MELCOR code. The participants weighed not only thermal hydraulic behavior of the codes but also the capability of the codes to calculate fission product release, consistency with prior Westinghouse CSGTR calculations, consistency with other severe-accident calculations, the impact of changing codes, and plans for the direction of future code usage for these purposes at the NRC. The NRC staff review did consider a cross comparison between MAAP, MELCOR, and SCDAP/RELAP performed by Vierow, *et al.*, "*Comparison of the MELCOR, MAAP4, and SCDAP/RELAP5 Severe Accident Codes for PWR Station Blackout Calculations*; presentation at the MAAP4 Information Exchange Meeting, Rockville, MD September 22, 2004." This study also found that one-dimensional TH models cannot capture the complex flow in steam generators without additional models or user-entered coefficients. Both RELAP/SCDAP and MELCOR can be adjusted to match CFD results with user-added adjustments. After weighing all these considerations the participants concluded that MELCOR was the proper code to use for this project. The impact of changing codes was a substantial concern for the project. To address this Sandia National Laboratories ran CE calculations with both the MELCOR and RELAP/SCAP codes then compared the results. The Sandia National	N

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		Laboratories report; Louie, *et al*, "*A MELCOR Model of the Calvert Cliffs Two-Loop Pressurized Water Reactor and Containment for the Steam Generator Tube Rupture Scenarios*, "Sandia National Laboratories, October 2012) which was referenced in Chapter 3 of the NUREG, contains a chapter on this comparison. No changes are being made to the NUREG in response to this question.	
A.2.1	CC RCP Leakage is 21 gpm/RCP This leakage value is not typical of CE PWRs. With controlled bleed-off (CBO) not isolated, leakage rates would be on the order of 2 gpm. If CBO is isolated, the leakage would be lower. The increased leakage results in more rapid depressurization of the RCS. (In order to estimate the potential impact of the thermal hydraulic assumptions, Calvert Cliffs SBO results using the PCTRAN code was reviewed and compared with the early pre-core uncovery predictions from MELCOR. RCS pressure differences are illustrated in Figure A-1 and Figure A-2. Both cases suggest that the 21 gpm /pump assumed RCP leak rate resulted in a high system depressurization. The impact on core uncovery was assessed for early and delayed SBO. MELCOR prediction estimated early and delayed SBO core uncovery times of about 210 minutes and 450 minutes, respectively. The corresponding PCTRAN predictions are 270 minutes and 600 minutes, i.e., the core uncovery transient appears to be accelerated in the MELCOR model.)	This is very good point about the impact of RCP seal leakage modeling. Thank you for the feedback. Since the CE analysis effort reproduced work already performed for Westinghouse in NUREG/CR-6995, to limit the resources required to perform this analysis some of the features of the Westinghouse analysis, namely the 0.5" SG-secondary leakage and the RCP seal leakage, were adopted rather than expending considerable effort in attempting to characterize these parameters. This low leakage rate was not expected to contribute significantly to depressurization so it was considered that it would be better to expend resources on other aspects that would impact results more. We reran the base stsbo case with no RCP leakage to evaluate the effect of this leakage. Indeed, as mentioned in this comment, the seal leakage did affect the depressurization and somewhat delayed uncovery, the start of heat-up, and component failure. It did not, however, substantially change the relative failure timing between SG tubes and other components in the	N

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		MELCOR screening calculations which governs whether a containment bypass occurs or not, which was the primary purpose for these calculations. Further confirmation of this conclusion using the calculator is not currently performed. Plots of the results are provided in the attached document (Attachment A). The leakage and its impact can be seen in the plotted parameters: calculated RCP water leakage and comparisons of pressures, secondary levels, structure temperatures, and MELCOR creep rupture indices of the no-RCP-leakage case to that of the base stsbo case.	
A.2.2	SIT Pressure is Estimated at Around 700 psig Calvert Cliffs Technical Specification Bases 3.5.1 indicates that SITs are to be maintained between 200 and 250 psig. The discussion of the analyses suggests that analyses were performed at 700 psig. A more realistic assessment of Calvert Cliffs SIT discharge would be closer to 200 psig. It is noted that this setpoint stipulation does not impact incipient failure timings.	The MELCOR CE plant model uses the SIT activation pressure of 214 psi. All references to accumulator discharge of 700 psi in Section 7 are only valid for the selected Westinghouse plant. The SIT discharge pressure for the selected CE plant is 200 psi minimum and 250 psi maximum. All references to 700 psi for SIT discharge for CE plants have been either corrected or deleted. As mentioned in the question the SIT activation pressure does not affect component failure timing.	Y
A.2.3	Limited Details are Provided Regarding Other Calvert Cliffs Inputs Given that the above fundamental inputs were not properly modeled for Calvert Cliffs, it would seem that more modeling details should be provided for review (perhaps another appendix) and inputs should be reviewed by the PWROG.	Information on modeling is provided in the (Louie, *et al*, *A MELCOR Model of the Calvert Cliffs Two-Loop Pressurized Water Reactor and Containment for the Steam Generator Tube Rupture Scenarios*, Sandia National Laboratories, October 2012), referenced Sandia Document. As mentioned above the 700 psi referred to a Westinghouse, not a CE, SIT.	N
A.2.4	Treatment of Hot Leg Creep Pages 42 /43 of the draft NUREG note that only one creep model could be included in the MELCOR model despite that the hot leg includes both stainless steel and carbon steel constituent layers. The stronger of the two materials was used to model creep failure. This has a significant impact on results. What appears to be	The MELCOR calculations were used primarily to predict the TH response. The creep models in MELCOR, as the creep models in SCDAP/RELAP for prior CSGTR work for NUREG/CR-6995 were simply used to identify cases for further analysis. Because the MELCOR creep modeling was only	N

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	discussed later is that these calculations were not used in the hot leg material failure model. Some reference to that discussion should be provided in Section 3.4 of the NUREG.	used to screen cases and that separate, standalone, calculations were used the TH to determine creep failure for the risk analysis, the MELCOR creep modeling was not pursued further in light of other priorities so the issue was mentioned in the report as a potential issue to address in any future work.	
A.2.5	Steam Generator Design Calvert Cliffs analysis appears to have been performed for the CE-67 BWI replacement SG. This design is typical of Millstone Unit 2 and St. Lucie Unit 1. These plants are all typical of the CE-2700 MWt design. However, other CE units have different replacement generator designs from different manufacturers along with different fuel and upper plenum designs and power levels. To what extent will these factors influence the TI-SGTR failure likelihood for the non-analyzed designs?	This issue is addressed at the end of section 3.3.3. "The results are specific to the geometry and conditions used in this study and in NUREG-1922 and are not considered to be universally applicable."	N
A.2.6	In Order to Address the Above, the Following is Recommended (1) Model changes made to SCDAP/RELAP5 in order to better simulate these transients as a result of MAAP/RELAP5 comparisons be reviewed in the context of MELCOR calculations so that the NRC can confirm that MELCOR has the necessary models such that predictions of MELCOR and SCDAP/RELAP5 would be close. A direct comparison between MELCOR and the other two codes for the SBO scenario would be helpful. (2) The MELCOR CE parameter file be reviewed by the PWROG to confirm key plant inputs relevant to C-SGTR modeling (e.g., SG tube thickness, hot leg geometry, SIT injection pressure, and RCP seal leakage assumptions, etc.).	1) See response to A.1. Differences between MELCOR and SCDAP/RELAP were addressed at the beginning of the project: Previous comparisons between the potential codes and the industry code MAAP were part of the decision-making process to use MELCOR. As one of the first tasks after generating the MELCOR CE deck Sandia National Laboratories ran the CE simulation with both MELCOR and the RELAP/SCDAP deck on which it was based to compare and quantify the differences between the codes. These differences are documented in the SNL report (Louie, *et al*, *A MELCOR Model of the Calvert Cliffs Two-Loop Pressurized Water Reactor and Containment for the Steam Generator Tube Rupture Scenarios*, Sandia National Laboratories, October 2012). MELCOR has previously been compared to both MAAP and RELAP for other Station Blackout Calculations (Vierow, *et al.*, *Comparison of the MELCOR, MAAP4, and SCDAP/RELAP5 Severe*	N

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		Accident Codes for PWR Station Blackout Calculations. Presentation at the MAAP4 Information Exchange Meeting, Rockville, MD September 22, 2004). 2) The plant model is described in (Louie, *et al*, *A MELCOR Model of the Calvert Cliffs Two-Loop Pressurized Water Reactor and Containment for the Steam Generator Tube Rupture Scenarios*, Sandia National Laboratories, October 2012). SIT injection pressure and seal leakage assumptions are listed elsewhere in these responses. The plant model is not available for public release.	
A.3	C-SGTR CALCULATOR AVAILABILITY NRC provides several methods for assessing the failure probability on a plant-specific basis. The more complex method is said to be needed for CE PWRs. As the existing results may not be generally applicable for CE PWRs (as is noted in the text), it is not clear how NRC will use these results. If NRC intends on having the utilities upgrade the LERF models with this methodology, it would be helpful to know if NRC plans to release its SG tube frequency calculator software to the industry to allow these plants to benefit from availability of this tool so that they could perform plant-specific assessments using a MAAP driving parameters, if desired.	The C-SGTR Calculator is currently not supported by the NRC. No resources are currently available to update and support the software to make it publicly available. An updated and supported version of the Calculator which will work on a current Windows-based PC may be available from the 2 vendors who worked on creation of the Calculator. NRC does not necessarily endorse this, nor has information about existence or feasibility of such a service. It should be noted that the use of the Calculator must be coupled with pre and post processing of input/output and various judgement calls on the part of the user.	N
A.4	FLAW DATA It is noted that the assessment uses limited flaw data based on in-service inspection data. Is there any intent to perform a broader review of this information? Why weren't more plants used in developing flaw distribution? Are there plans to augment the flaw distribution with data from plants that have been decommissioned/shutdown?	During the project implementation, resource and schedule constraints prevented obtaining a larger data input. This was also driven by the fact that the data available to the NRC is not presented in a uniform data format, which either renders some data less useful and not consistent with the others. NRC/RES has no plans to augment the data for the	

