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Review of Reactor Pressure Vessel Evaluation Report for Yankee Rowe Nuclear Power Station (YAEC No. 1735)

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Abstract

The Yankee Atomic Electric Company has performed an Integrated Pressurized Thermal Shock (IPTS)-type evaluation of the Yankee Rowe reactor pressure vessel in accordance with the PTS Rule (10 CFR 50.61) and U.S. Regulatory Guide 1.154. Upon receipt of the corresponding document (YAEC 1735), the Nuclear Regulatory Commission requested that the Oak Ridge National Laboratory (ORNL) review the YAEC document and perform an independent probabilistic fracture-mechanics analysis. The ORNL review included a detailed comparison of the Pacific Northwest Laboratory (PNL) and the ORNL probabilistic fracture-mechanics codes (VISA-II and OCA-P, respectively). The review identified minor errors that were subsequently corrected and one significant difference in philosophy with regard to the variation of fracture toughness through the wall. Also, the two codes have a few dissimilar peripheral features. Aside from these differences, VISA-II and OCA-P are very similar. With errors corrected and an adjustment made for the differences in the treatment of fracture-toughness distribution through the wall, the two codes yield essentially the same value of the conditional probability of failure.

The ORNL independent evaluation indicated RT_{NDT} values considerably greater than those corresponding to the PTS-Rule screening criteria and a frequency of failure substantially greater than that corresponding to the "primary acceptance criterion" in Reg. Guide 1.154. Time constraints, however, prevented as rigorous a treatment as the situation deserves. Thus, these results are very preliminary.

Contents

Preface

This report was originally submitted to the United States Nuclear Regulatory Commission (USNRC) on November 5, 1990, as a draft of a letter report. More recently, there was an urgent need on the part of the NRC for the document to be published as soon as possible as a NUREG report, and to expedite publication, a request was made by the NRC to minimize changes, including editing and graphics. For this reason, the report does not meet the usual high standards of the Oak Ridge National Laboratory nor does it fully conform to the NRC NUREG report format.

FOREWORD

The work reported here was performed at Oak Ridge National Laboratory under the Heavy-Section Steel Technology (HSST) Program, W. E. Pennell, Program Manager. The program is sponsored by the Office of Nuclear Regulatory Research of the U.S. Nuclear Regulatory Commission (NRC). The technical monitor for the NRC is M. E. Mayfield.

This report is designated HSST Report 136. Prior and future reports in this series are listed below.