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		purposes of the current project. This does not exclude a potential future activity, if further interest in the NRC is generated, and prioritized.	
A.5	DETERMINISTIC STRUCTURAL EVALUATION a) NUREG-2195 would benefit from a short overview section summarizing the deterministic evaluation performed and identify how the deterministic analyses were used within the probabilistic framework. b) Have benchmark studies been performed on the finite element analyses (FEA) and computational fluid dynamics (CFD) tools used for the assessment? c) Section 4.2.1 of NUREG-2195 discusses surge line modeling. Please clarify, are stratification conditions taken into account in the surge line creep failure assessment? The section does not discuss this topic. d) Section 4.3 of NUREG-2195 discusses SG lower head model. Was a divider plate modeled in the FEA for the SG lower head? If not please provide justification. e) Weld overlay analysis in Section 4.4.6.1 of NUREG-2195 should account for the welding residual stresses of the weld overlay process. Are any residual stresses considered in the present analysis? f) Note that some of the PWR reactor vessel nozzle dissimilar metal welds Alloy 82/182 (susceptible PWSCC) have applied the Mechanical Stress Improvement Process (MSIP®1) to redistribute the welding residual stresses and reduce susceptibility to PWSCC. Would this have any impacts on the SGTR evaluation? g) Was PWSCC crack growth considered for Alloy 600/690 tubes? If not, please justify treatment.	a) PRA analysis utilized the calculator software to predict the time dependent failure probability for SG tubes and for hot legs (HL) and surge line (SL). The correlation used for estimating the failure probability for SG tubes is based on previous NRC studies which mostly performed at ANL. The calculator software also use empirical models to predict failures of HL and SL as documented in reference B-1, based on previous EPRI correlations. Section 4 documents detailed structural analyses for the Zion plant which were conducted to verify the adequacy of the RCS time dependent failure times predicted by the simplified model in the calculator software. The results showed that the simplified models consistently predicted later times to HL failure than more detailed modeling. This detailed modeling also indicated that the upper portion of the HL will fail earlier than other RCS regions. Section 5 documents the technical basis and the empirical model for predicting ligament rupture pressure, crack opening area and unstable burst pressure of steam generator tubes with flaws under severe accident transients. These same models are used in the calculator software. b) As discussed in Section 3, a benchmark study by the NRC staff, documented in NUREG 1781, “CFD Analysis of 1/7th Scale Steam Generator Inlet Plenum Mixing during a PWR Severe Accident,” demonstrates that CFD predictions can adequately predict the inlet plenum mixing	Y [response to item (a) was included in Section 4.1]

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		observed in the one-seventh scale tests. The FEA analyses uses material models and parameters based on experiments and are performed using benchmarked commercial code. However, no experiments were performed on the components under the severe accident conditions, considered in the analyses. c) Stratification of counter flow was considered in the analysis. d) Yes, it was modeled (see Fig 4-29). e) The weld residual stresses are not considered in the analysis. Such stresses will relax due to thermal and diffusion creep, as the components experiences such high temperatures. f) The compressive stresses due to MSIP on the surface of the pipe will relax under the temperatures of interest and would not have any impact under the severe accident conditions simulated in the analyses. g) This is not relevant within the time-scale of interest for the simulations considered in this section.	

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A.6	Specific Comments on Draft NUREG-2195		
A.6-1 Page 1-2, 3rd Para. Line 21	The text notes that "although SGTRs have previously been considered in risk analyses, C-SGTRs have typically not been considered" This statement is incorrect. The PWROG developed a Level 2 model guidance report (WCAP-16341-P [4]) which explicitly includes thermally induced and pressure induced SGTR failures. (See "Westinghouse Owners Group Simplified Level 2 Guidance," R.E. Schneider, J. Armstrong, PSA'05, September 11-15, 2005.) C-SGTR was based on flaw data available in NUREG/CR-1570 [3]. This approach is used in LERF assessments of many PWROG members.	This statement was mainly in reference to NRC risk analyses – primarily NUREG-1150. SGTRs were considered in 1150, but not CSGTRs. However, to reflect other efforts outside NRC the following modifications were made as a result of this comment. • Ref. 4 was added to Chapter 1. • The paragraph on page 1-2 was modified. • The remainder of the chapter 1 was reviewed for consistency. • Acronyms were checked for PWROG/EPRI, PWROG was added to the acronym.	Y
A.6-2 Table 2-1, Third Row	First column. Main steam line break or inadvertent opening of safety relief valve will tend to cool the RCS as well, resulting in lower SG tube delta pressures (closer to 1500-1700 psi).	Incorporated into Table 2-1; also note that the discussions in all sub-sections 7.4.2 have to be modified for consistency (a 2250 PSI pressure is quoted as bounding) Page 7-72.	Y
A.6-3 Table 2.1-1, fifth row	The ATWS challenge is associated with "unfavorable" moderator temperature coefficients.	UET (Unfavorable Exposure time) is defined as the time during the cycle when the reactivity feedback is not sufficient to prevent RCS pressure from exceeding 3200 psig. Many factors such as initial power level, time in cycle when transient occurs, reactivity feedback as a function of the cycle life, the number of available primary relief/safety valves, the failure/success of control rod insertion, and AFW flow rates affect UET. The noted pressure below 3200 psi is used as the bounding primary pressure value for cases when the MTC is favorable. For unfavorable MTC when the pressure exceeds 3200 psi, vessel failure and core damage is assumed and C-SGTR may be	Table 2-1 was modified and a table note (b) was added. Additional discussion is included in 7.4.1. Abbreviations for MTC and UET were added

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		considered as a part of LERF analysis. This latter pressure induced failure of C-SGTR was not included in this study.	
A.6-4 Table 2.2-1	RCP leakage for base SBO analyses set at 21 gpm per pump. This is typical of a Westinghouse plant without SHIELD®[2]. CE plants with Flowserve or similar seals and Westinghouse plants with SHIELD® mechanical seals or Flowserve pumps or CE plants have very low leakages following SBOs.	First row in Table 2-2 (p2-9) was modified and a Table note was added (table note a, and existing (a) was moved to (b)) to explain what is meant by small RCP leakages.	Y
A.6-5 General	Loss of batteries is assumed to result in loss of indication of SG level and overfill of SG resulting in failure of TDAFW. FLEX equipment is intended to operate following an extended loss of all AC power. Given the investment in implementing these backup systems, this feature should be discussed as potential alternate scenarios.	Added the following sentence to the last paragraph before Table 2-2 : *These analyses did not credit mitigation capabilities provided by FLEX and B5b backup systems, and post core damage SAMG strategies. The current results as discussed in Sections 2.3 and 2.4 are therefore somewhat conservative.*	Y
A.6-6 Section 3.4	The report discusses the modeling of the two regions hot leg as being needed for a realistic representation of the CE hot leg. While not clearly discussed. CE plant hot legs have a stainless steel clad liner and a carbon steel pipe. As only one property could be used in the model, it was determined that the most limiting of the two be considered. Thus, the assessment used the minimum creep failure for either material. It was noted that this resulted in a delayed hot leg failure time of 2.5 hours. It is likely this will have a significant impact on the CE conditional failure rates, and a determination on the impact of the C-SGTR probability should be estimated if it is anticipated these values be used in regulatory analysis.	See response to questions A.2.4 A standalone model, the CSGTR calculator, was used to evaluate hot leg and tube failure for the risk analysis. The MELCOR creep model was used during the TH calculations to screen for cases to be looked at further using the CSGTR calculator. The fact that the MELCOR creep modeling was used to screen cases for further analysis was the reason that other tasks were prioritized over pursuing this issue and why it was only noted as a topic to look into future work.	N

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A.6-7 Page3-50	Page 3-50 it is stated that "*The difference in the prediction HL failure timing was found to vary greatly simply by the assumption of material (stainless or carbon steel) – approximately 2.5 hours. Because the SG calculator and FE calculations are providing more precise estimates of component failure timing, updating the HL creep modeling within MELCOR was not prioritized over other modeling aspects that provide information not available from other sources.* *While this difference in failure timing is not directly applicable as an additional uncertainty in failure timing for this analysis it does underscore the importance of using the correct creep-rupture-related material properties. It indicates that this material property can make the difference of whether a SG tube or a HL fails first.* "Confirm that MELCOR creep estimates were not used in the failure model. Reference this discussion in Section 3.4	See response to questions A.2.4 A standalone model, the CSGTR calculator, was used to evaluate hot leg and tube failure for the risk analysis. The MELCOR creep model was used during the TH calculations to screen for cases to be looked at further using the CSGTR calculator.	Y "…failure timing, the results from the calculator software was used for the PRA analysis…
A.6-8 Section 4 Figures	The following figures do not have axis labels (Figures 4-3, 4-5, 4-6, 4-7, 4-8, 4-17, 4-25).	It all appears to be Pdf Conversion issue. Resolved.	N
A.6-9 Page 4-41	Section 4.4.6.1 1st line "pressurized" should be "primary". (Primary water stress corrosion cracking (PWSCC))	Done	Y
A.6-10 Section 5.2.1.1.1	Report notes that Argonne National Laboratory (ANL) developed a model for axial part through wall flaws. Please provide reference for the ANL contribution.	All the rupture models that we developed are related to SG tubes with cracks. See for example, NUREG/CR-6575.	N
A.6-11 Section 5.2.2.1.1	Provide reference for ANL test	See NUREG/CR-6575.	N
A.6-12 Page 5-5	Recommend that the reference literature on the data for creep rupture be expanded.	As noted in Appendix A, additional testing was conducted at Argonne National Lab through an NRC-funded effort to expand the available database of high temperature (severe accident conditions) creep properties for selected steels and weldments used in the reactor cooling system components. While more data is always better to reduce uncertainties, it is not clear if that would lead to different conclusions.	N
A.6-13	Model assumes creep failure based on the 95% L-M	Yes this would increase the failure time. But	N

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Page 5-5	creep rupture parameters. Would conclusion be changed if mean values were used.	the increase will not be significant because high temperatures involved and the rate of temperature increase is quite fast.	
A.6-14 Page 5-15	Figure 5-9(b) does not show model predictions.	The flow stress model does not change with temperature and it is fixed. The graph is fine.	N
A.6-15 Page 5-20, line 24	Typo. Inadvertent inserted 9.	Corrected	Y
A.6-16 Page 5-21	Please provide references for Idaho National Laboratory (INL) data.	NUREG/CR-6756 – "Analysis of Potential for Jet-Impingement Erosion from Leaking Steam Generator Tubes during Severe Accidents," December 2001.	N
A.6-17 Section 5.2.1.2	Provide a figure that shows the angle "β" & "θ" for the circumferential flaws.	This is Figure 5-4, a Pdf issue, are resolved.	N
A.6-18 Section 7.1 first bullet	NRC notes that MELCOR natural circulation predictions are compared to CFD models. CE plants have performed natural circulation tests. Is there interest in confirmation of predictions against test data?	(This is related to the discussion related to use of RELAP for ZNPP (Westinghouse plant) as reported in NUREG/CR-6995.) If test data are relevant to severe accident natural circulation we would be happy to receive that information.	N
A.6-19 Section 7.1.8 (line 25)	Section notes that W results should not be considered as "generic results". The statements notes that the results apply to a specific SG design, configuration, and geometry of the plant systems and specific hot leg and surge line connections. Given that statement, what is intention of Westinghouse analysis? Are TI-SGTRs to require plant-specific analyses with generator specific results?	Plant specific analysis is the preferred method for evaluating the TI-SGTR contribution to plant risk through PRA evaluations. Bounding or screening methods are not yet currently developed, therefore plant specific analysis is desirable. The word "representative" as applies to the W and CE plants were either removed or modified to "example" all throughout the report.	Y This correction was done to the whole report.
A.6-20 Page 7-55	Text in paragraph below Figure 7-22, timings used for discussion off a factor of ten. Please check values.	The text was corrected.	Y
A.6-21 Page 7-29 (and other locations)	700 psig does not apply to Calvert Cliffs.	700 psi applies to ZNPP so no changes in page 7-29. However, in Table 7-18 –removed the row associated with 700 psi, and write up in page 7-62 was modified to reflect the accumulator discharge pressure.	Y

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A.6-22 Page G-4	NRC notes low LERF contribution due to availability of 2 TDAFW pumps. Current plants also have additional SBO DGSs and new FLEX equipment.	Modifications were made in pages 7-67 (first bullet) and page 7.66 two paragraphs above section 7.2.7. Similar modification was also made at the end of Section G1.3 (page G-4) Added: however, current plants are equipped with additional SBO DGs and a set of new FLEX equipment.	Y
A.6-23 Page 7-66	PRW should be PWR	corrected	Y
A.6-24 Section 7.3.1.2	What is purpose of sensitivity? The RCS hot leg is a clearly defined parameter.	Added some clarification that this sensitivity analysis is to get insights across plants and not to address uncertainty within a plant.	Y
A.6-25 Page 7-10, Section 7.1.3	Section references 6.1.1. No such section.	Changed to 6.3.2	Y
A.6-26 Figure 7-8	Legend SG-> SG Tube	Corrected	Y
A.6-27 Section 7.1.4.5	Referenced sections at beginning of section should be 7.1.4.1-7.1.4.3	Line 6 in page 7-21 was changed to say In Sections 7.1.4.1 to 7.1.4.3,	Y

END OF PWROG PUBLIC COMMENTS

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1	Comment 1 (page xxv, 4th paragraph): "A key consideration for C-SGTR sequences is the relative timing between failure of SG tubes and failure of other locations of the reactor coolant system (RCS)." I would like to suggest an alternative key consideration for C-SGTR sequences: the relative heating rate between the SG tubes and other RCS components. The prediction of failure time of RCS components is an opaque process involving several embedded calculations, the thermal-hydraulic (TH) and heat transfer to component heat structures primarily using system codes and a correlation (usually the Larson-Miller creep rupture model) to predict failure time. The creep rupture process is a threshold and exponential process so small deviations in the problem assumptions can lead to very different calculated results. This makes direct comparisons between even very similar simulations difficult. Heating rates are physical quantities representing conservation of energy. Quantitative comparisons of heating rates between different reactor types and calculations using different code systems can provide useful insights into C-SGTR. How heating rates change as a function of reactor type, sequence timing (e.g. early/late AFW failure) and secondary side conditions (SG pressure at MSSV setpoint pressure vs. atmospheric pressure from open ADV), and other factors such as SG tube bundle and hot leg geometry can provide a more normalized figure of merit to assess C-SGTR sequences.	The relative heating rate by itself does not provide a complete picture of C-SGTR occurrence. Different structures and different materials will respond to, and fail from heat up differently. A C-SGTR description should account for both the heat up rate as well as the fracture mechanic failure mechanisms of the structures. That said; it may be possible to use the heat up rate to categorize and bin different accident sequences with respect to C-SGTR evaluation to reduce the number of fracture mechanic analyses that have to be performed. The authors think that even constant HL and SG tube heat up rates, given an intersection, pretty well approximate heat up behavior and could be used as the input for the models for RCS/SG tube failure time evaluation. In some calculations; in addition to those in the NUREG, the constant heat up rates for hot legs and the hottest and coldest tubes in the hot plume, were used to predict the temperature throughout the sequence.	N
2	Comment 2 (Page 2-6, lines 26-33): Is the success of post core damage RCS injection via	The discussion provided in this section of the report, relates to possible actions per Westinghouse Severe	Y

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	accumulator water to arrest core melt within the vessel conjecture or has this particular phenomenon been studied in detail by the NRC and industry? Later in this report, some MELCOR simulations were terminated when the accumulators started to inject. In the SAMG space for different PWR types with a faulted/isolated SG, can primary depressurization below accumulator pressure setpoints be achieved through secondary cooldown using the remaining SGs? Are secondary cooldown SAMGs "aggressive cooldown" procedures or are maximum RCS cooldown limits of 55 deg C/hr followed? How much additional time is provided to operators to align makeup water to RWST? After accumulator injection into degraded cores, how fast does the RCS re-pressurize? In CE plants, high pressure safety injection (HPSI) are medium-head pumps and cannot inject at high RCS pressures near PORV setpoints; separate charging system with low flow pumps (44 gpm/pump) is used for charging and chemical control In WH plants , HPSI and charging system use the same high-head medium flow pumps. The implementation and success of SAMGs to limit SGTR release may be plant type and sequence timing specific.	Accident Management Guidelines. RCS depressurization and recovery of injection is expected to have several beneficial effects. It eliminates the possibility that vessel rupture occurs under high pressure, it provides scrubbing of the in-vessel releases, and if is performed early enough, it could terminate further core melt. Assuming that transition from EOP to SAMG is made early in the sequence; right at or before the onset of core damage, core damage could be terminated by early injection before any significant molten debris is accumulated. The intent of the discussion was not the traditional analysis of in-vessel core retention when significant portion of the core is melted and relocated. The discussion provided here is for identifying the considerations for a detailed level 2 analyses. The value added by a detailed level 2 evaluation however could not be justified and it was not considered within the scope of this study. SAMG actions post core damage however, were qualitatively discussed for both Westinghouse and the CE plants throughout the report especially in Sections 2.1.2, 7.1, and 7.2. Depressurization below accumulator set point before core damage in Westinghouse plants via aggressive cool down was modeled using RELAP code. The accumulator set point for the representative Westinghouse plant is around 700 psi. Similar analysis however was not performed for CE plants where the	

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		accumulator (SIT) activation pressure is between 200 to 250 psi. Following TMI considerable interest arose regarding core debris coolability. Coolability within particulate beds had also been studied in relation to liquid metal fast breeder reactors (LMFBR). The MELCOR code predicts particulate debris formation upon melt contact with water. Coolability of debris beds is calculated based on a Lipinski correlation based upon calculating the conditions under which the debris bed would dry out thereby severely degrading its cooling capability. The dryout model was validated against several experiments. R. J. Lipinski, *A model for Boiling and Dryout in Particle Beds*, NUREG/CR-2646, SAND82-0765, June 1982.	
3	Comment 3 (Page 2-8) Alternative 1: what are the effective leak areas when 1 PORV or an SRV sticks open? Alternative 4: what is the effective leak area of the stuck open SG PORV?	The setpoint pressure and the PORV size are specific to plants. For example, for Calvert Cliffs; the representative CE plant, the PORVs is set at 2400 psia and the relieving capacity is 153,000 lb/hour. The pressure set points for the two SRVs are at 2500 and 2565 psi respectively. TH cases for scenarios where primary relief valves stick were not evaluated for the purpose of containment bypass since these scenarios already effectively involve a break to containment.	N

	FYNAN Public Comment	RES Response	PUT IN NUREG?
		Flow areas in MELCOR were taken from the RELAP deck and the FSAR, Rev 34.. Maximum opening areas are provided below. The actual opening area is set by control logic. PORV: Max opening of 2 valves at 6.85E-4 m^2/valve for 1.37E-3 m^2 (0.014747 ft^2) SRV: Max area SRV: Max opening of 0.0025013 m^2 (0.026925 ft^2) (2 valves) SGPORV: Max area of 0.008107 m2 (0.08726 ft^2) A few different options were used for the SG PORV/SRVs: 1) Full SGPORV and SGSRV opening MSSVs: .total area 0.03648 m^2 (0.3927 ft^2) A few different options were used for the sticking of MSSVs: 1) Sticking upon MSSV opening. For the scenarios considered the MSSVs did not fully open and therefore did not stick open. 2) Opens to extent decided on by model but doesn't	

	FYNAN Public Comment	RES Response	PUT IN NUREG?
		close. No single value exists for the opening area. The opening area depends on scenario. 3) Immediate opening upon reactor scram. (Operator action or common-cause failure) Full opening area.	
4	Comment 4 (Page 2-10, lines 1-6): The free volumes of SG secondary sides are 0(10^2) m^3. SG secondary side pressures can range from approximately 8 MPa, MSSV setpoint pressures, to near atmospheric pressure, especially if an operator has purposely (following an BOP or SAMG) opened an ADV to depressurize the SG during a cooldown operation or depressurization operation to allow water injection to the SG at low pressure using low-head pumps, and steam densities are 0(10) kg/m^3 to 0(10-^{1} kg/m^3•› When the SG is "dry", there is still a residual mass of superheated steam in the steam generator ranging from 0(10^3› kg to 0(10) kg acting as a large heat sink along with the mass of the Inconel›› SG tubes. The thermal mass of the heat sink is a function of the SG pressure and the countercurrent natural circulation flows on the primary side of the SG tubes are coupled by the heat transfer to these heat sinks. By assuming 0.5 in^2 secondary side leakage area, the SGs slowly approach atmospheric pressure after SG dryout and the natural circulation flow rates and cooling of	SCDAP/RELAP5 modeling as described in NUREG/CR-6995 details various insights and nuances in modeling results. The secondary side leakage for Westinghouse plants was considered to be fixed at 0.5 cm^2; however different rate of depressurizations was considered including aggressive cooling prior to onset of core damage. Intentional depressurization after the onset of core damage as a part of SAMG actions was also qualitatively considered as a part of PRA discussion. Detailed modeling of SAMG mitigation strategies were not considered within the scope of this study.	N

	FYNAN Public Comment	RES Response	PUT IN NUREG?
	primary side steam as it flows through the SG tubes are governed by this slow SG depressurization. The heating rate of the SG tubes may also be coupled to the SG secondary side depressurization. A primary side flow rate of 0(10) kg/s being cooled 100 deg C can raise the temperature of secondary side steam mass of $0(10^3)$ kg by approximately 1 deg C/s. These are some of the subtleties that should be considered when comparing the base cases with other sequences with different secondary side depressurization assumptions.		
5	Comment 5 (Page 2-12, line 3): What is the technical basis for assuming 21 gpm per pump RCP seal leakage? Are these plant specific/installed pump seal package values or just assumptions? What is the effective leak path area modeled to obtain 21 gpm at operating RCS pressures? For all analyzed sequences in Chapters 3 and 7, it would be helpful to see a plot of the integrated mass flow through the pump seal leaks as a function of time compared to integrated mass flows through the pressurizer PORVs and SRVs. For the past 25 years, much time and effort has been spent in the severe accident simulation community agonizing over pump seal leakage modeling and many NUREG and topical reports have sections or chapters dedicated to the topic. However, it remains unclear whether the small mass and enthalpy flow rates through modeled nominal RCP seal leaks actually matter to any of the severe accident phenomena of interest in these studies.	The 21 gpm pump seal leakage is typical of a Westinghouse plant without SHIELD®[2]. CE plants with Flowserve or similar seals and Westinghouse plants with SHIELD® mechanical seals or Flowserve pumps or CE plants have very low leakages following SBOs (generally less than 2 gpm). The 21 gpm nominal seal leakage per RCP was assumed for the CE plant just for consistency with the Westinghouse plant. Additional MELCOR run was performed assuming no RCP leakage for the CE plant. The analysis indicated that even the small amount of seal leakage will impact the rate of early depressurization, the heat up rate will shift in time, but the conclusions regarding C-SGTR probability is not expected to be significantly changed. In response to another comment a simulation was rerun assuming no RCP seal leakage.	Y