1. S. Yukawa, General Electric Company, Schenectady, N.Y., *Evaluation of Periodic Proof Testing and Warm Prestressing Procedures for Nuclear Reactor Vessels*, HSSTP-TR-1, July 1, 1969.
2. L. W. Loechel, Martin Marietta Corporation, Denver, Colo., *The Effect of Testing Variables on the Transition Temperature in Steel*, MCR-69-189, November 20, 1969.
3. P. N. Randall, TRW Systems Group, Redondo Beach, Calif., *Gross Strain Measure of Fracture Toughness of Steels*, HSSTP-TR-3, November 1, 1969.
4. C. Visser, S. E. Gabrielse, and W. VanBuren, Westinghouse Electric Corporation, PWR Systems Division, Pittsburgh, Pa., *A Two-Dimensional Elastic-Plastic Analysis of Fracture Test Specimens*, WCAP-7368, October 1969.
5. T. R. Mager and F. O. Thomas, Westinghouse Electric Corporation, PWR Systems Division, Pittsburgh, Pa., *Evaluation* by *Linear Elastic Fracture Mechanics of Radiation Damage to Pressure Vessel Steels*, WCAP-7328 (Rev.), October 1969.
6. W. O. Shabbits, W. H. Pryle, and E. T. Wessel, Westinghouse Electric Corporation, PWR Systems Division, Pittsburgh, Pa., *Heavy-Section Fracture Toughness Properties of A533 Grade B Class 1 Steel Plate and Submerged Arc Weldment*, WCAP-7414, December 1969.
7. F. J. Loss, Naval Research Laboratory, Washington, D.C., *Dynamic Tear Test Investigations of the Fracture Toughness of Thick-Section Steel*, NRL-7056, May 14, 1970.
8. P. B. Crosley and E. J. Ripling, Materials Research Laboratory, Inc., Glenwood, Ill., *Crack Arrest Fracture Toughness of A533 Grade B Class 1 Pressure Vessel Steel*, HSSTP-TR-8, March 1970.
9. T. R. Mager, Westinghouse Electric Corporation, PWR Systems Division, Pittsburgh, Pa., *Post-Irradiation Testing* of *2T Compact Tension Specimens*, WCAP-7561, August 1970.
10. T. R. Mager, Westinghouse Electric Corporation, PWR Systems Division, Pittsburgh, Pa., *Fracture Toughness Characterization Study of A533, Grade B, Class 1 Steel*, WCAP-7578, October 1970.
11. T. R. Mager, Westinghouse Electric Corporation, PWR Systems Division, Pittsburgh, Pa., *Notch Preparation in Compact Tension Specimens*, WCAP-7579, November 1970.
12. N. Levy and P. V. Marcal, Brown University, Providence, R.I., *Three-Dimensional Elastic-Plastic Stress and Strain Analysis for Fracture Mechanics, Phase I: Simple Flawed Specimens*, HSSTP-TR-12, December 1970.
13. W. O. Shabbits, Westinghouse Electric Corporation, PWR Systems Division, Pittsburgh, Pa., *Dynamic Fracture Toughness Properties of Heavy Section A533 Grade B Class 1 Steel Plate*, WCAP-7623, December 1970.
14. P. N. Randall, TRW Systems Group, Redondo Beach, Calif., *Gross Strain Crack Tolerance of A 533-B Steel*, HSSTP-TR-14, May 1, 1971.
15. H. T. Corten and R. H. Sailors, University of Illinois, Urbana, Ill., *Relationship Between Material Fracture Toughness Using Fracture Mechanics and Transition Temperature Tests*, T&AM Report 346, August 1, 1971.
16. T. R. Mager and V. J. McLaughlin, Westinghouse Electric Corporation, PWR Systems Division, Pittsburgh, Pa., *The Effect of an Environment of High Temperature Primary Grade Nuclear Reactor Water on the Fatigue Crack Growth Characteristics of A533 Grade B Class 1 Plate and Weldment Material*, WCAP-7776, October 1971.
17. N. Levy and P. V. Marcal, Brown University, Providence, R.I., *Three-Dimensional Elastic-Plastic Stress and Strain Analysis for Fracture Mechanics, Phase II: Improved Modelling*, HSSTP-TR-17, November 1971.
18. S. C. Grigory, Southwest Research Institute, San Antonio, Tex., *Tests of 6-in.-Thick Flawed Tensile Specimens, First Technical Summary Report, Longitudinal Specimens Numbers 1 through 7*, HSSTP-TR-18, June 1972.

19. P. N. Randall, TRW Systems Group, Redondo Beach, Calif., *Effects of Strain Gradients on the Gross Strain Crack Tolerance of A533-B Steel*, HSSTP-TR-19, June 15, 1972.

20. S. C. Grigory, Southwest Research Institute, San Antonio, Tex., *Tests of 6-Inch-Thick Flawed Tensile Specimens, Second Technical Summary Report, Transverse Specimens Numbers 8 through 10, Welded Specimens Numbers 11 through 13*, HSSTP-TR-20, June 1972.

21. L. A. James and J. A. Williams, Hanford Engineering Development Laboratory, Richland, Wash., Heavy Section Steel Technology Program Technical Report No. 21, *The Effect of Temperature and Neutron Irradiation Upon the Fatigue-Crack Propagation Behavior of ASTM A533 Grade B, Class 1 Steel*, HEDL-TME 72-132, September 1972.

22. S. C. Grigory, Southwest Research Institute, San Antonio, Tex., *Tests of 6-Inch-Thick Flawed Tensile Specimens, Third Technical Summary Report, Longitudinal Specimens Numbers 14 through 16, Unflawed Specimen Number 17*, HSSTP-TR-22, October 1972.