	FYNAN Public Comment	RES Response	PUT IN NUREG?
		See response to question A.2.1. A plot of integrated mass flow through RCP seal leak for the stsbo case is provided in the attached document (Attachment A).	
6	Comment 6 (Page 2-15, line 29-41): Is the discussion in this paragraph conjecture or has technical work or simulations been performed to support these statements? To clarify, this paragraph appears to be discussing the blow down of the high pressure RCS through SG tube breaks to the low pressure SG secondary side. The collapse of the countercurrent flow regime in the RCS primary side would be analogous to the temporary breakdown of the countercurrent flow when the pressurizer PORV or SRVs cycle. Is the conclusion of this paragraph that guillotine break of three tubes causes the blow down of the RCS that disrupts the countercurrent natural circulation flow? In lines 31-33, would such a blowdown event depressurize the RCS such that the accumulators inject possibly arresting core melt?	The discussion referred to by this comment was based on simplified analysis and it was originally intended for detailed Level 2 PRA evaluation. It is nor more pertinent to this study and it is therefore removed. The report was modified to reflect that. Regarding the primary depressurization as a result of the guillotine break of one tube, we do not expect that such a blow down event depressurize the RCS such that it results in accumulator injection. Section 7.1.6 of the report discusses that such a small leakage will not depressurize the primary such that accumulator/SIT injects unless additional tubes are failed.	Y 1. Text was deleted. 2. Ref.4 was removed and other ref. numbers were changed, 3. Noted there are no references made in the text for Ref. 5 and 6. They were removed.
7	Comment 7 (Section 3.1.2, Page 3-7, Lines 11-50): Additional	1. As noted at the end of section 3.3.3, “The results	N

	FYNAN Public Comment	RES Response	PUT IN NUREG?
	CE Plant Considerations All CE plants in the USA are/were operating with replacement SGs from a variety of manufactures including Westinghouse/ENSA (AN0-2 and Waterford-3), Framatome (St. Lucie- 2), BWI (Calvert Cliffs, St. Lucie-1, Millstone-2), ABB/CE/Ansaldo (Palo Verde), .MHI (SONGS and Fort Calhoun), and CE (Palisades). Only Palo Verde and Palisades replacement SGs retained the original "square" bend U-tube design of CE SGs. The OPRIOOO and APR1400 series reactors in Korea and UAE based off of CE System 80 design also feature square bend U- tube SGs manufactured by Doosan Heavy Industries. All other replacement SGs have semi- hemispherical tube bundle bend regions. The second major difference is tube diameter. CE steam generators are 3/4 inch outer diameter tubes with the exception of the delta 109 RSG for AN0-2 which has 11/16 inch diameter tubes. The Westinghouse AP IOOO, a 2x4 plant, has delta 125 SGs with 11/16 inch diameter tubes manufactured by Doosan. The Zion NPP WH model 51B SGs are 7/8 inch outer diameter tubes and with the power rating of Zion of only 3250 MWt, one of the lowest power rated 4-loop WH PWRs, the tube bundle heights are short. In contrast, the delta 94 replacement SGs for South Texas Project, the highest power rated 4-loop PWRS are 3853 MWt, are significantly taller and 11116 inch diameter tubes. Tube diameter and tube bundle height are important factors controlling the natural circulation flow rates. Inlet plenum geometry is of second order importance.	are specific to the geometry and conditions used in this study and in NUREG-1922 and are not considered to be universally applicable." 2. Regardless of small differences in the tube diameter and bundle height, which do clearly affect the natural circulation flow rates, the inlet plenum geometry affects the mixing which directly affects the temperatures entering the tube bundle. These temperatures are of first order importance when considering induced tube failures.	
8	Comment 8 (Section 3. 3 Computational Fluid Dynamics,	a. See reference 7 in section 3. This reference	Reference

	FYNAN Public Comment	RES Response	PUT IN NUREG?
	pages 3-8 to 3-12): a) The recirculation ratio and hot tube fraction values for CE SG reported in Section 3.3.3 differ from previous reported values in NUREG-1788 by almost a factor of 2. A standalone report should be published clearly detailing the CFD model evolution that produced such a discrepancy. The scientific method requires reproducibility. All boundary conditions applied to the CFD models should be documented because numerical results of CFD simulations are highly dependent on the boundary conditions. b) Page 3-11, lines 34-36, what are the target pressure drops and heat transfer rates of the prototypical steam generator that the CFD models are calibrated to and how were they originally determined? c) How does the current use of CFD differ from the earlier use of the COMMIX finite element code (Domanus and Sha, NUREG/CR-5070, 1988) in calibration of system code models related to the TI-SGTR analysis?	discusses the updated modeling approach. Previous modeling efforts on CE type geometries were very limited in scope. This work incorporates the lessons learned in NUREG 1922 to the work done in NUREG-1788. b. Target pressure drops are based upon detailed CFD modeling of prototypical SG tubes with no surface roughness. Heat transfer rates are inferred from system code predictions of temperature along the tube flow path. No detailed three –dimensional modeling on the external side of the bundle is completed. Reference 7 includes a discussion of the tube bundle model development and the calibration of the model to predict the pressure drop and heat transfer rates. c. The COMMIX code predictions used an inadequate mesh to adequately resolve the flow patterns and mixing of interest. These latest modeling efforts benefit from the significant developments in modeling n computer capabilities and result in a much more detailed simulation.	7 added.
9	Comment 9 (Page 3-13, lines 29-35): The SG tubesheet is a huge thermal mass and offers several feet of heat transfer length per tube. A CFD or finite element model study using controlled boundary conditions would be useful to investigate how the tubesheet affects the natural circulation flow. In particular, estimate the enhanced heat	The impact of the tube sheet mass is considered but is not modeled in full detail. This is considered to be a small effect in the overall evaluation of induced SG tube failure risk. Heat transfer to the tubesheet and the tubesheet's	

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	transfer due to *vena contracta* and possible larger temperature differential from primary steam to the large steel volume.	thermal mass was modeled in the MELCOR analysis.	
10	Comment 10 (Page 3-13, lines 37-45): See comment 1 about heating rates. Here relative heating rates would be an excellent figure of merit to measure the effects of various heat transfer coefficients on the severe accident progression.	Relative hot leg and tube heating rates were evaluated for a separate calculation for a few of the cases. The heating rates were evaluated considering the time and temperature at the beginning and end (before component failure) of the near-linear temperature rise. Average heating rates for the tubes are provided in the response to question 19.	
11	Comment 11 (Page 3-14, lines 13-26): Secondary-side relief-valve fail open modeling is a red herring. Lines 24-26 correctly identify that there are procedures, both in EOP space and SAMG space that tell operators to intentionally open secondary side relief valves. For CE plants during SBO, this is likely the ADV that must be manually opened at the valve location using the handwheel, but this is sometimes difficult (see USNRC Information Notice No. 89-38, Atmospheric Dump Valve Failures at Palo Verde Units 1, 2, and 3). An alternative strategy that might be implemented from the control room is opening the MSIV bypass valve, 4 inch air operated and solenoid controlled valve, and the air operated turbine bypass valves to allow secondary depressurization to the condenser.	 The possibility of secondary-side valve leakage was extensively discussed during the Steam Generator Action Plan (SGAP) and the generation of NUREG/CR-6995. For fission products in the secondary side to reach the environment some pathway must exist. It was considered during that analysis that secondary side valves could fail open. Characterizing secondary valve failure was not within the scope of this CSGTR project since the resources were not available to do so. Secondary valve failure was rather part of the problem definition. The RELAP/SCDAP models used for NUREG-6995 did not address secondary-side valve failure explicitly since the analysis did not involve the calculation of fission-product (aerosol) transport. Similarly the RELAP/SCDAP CE deck from which the MELCOR deck	N

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		was based also did not address secondary side leakage. When it came time to produce the initial results, in order to calculate fission product release to the environment some decision had to rapidly be made on how to model this secondary-side valve failure. To be consistent with other MELCOR analyses and previous assumptions these calculations involved modeling of secondary-side relief valve sticking. When the initial modeling (valves don't stick until fully opening) did not result in releases (valves didn't fully open) the failure modeling was further altered to evaluate the possible failure criteria that would possibly result in fission product releases to the environment (valves stick open as far as they have opened). ACRS members pointed out that this was inconsistent with the CE EOPs during a review. The simulation was then rerun assuming immediate opening of secondary relief valves. The TH outputs for both cases were made available for use in the risk analysis. One of the interesting things about the valve failure calculation is the heatup and releases were delayed by not depressurizing initially. This suggests that not depressurizing the SGs may be the better course of action if a source of water has not been secured	

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		because it would provide more time for evacuation or securing water before releases.	
12	Comment 12 (Page 3-15, lines 37-41): Caution should be employed when performing cross code comparisons and excessive tuning of models to attain agreement with a different model that may be using different boundary conditions, assumptions or solution structures should be avoided. For example, what are the natural circulation lengths input on cards 801 and 901 for the U-tube heat structures of the SCDAP/RELAP5 model? Compare to the characteristic length on cards 500 and 700 for the U- tube heat structures of the MELCOR model Are the MELCOR lengths equal to the hydraulic diameter of the U-tube (~2 cm) and are the RELAP5 heights equal to the heat structure height (~1 m), a function of the user selected nodalization). The natural convection heat transfer coefficients that are calculated by RELAP5 and MELCOR (using different correlations) are directly proportional to these lengths. A major difference between the RELAP5 and MELCOR models might be that different heat transfer rates are calculated which determine natural circulation flow rates. Furthermore, what heat transfer coefficients were applied as user defined boundary conditions on the external U-tube wall in the new CFD calculations?	It is agreed that caution should be employed in cross-code comparisons and the excessive tuning should be avoided. The important issue to realize for this problem is that the one-dimensional flow equations cannot capture the overall behavior of complex flow in steam generators without additional models or user-entered coefficients to match experimental data or 3D CFD results. Additions must be made to RELAP/SCDAP and MELCOR to match the CFD results as designated by a few parameters. The parameters and process to extract them from the CFD results are described in NUREG 1922. The paragraph in question refers to a check to verify that the CFD parameters were matched. The parameters had a sufficient discrepancy that they needed to be changed. Some of the parameters requested have limited impact on behavior given the method for calculation of recirculation. On the external side of the SG tubes in the CFD model, a convective boundary condition is used. With	N

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		"freestream" secondary side temperature set using severe accident code predictions. The heat transfer coefficient is adjusted to ensure tube temperature profiles are consistent with the severe accident code behavior. See reference 7 in section 3.	
13	Comment 13 (Page 3-15, lines 43-49): Is there a reference that can be cited detailing the new method? Is this paragraph referring to the opposed pump control function models that are applied to the split hot leg flow paths? What are the actual deltaP's of the pumps in Pascals calculated by two methods for a representative severe accident natural circulation? What are the calculated pressure drops through hot leg volumes using active control? If active control is not used and the split hot legs are just modeled as pipes with conventional wall friction loss, what are the calculated pressure drops through the hot leg volumes? The delta P's of the active control pumps are probably on the order of 5 - 10 Pa whereas the pressure drop due to friction in the U-tubes are $0(10^3)$ Pa and changes in gravitational head from density change of steam and U-tube height are $0(10^3$ Pa.	"Active control" does indeed refer to the split hot leg pump models. The initial model had a control system to match the CFD-generated parameters. The stability issues mentioned in the paragraph were in the control system for this pump. The following two aspects were changed in the model: 1) A single pump was used in the upper hot leg in the initial model. An additional pump was added in the lower hot leg such that (directional) dPlower = - dPupper 2) Instead of using a control system to activate the pump and match a specific velocity when natural circulation conditions were detected, the pump differential pressure was based on the velocities of the upper and lower hot legs in the manner similar to wall drag to take the form: dP = k \rho_{average} (v_upper – v_lower)^2 with k determined iteratively. In this way the pump represents the shear force between the upper and lower hot leg flows. With this information along with the velocity and density	N

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		one can approximate the pressure drop. The velocity for each segment was approximately ½ m/s for a differential velocity, and differential velocity squared, of about 1. The density varied with increasing temperature but was in the range of 50 kg/m^3. So the differential pressures resulting from the countercurrent flows were approximately in the range of: $dP \sim 1.875 \times 50\ [kg\ m^{-3}] \times (1)\ [m^2\ s^{-2}] \sim 94\ Pa$	
14	Comment 14 (Page 3-19, lines 34-35): Please quantify the statement "...were found to be higher than those of Fluent". Why do higher (and quantify how much higher) hot-leg velocities prefer tube over HL failure? Do heat transfer coefficients and residence time change as a function of velocity? See comment 1: the result of different heat transfer coefficients and residence time can be observed in the heatup rates.	It was a significant fraction- some tens of percent. The specific amount was never quantified because it represented an intermediate stage in the deck development process. Note that this relates to the modeling described in your questions 12 and 13. As mentioned in those responses user-added models set the HL velocities. The fact that the velocity was off just indicates that the user-added model wasn't calibrated and does not reflect something inherent to the MELCOR code. No specific higher velocity threshold that prefers tube failure over HL is expected. The expectation that higher	N

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		velocities favor SG failure results from the following general reasoning: the temperature drop over the HL is less at higher velocities resulting in a slightly higher temperature of gas entering the tubes. Since not much cooling occurs this effect is not expected to be large. Both heat transfer coefficients and residence time do indeed depend on velocity.	
15	Comment 15 (Page 3-20, line 32): This behavior *is* scenario dependent. Most of the cases presented later in Ch. 3 assume the 0. 5 in^2 secondary leakage which also controls secondary depressurization after TI-SGTR and RCS blowdown to the secondary side. See Figure 3-5 on page 3-26 from 23000 seconds to 26000 seconds. If the initial depressurization is modeled as an opened ADV, the release to the environment will be much greater.	Yes, fission product releases would differ for open relief valve scenarios. The statement in the document referred to whether or not releases would occur in different closed-secondary-relief-valve scenarios.	N
16	Comment 16 (Page 3-21, line 26): What are the RCS depressurization mechanisms in the sequences that experienced reflood via accumulator injection? Larger RCP seal leakage due to blowout of seal internals? Blowdown through the TI-SGTR break?	RCS depressurization resulted from either steam generator tube failure or hot leg failure. RCP seal failure was not modeled in MELCOR.	N
17	Comment 17 (Pages 3-26 to 3-40, comments on figures) A figure showing together the decay power and power from metal-water reactions calculated by the COR package as a function of time would be helpful. During the onset of core damage, there can be time periods when the power from metal-water reaction exceeds, by over a factor of 2, the decay power.	Plots of the decay and oxidation energies and powers and energies are provided in the attached document (Attachment A). A possible explanation for the steam generator secondary split is that some fraction of the hot gases that enter loop A were lost through the pressurizer relief	N

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	Fig. 3-5: at 16000 s, what is causing the bifurcation in the SG A and B pressures? Are there two different valves modeled on the pressurizer and what are the setpoints and open areas? It appears the SG pressure bifurcation is correlated to the jump in RCS pressure. See comment 5: here seeing the mass and enthalpy flow out the pressurizer would be helpful. Fig. 3-7: Reproduce a Fig. 3-7b showing the detail of the heatup rates leading to first component failure. Limit x-axis from 12000 s to 24000 s and y-axis from 600 K to 1100 K. Fig. 3-8: Loop B (without the pressurizer) is being predicted to fail first. Loop B tube heatup rate is greater than Loop A. Some previous TI-SGTR studies concluded that the pressurizer loop heats up faster. Very interesting result and should be investigated more. Fig. 3-10: Appears to be significant movement of hydrogen to the U-tube bundle after the TI- SGTR and RCS begins to blowdown to faulted SG. Was this hydrogen produced earlier and was residing in a different location (what location) or does the RCS blowdown initiate additional water metal reactions? Fig. 3-11: Make a cut-out of the I and Cs release and rescale to show detail and place in the blank space to the left of the Te curve. Fig.3-14: Repeat the rescaling that was done for Fig. 3-7b. Figs. 3-17 and 3.18: a Table summarizing the heatup rates from SG dryout to first RCS component failure for the two sequences would be a more informative way to present the very important data contained in the figures.	valves whereas for loop B these hot gases went to the steam generator and kept the B secondary side hotter and at a higher pressure than the A secondary side. Relief valve setpoints are discussed in the response to question 3 above. The loop *without* the pressurizer behaved worse (heated up quicker) in the CE analyses. This was not the case in the W analyses. MELCOR predicted liquid water to be held up in the pressurizer throughout the sequence. Although the inventory of the liquid water decreased some water remained. Water flowing from the pressurizer to the hot leg can cool the gases in that hot leg, before it goes to the steam generator. Many figures and figure updates were requested. Although some of these were provided it was not practical to extract all the requested items. There may be limited utility of scrutinizing fission product releases further. Although the capability to calculate fission product release and transport was a major factor in the choice of MELCOR and the evaluation of fission product releases to the environment was part of the original plan for the project, evaluating these releases was eliminated in a reduction of scope for the project. Because of this simulations that ended prematurely but	

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	Fig. 3-21: rescale, See Fig. 3-I4b. Figs 3-22 and 3-23: Make cut-outs from 60000 s to 70000 s and rescale to show detail and place in the large blank space to the left of the curves. Figs. 3-27 and 3-29: rescale figure.	provided sufficient TH information for the risk analysis were not rerun to evaluate releases further. Nevertheless it was considered useful to provide the releases that were calculated and extracted. These releases should be considered as approximate values up to the time of termination for the scenarios considered. Because fission-product releases were not a primary focus the plots were provided as-is and were not rescaled. A plot of hydrogen generation for the stsbo case is provided in the attached document. For Figure 3-11 the FP release to the environment occurred in a single opening of the relief valve. The final environmental release fractions are provided in Table 3-2 Figs 3.27 and 3.29: the bypass environmental release fraction is 0 since secondary side relief valves did not open and were not assumed to leak. The plot would therefore be the same on any scale.	
18	Comment 18: (Page 3-41, line 46) The rise is not linear. At approximately just before 15000 s, although it is hard to see on the current scale, there appears to be a discontinuity in the first derivative (heatup rate) of the temperature curves. Note that this is about the same time as the pressure bifurcation in Fig. 3-5.	The heatup was described as nearly linear because, for most of the temperature rise, it can be well approximated using a constant heat up rate for most of the heatup, especially after the rate change.	N
19	Comment 19: (Page 3-41, line 48)	It can be seen that the heatup rates are similar in figure	

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	Quantify the statement "a little slower". This is a very significant result. Although the heatup of RCS components is occurring at very different absolute times (over 6 hours difference) the *heatup rates* are approximately the same.	3-18. The stsbo and ltsbo sequences seem to be very similar – just time shifted. The average temperature rise for the hottest tube for stsbo-a was 0.58 K/s with a faster temperature rise of .078 K/s over approximately 2000 s and 0.052 K/s afterwards. For the ltsbo-a the average temperature rise for the hottest tube was 0.049 K/s. The faster rate was not as significant for the ltsbo case: ~0.55 K/s before and 0.48 K/s after the inflection. Note that these changed fluctuate somewhat with time and that the calculated rates depend somewhat on the bounds chosen for their evaluation.	
20	Comment 20: (Page 3-42, line 40) See comment 17: show power from water metal reactions as function of time.	Plots of the decay and oxidation energies and powers and energies are provided in the attached document (Attachment A).	
21	Comment 21: (Section 3.7 Potential Future Analyses) This section may be the most important section of the NUREG because it identifies limitations and subtleties of the current work and recommends specific technical items to address in the future. To resolve the loop seal clearing problem, please consider developing a SBO MELCOR model for the WH AP 1000 that will serve as a surrogate model for once through natural circulations resulting from loop seal clearing for other PWRs. The AP 1000 RCPs draw suction directly from the SG outlet plenums so there are no cold leg suction legs where the loop	Loop seal clearing was looked into in detail for Westinghouse plants in NUREG/CR-6995. For Westinghouse plants loop seal clearing results in substantially hotter gases reaching the tubes. This is characterized by a peak normalized tube gas temperature $T^*=(T-T_c)/(T_h-T_c)$ of approximately 0.45 under closed loop natural circulation conditions reaching over 0.9 upon loop seal clearing. For the CE plant analyzed the impact of loop seal clearing is not as significant since the peak normalized	N

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	seals form. Secondly, the startup feedwater system doubles as the non-safety grade AFW system using AC powered pumps. During SBO, there appears to be no available AFW for the AP 1000. At the 10 SBO events at commercial NPPs, the SBO was not the initiating event and the NPPs were in various stages of cooldown procedures (Fukushima Diachii 4 of 6 units, Fukushima Diani 3 of 4 units), hot standby (Maanshan Unit 1), and refueling outages (Vogtle 1 and Kori 1). A key component of the AP 1000 passive safety system is the 4th stage of the automatic depressurization system (ADS) employing the large squib valves that if spuriously activated will cost the plant millions of dollars. Are there operation procedures during low power or shutdown that isolate or lock out the ADS?	temperature, which gives an indication of the extent of mixing of gases prior to reaching the SGs and thus of the relative heatup of SGs and hot legs, is already greater than 0.9 under closed-loop-seal natural circulation conditions. This is to say that little mixing of the hot plume was observed for the CE configuration even under natural circulation conditions. Loop seal clearing cannot decrease mixing or increase the normalized temperature much since it hasn't cooled off in the first place. Because of the substantially lower impact of loop seal clearing for the CE configuration analyzed it was decided to expend resources elsewhere in this project. The seal clearing in AP 1000 was not within the scope of this study.	
22	Comment 22 (Section 3.8 Conclusions) Page 3-49, lines 33 - 37: The TH phenomena are coupled to the SG secondary side conditions and valve modeling. Excellent insight. Page 3-50, lines 4-7: Excellent insight. See comments 4, 11, and 14.	Thank you for the comment.	
23	Comment 23: (Page 4-1, line 25) How typical is the Zion NPP? Of the 30 operating WH 4-loop PWRs in the USA, Indian Point are the only units with a lower power rating than Zion. The average rated power of WH 4-loop plants are 8.3% higher than what is assumed in the Zion analyses. South Texas Project reactors are almost	As noted in several places in the report, this study focused on two specific plants (One CE and one W). The study did not attempt to evaluate the impact of the various design and operational parameters across plants.	**Y**