23. S. C. Grigory, Southwest Research Institute, San Antonio, Tex., *Tests of 6-Inch-Thick Tensile Specimens, Fourth Technical Summary Report, Tests of 1-Inch-Thick Flawed Tensile Specimens for Size Effect Evaluation*, HSSTP-TR-23, June 1973.

24. S. P. Ying and S. C. Grigory, Southwest Research Institute, San Antonio, Tex., *Tests of 6-Inch-Thick Tensile Specimens, Fifth Technical Summary Report, Acoustic Emission Monitoring of One-Inch and Six-Inch-Thick Tensile Specimens*, HSSTP-TR-24, November 1972.

25. R. W. Derby, J. G. Merkle, G. C. Robinson, G. D. Whitman, and F. J. Witt, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Test of 6-Inch-Thick Pressure Vessels. Series 1: Intermediate Test Vessels V-1 and V-2*, ORNL-4895, February 1974.

26. W. J. Stelzman and R. G. Berggren, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Radiation Strengthening and Embrittlement in Heavy Section Steel Plates and Welds*, ORNL-4871, June 1973.

27. P. B. Crosley and E. J. Ripling, Materials Research Laboratory, Inc., Glenwood, Ill., *Crack Arrest in an Increasing K-Field*, HSSTP-TR-27, January 1973.

28. P. V. Marcal, P. M. Stuart, and R. S. Bettes, Brown University, Providence, R.I., *Elastic Plastic Behavior of a Longitudinal Semi-Elliptic Crack in a Thick Pressure Vessel*, HSSTP-TR-28, June 1973.

29. W. J. Stelzman, R. G. Berggren, and T. N. Jones, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *ORNL Characterization of Heavy-Section Steel Technology Program Plates 01, 02 and 03*, USNRC Report NUREG/CR-4092 (ORNL/TM-9491), April 1985.

30. Canceled.

31. J. A. Williams, Hanford Engineering Development Laboratory, Richland, Wash., *The Irradiation and Temperature Dependence of Tensile and Fracture Properties of ASTM A533, Grade B, Class 1 Steel Plate and Weldment*, HEDL-TME 73-75, August 1973.

32. J. M. Steichen and J. A. Williams, Hanford Engineering Development Laboratory, Richland, Wash., *High Strain Rate Tensile Properties of Irradiated ASTM A533 Grade B Class 1 Pressure Vessel Steel*, July 1973.

33. P. C. Riccardella and J. L. Swedlow, Westinghouse Electric Corporation, Pittsburgh, Pa., *A Combined Analytical-Experimental Fracture Study of the Two Leading Theories of Elastic-Plastic Fracture (J-Integral and Equivalent Energy)*, WCAP-8224, October 1973.

34. R. J. Podlasek and R. J. Eiber, Battelle Columbus Laboratories, Columbus, Ohio, *Final Report on Investigation of Mode III Crack Extension in Reactor Piping*, December 14, 1973.

35. T. R. Mager, J. D. Landes, D. M. Moon, and V. J. McLaughlin, Westinghouse Electric Corporation, Pittsburgh, Pa., *Interim Report on the Effect of Low Frequencies on the Fatigue Crack Growth Characteristics of A533 Grade B Class 1 Plate in an Environment of High-Temperature Primary Grade Nuclear Reactor Water*, WCAP-8256, December 1973.

36. J. A. Williams, Hanford Engineering Development Laboratory, Richland, Wash., *The Irradiated Fracture Toughness of ASTM A533, Grade B, Class 1 Steel Measured with a Four-Inch-Thick Compact Tension Specimen*, HEDL-TME 75-10, January 1975.

37. R. H. Bryan, J. G. Merkle, M. N. Raftenberg, G. C. Robinson, and J. E. Smith, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Test of 6-Inch-Thick Pressure Vessels. Series 2: Intermediate Test Vessels V-3, V-4, and V-6*, ORNL-5059, November 1975.

38. T. R. Mager, S. E. Yanichko, and L. R. Singer, Westinghouse Electric Corporation, Pittsburgh, Pa., *Fracture Toughness Characterization of HSST Intermediate Pressure Vessel Material*, WCAP-8456, December 1974.