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	19% higher rated power.	The word “representative” as applies to the W and CE plants were either removed or modified to “example” all throughout the report.	
24	Comment 24: (Page 7-23, Lines 27-44) What happens when you put cold water into a very hot dry SG? How might radionuclide transport and release be affected when this is implemented in EOP space (before significant core damage), in SAMG space after core damage but during RCS component heatup stage before TI- SGTR, in SAMG space after TI-SGTR has occurred but before other RCS component failure?	This is discussed qualitatively in page 7-23. No attempt was made for quantification or detailed modeling within this study.	N
25	Comment 25: (Page 7-35, Line 5) Pressure drop in SG tubes as a function of tube diameter and tube bundle height JS very important.	Agree, The report has tried to identify this issue and warn the reader to not extrapolate the insights to other plants (See Sections 8.1 and 8.2 for example).	N
26	Comment 26: (Page 7-36, Lines 16-18) How might these measures affect radionuclide transport during SAMG space before and after TI- SGTR? In particular, portable low-flow high-head pumps are available at plants for RCS injection (FLEX pumps at Palo Verde and Kerr pump at Surry). What happens when low flow rates of cold water are applied to partially degraded cores at high pressure?	Evaluating the effectiveness of SAMG and FLEX strategies are not considered within the scope of this report.	N

END OF FYNAN PUBLIC COMMENTS

Attachment A

Calvert Cliffs CSGTR Supplemental Plots for Responses to Public Comments

1. Comparison of stsbo-noRCPleak pressures to those of stsbo
2. Comparison of stsbo-noRCPleak SG boiler collapsed liquid levels to those of stsbo
3. Comparison of stsbo-noRCPleak Loop A structure temperatures to those of stsbo
4. Comparison of stsbo-noRCPleak Loop B structure temperatures to those of stsbo
5. Comparison of stsbo-noRCPleak tubesheet structure temperatures to those of stsbo
6. Comparison of stsbo-noRCPleak creep rupture indices to those of stsbo
7. Total hydrogen generation for the stsbo calculation
8. RCP seal leakage for the stsbo calculation
9. stsbo decay and oxidation power contribution
10. stsbo decay and oxidation energy addition
11. Comparison of upper hot leg B velocities for different hot-leg natural circulation models

1 Impact of RCP Leakage Comparison Plots

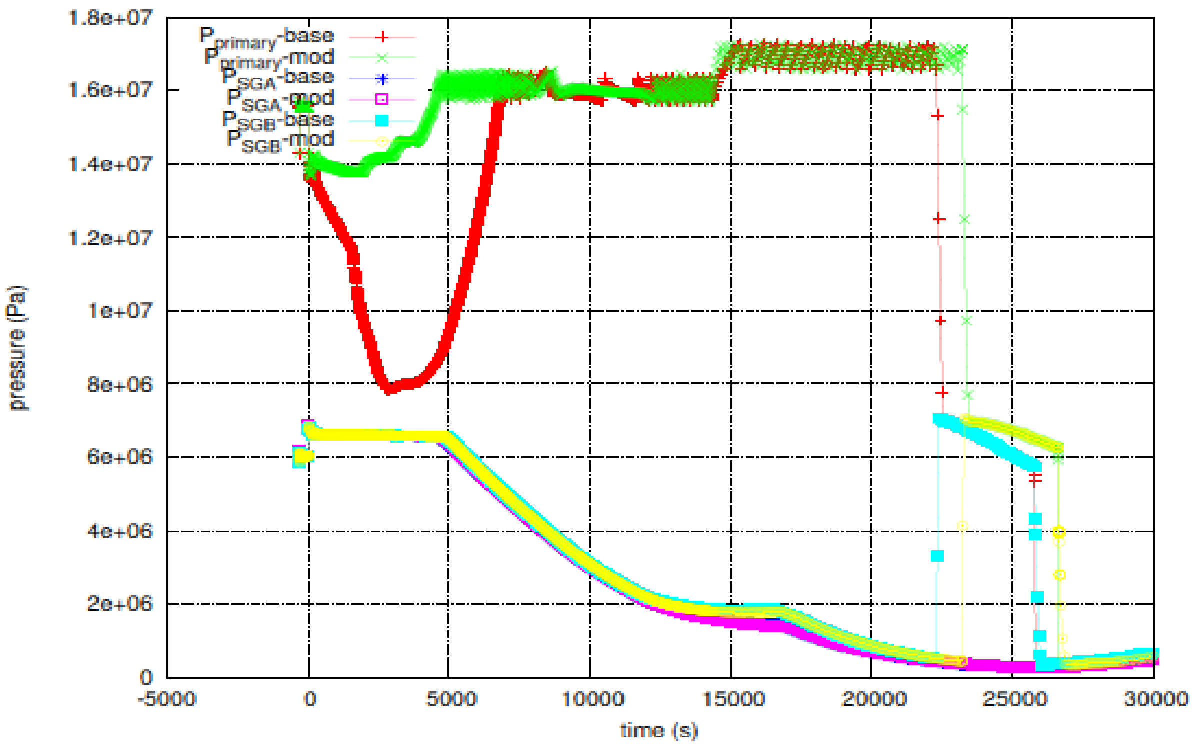

Fig. 1: comparison of stsbo-noRCPleak pressures to those of stsbo

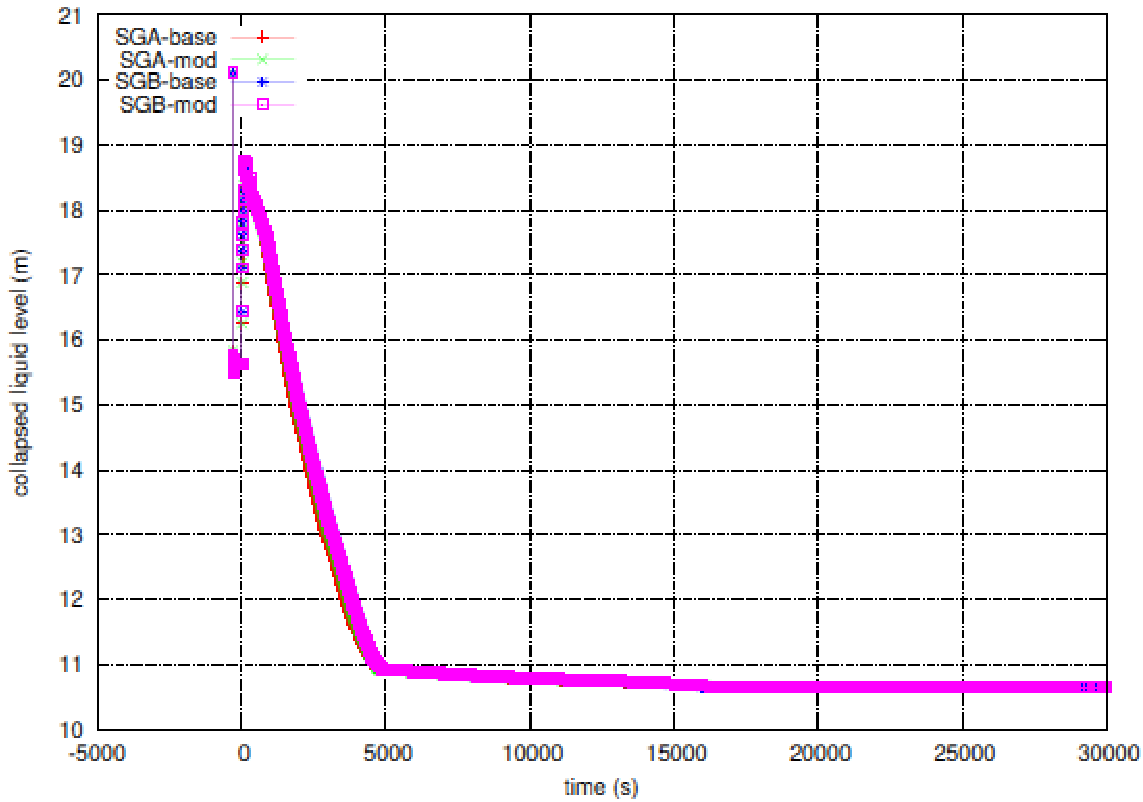

Fig. 2: comparison of stsbo-noRCPleak SG boiler collapsed liquid levels to those of stsbo

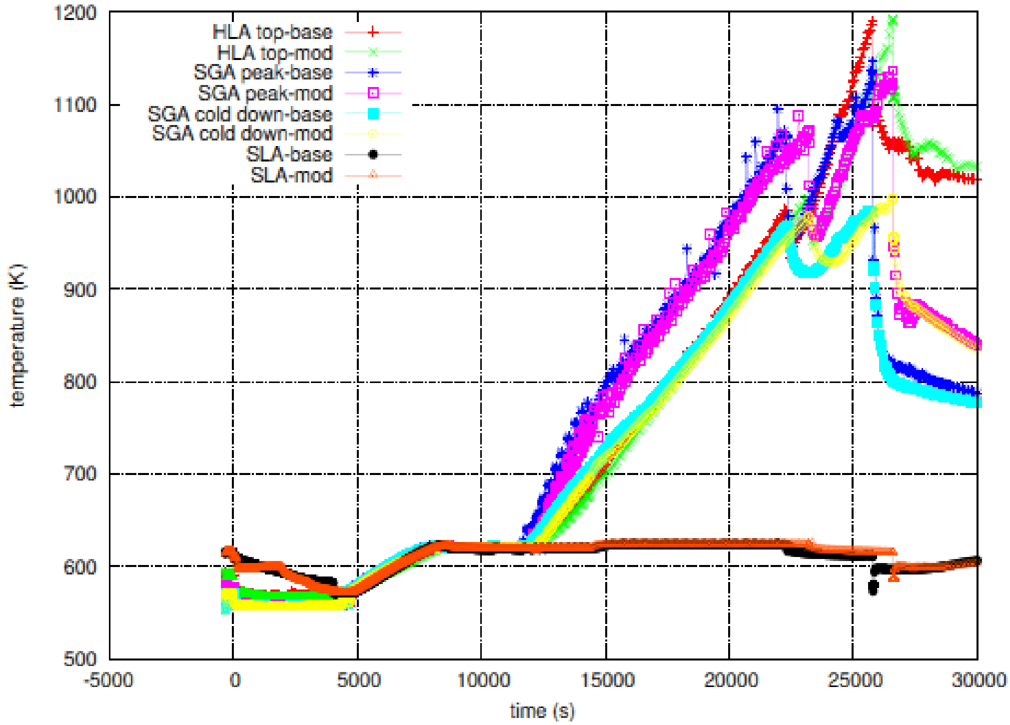

Fig. 3: comparison of stsbo-noRCPleak Loop A structure temperatures to those of stsbo

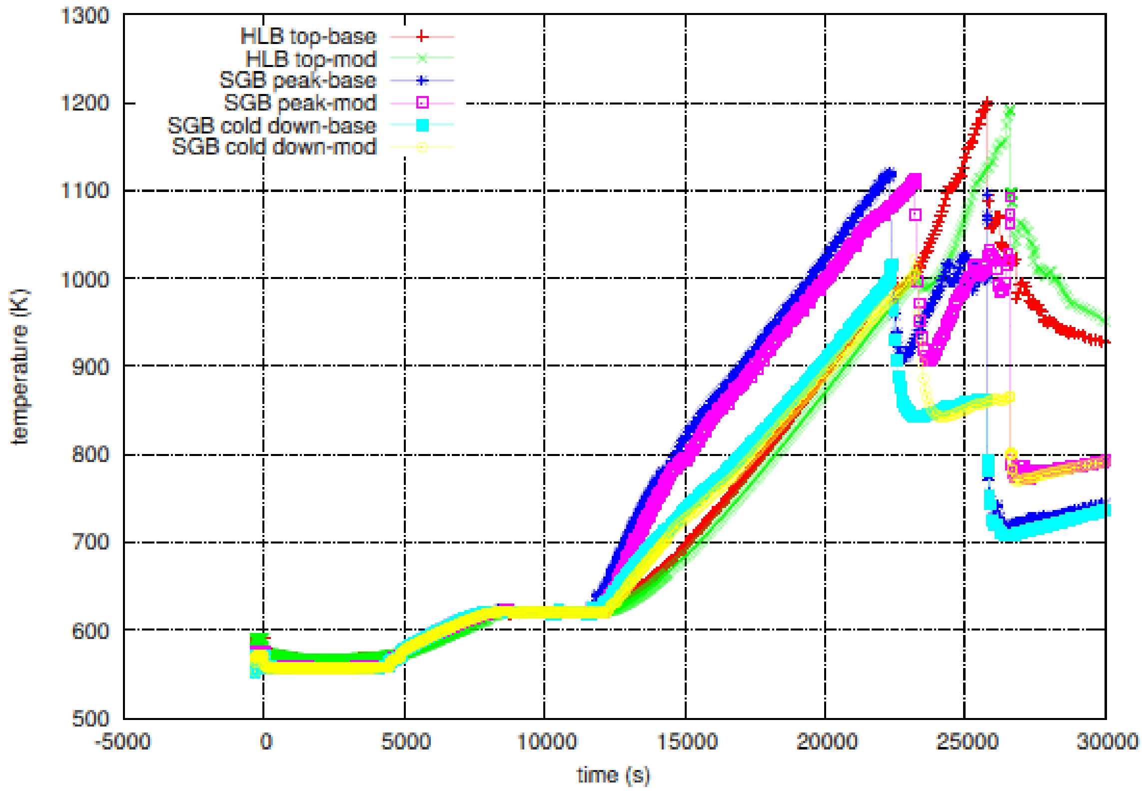

Fig. 4: comparison of stsbo-noRCPleak Loop B structure temperatures to those of stsbo

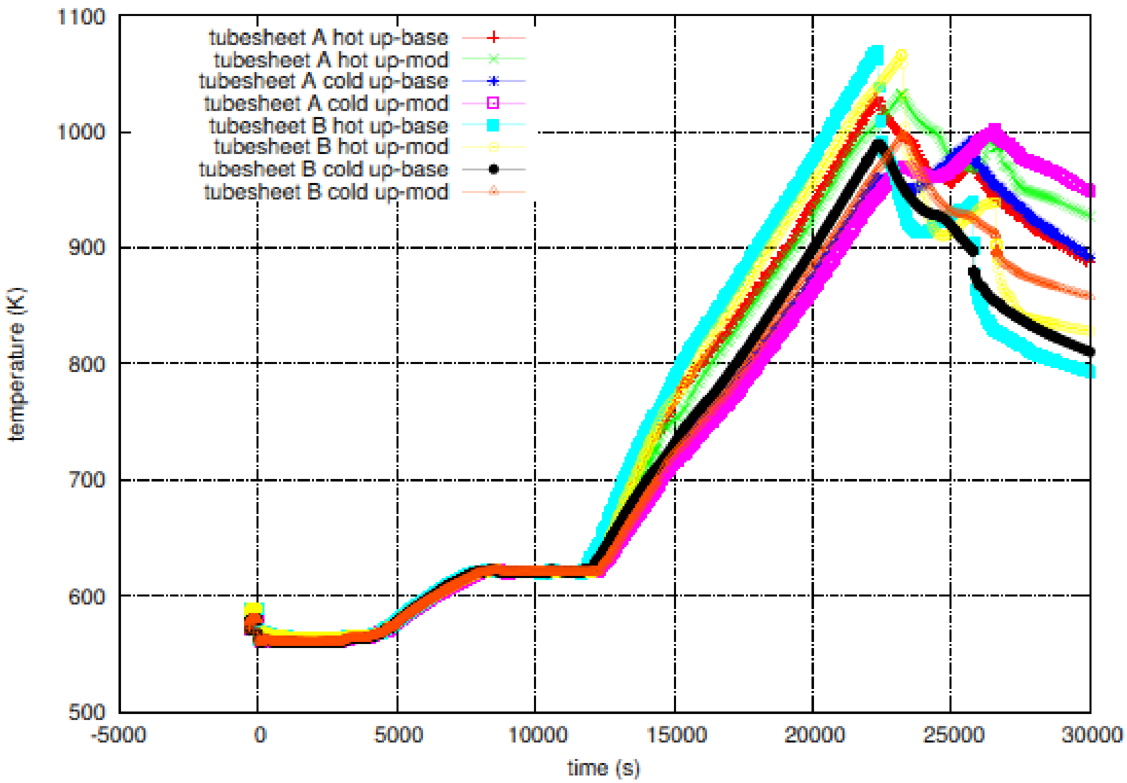

Fig. 5: comparison of stsbo-noRCPleak tubesheet structure temperatures to those of stsbo

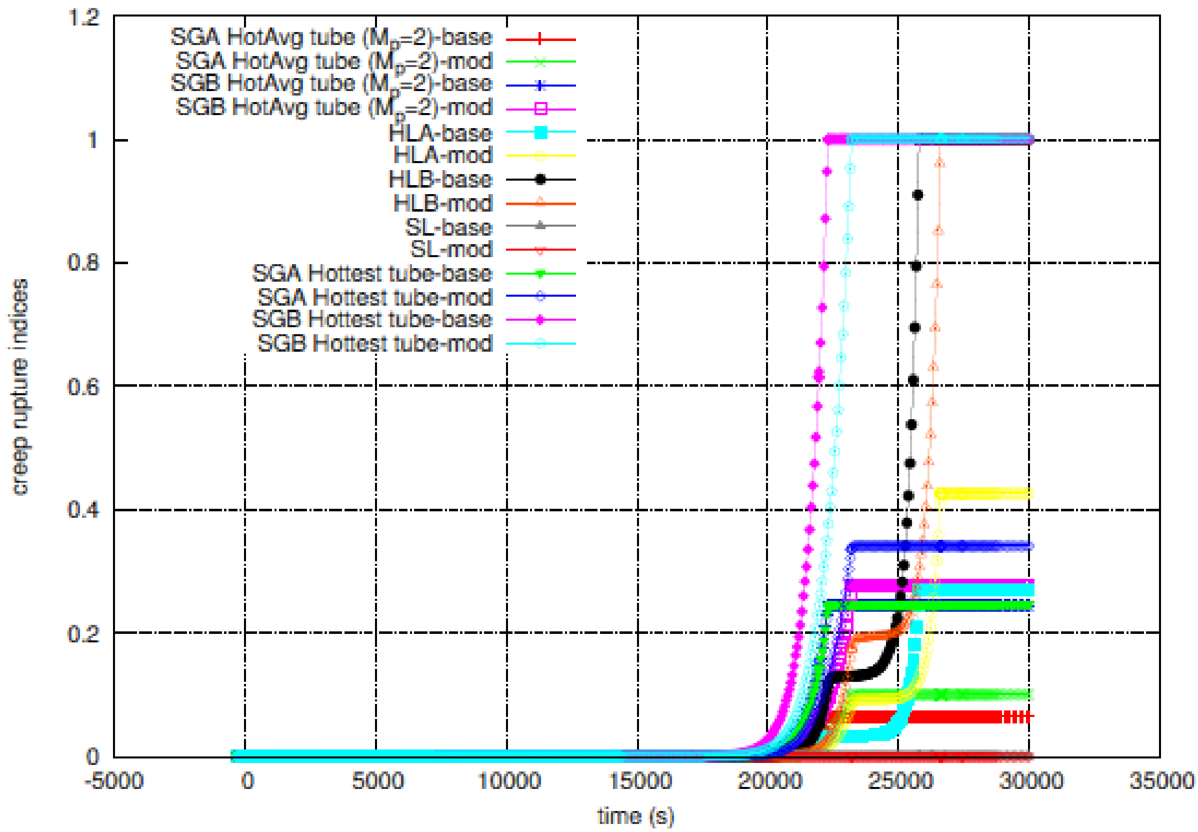

Fig. 6: comparison of stsbo-noRCPleak creep rupture indices to those of stsbo

2 Additional stsbo plots

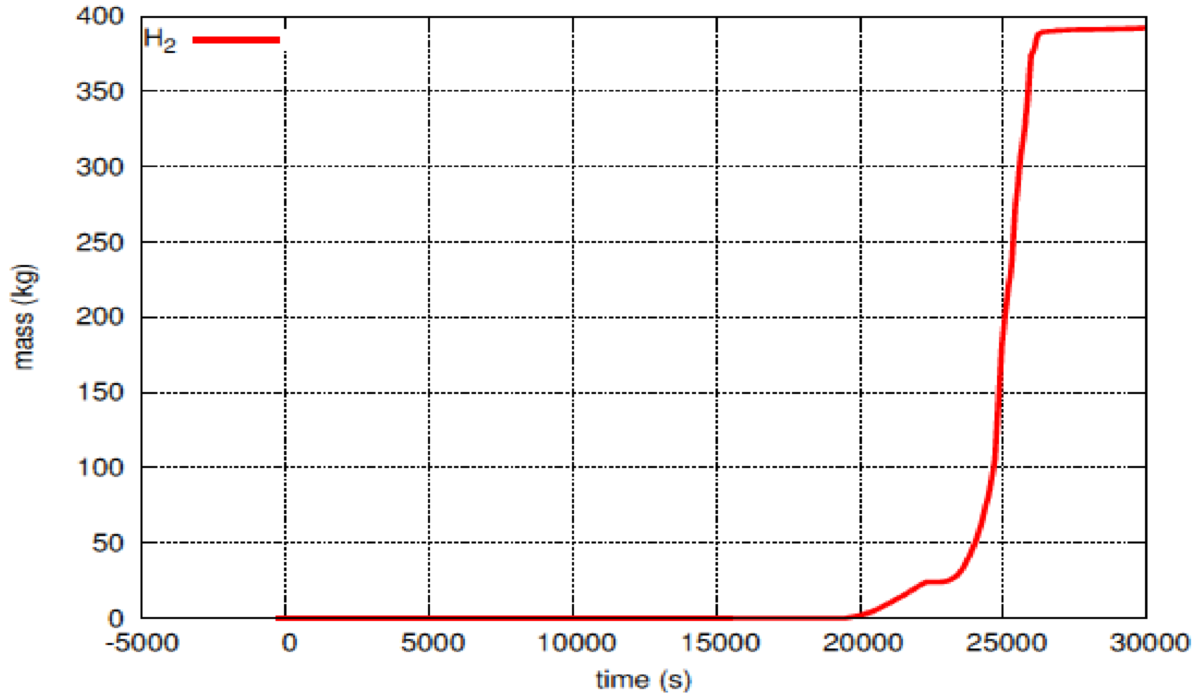

Fig. 7: total hydrogen generation for the stsbo calculation

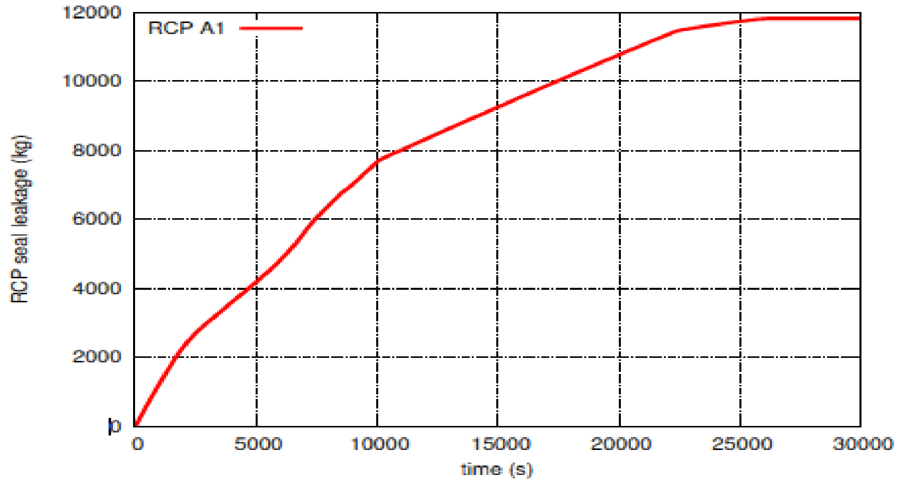

Fig. 8: RCP seal leakage for the stsbo calculation

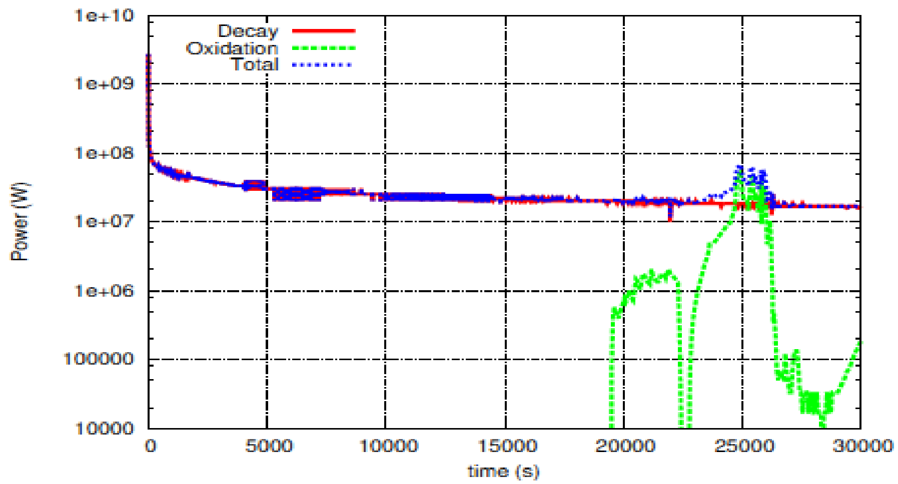

Fig. 9: stsbo decay and oxidation power contribution

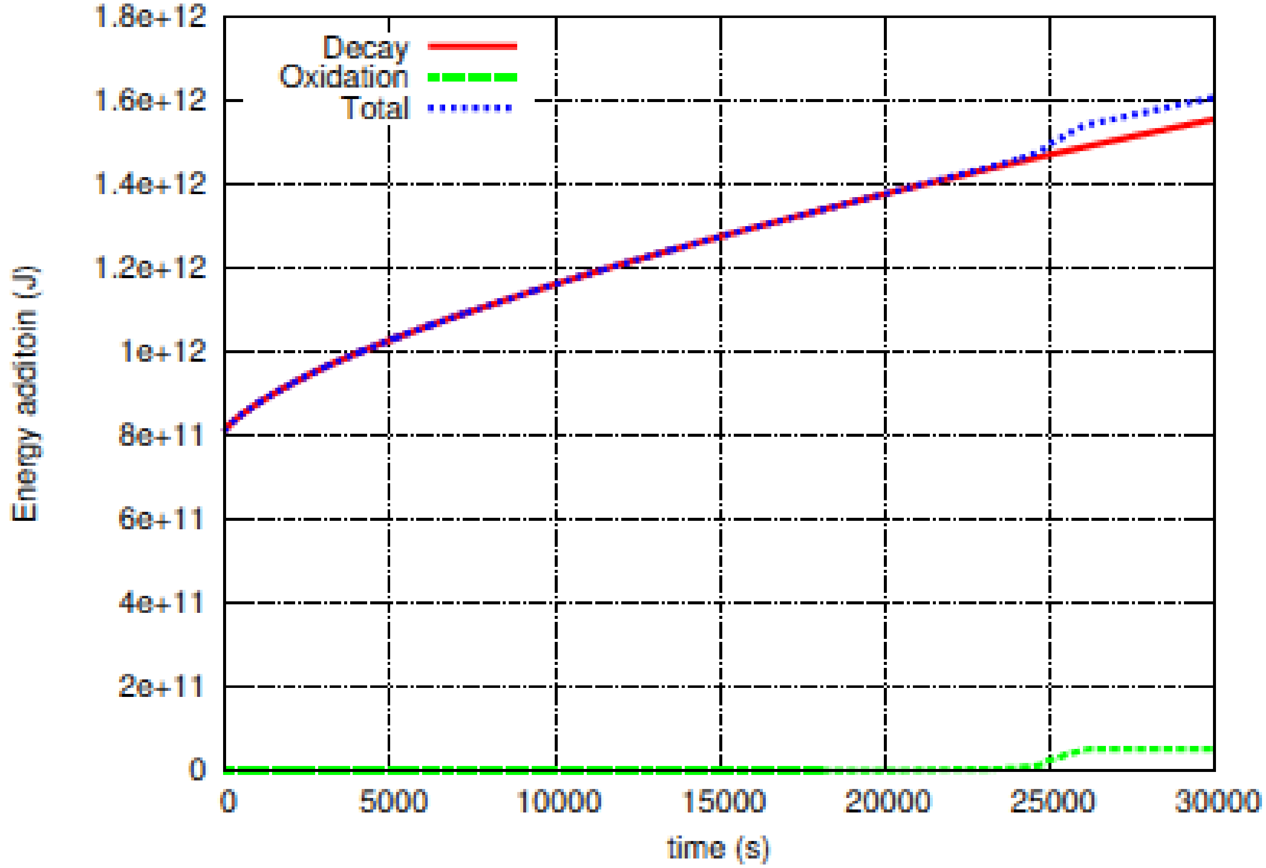

Fig. 10: stsbo decay and oxidation energy addition

3 Natural circulation plot

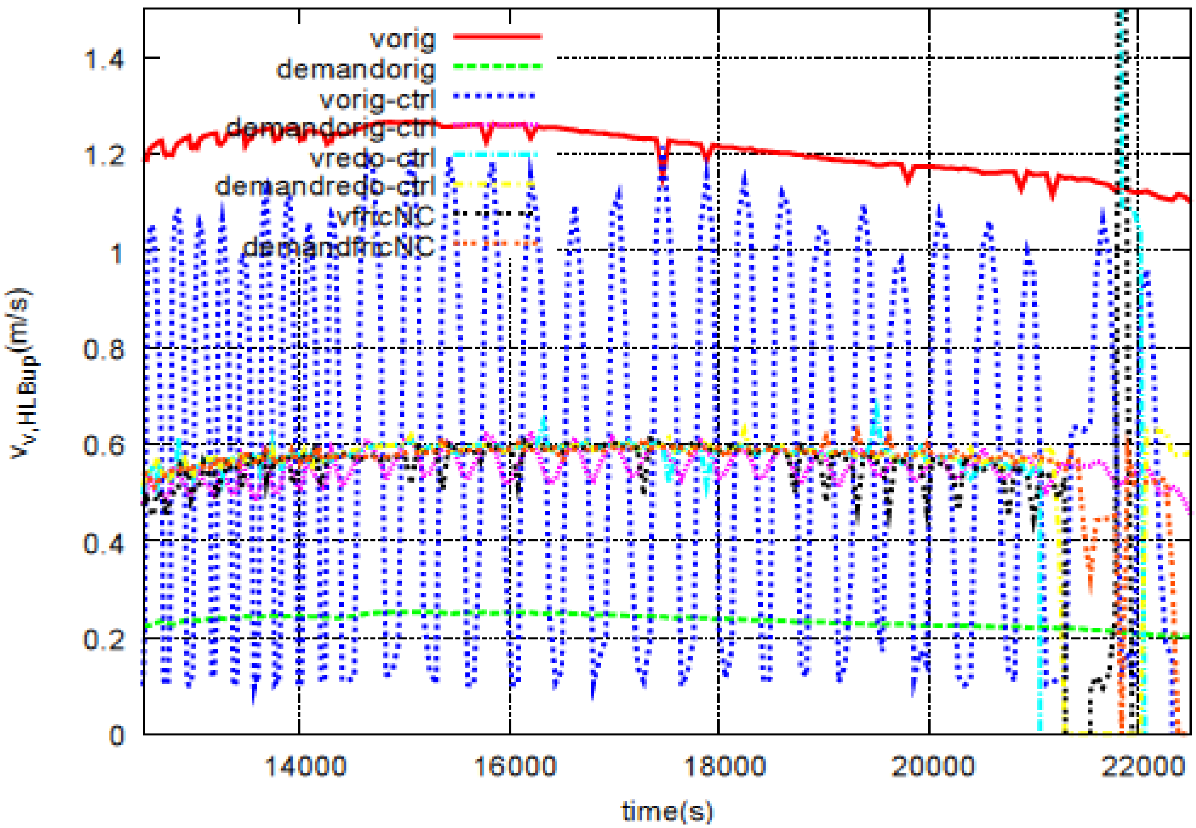

Fig. 11: Comparison of upper hot leg B velocities for different hot-leg natural circulation models