39. J. G. Merkle, G. D. Whitman, and R. H. Bryan, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *An Evaluation of the HSST Program Intermediate Pressure Vessel Tests in Terms of Light-Water-Reactor Pressure Vessel Safety*, ORNL/TM-5090, November 1975.

40. J. G. Merkle, G. C. Robinson, P. P. Holz, J. E. Smith, and R. H. Bryan, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Test of 6-In.-Thick Pressure Vessels. Series 3: Intermediate Test Vessel V-7*, USNRC Report ORNL/NUREG-1, August 1976.

41. J. A. Davidson, L. J. Ceschini, R. P. Shogan, and G. V. Rao, Westinghouse Electric Corporation, Pittsburgh, Pa., *The Irradiated Dynamic Fracture Toughness of ASTM A533, Grade B, Class 1 Steel Plate and Submerged Arc Weldment*, WCAP-8775, October 1976.

42. R. D. Cheverton, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Pressure Vessel Fracture Studies Pertaining to a PWR LOCA-ECC Thermal Shock: Experiments TSE-1 and TSE-2*, USNRC Report ORNL/NUREG/TM-31, September 1976.

43. J. G. Merkle, G. C. Robinson, P. P. Holz, and J. E. Smith, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Test of 6-In.-Thick Pressure Vessels. Series 4: Intermediate Test Vessels V-5 and V-9 with Inside Nozzle Corner Cracks*, USNRC Report ORNL/NUREG-7, August 1977.

44 J. A. Williams, Hanford Engineering Development Laboratory, Richland, Wash., *The Ductile Fracture Toughness of Heavy Section Steel Plate*, USNRC Report NUREG/CR-0859, September 1979.

45. R. H. Bryan, T. M. Cate, P. P. Holz, T. A. King, J. G. Merkle, G. C. Robinson, G. C. Smith, J. E. Smith, and G. D. Whitman, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Test of 6-in.-Thick Pressure Vessels. Series 3: Intermediate Test Vessel V-7A Under Sustained Loading*, USNRC Report ORNL/NUREG-9, February 1978.

46. R. D. Cheverton and S. E. Bolt, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Pressure Vessel Fracture Studies Pertaining to a PWR LOCA-ECC Thermal Shock: Experiments TSE-3 and TSE-4 and Update of TSE-1 and TSE-2 Analysis*, USNRC Report ORNL/NUREG-22, December 1977.

47. D. A. Canonico, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Significance of Reheat Cracks to the Integrity of Pressure Vessels for Light-Water Reactors*, USNRC Report ORNL/NUREG-15, July 1977.

48. G. C. Smith and P. P. Holz, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Repair Weld Induced Residual Stresses in Thick-Walled Steel Pressure Vessels*, USNRC Report NUREG/CR-0093 (ORNL/NUREG/TM-153), June 1978.

49. P. P. Holz and S. W. Wismer, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Half-Bead (Temper) Repair Welding for HSST Vessels*, USNRC Report NUREG/CR-0113 (ORNL/NUREG/TM-177), June 1978.

50. G. C. Smith, P. P. Holz, and W. J. Stelzman, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Crack Extension and Arrest Tests of Axially Flawed Steel Model Pressure Vessels*, USNRC Report NUREG/CR-0126 (ORNL/NUREG/TM-196), October 1978.

51. R. H. Bryan, P. P. Holz, J. G. Merkle, G. C. Smith, J. E. Smith, and W. J. Stelzman, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Test of 6-in.-Thick Pressure Vessels. Series 3: Intermediate Test Vessel V-7B*, USNRC Report NUREG/CR-0309 (ORNL/NUREG-38), October 1978.

52. R. D. Cheverton, S. K. Iskander, and S. E. Bolt, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Applicability of LEFM to the Analysis of PWR Vessels Under LOCA-ECC Thermal Shock Conditions*, USNRC Report NUREG/CR-0107 (ORNL/NUREG-40), October 1978.

53. R. H. Bryan, D. A. Canonico, P. P. Holz, S. K. Iskander, J. G. Merkle, J. E. Smith, and W. J. Stelzman, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Test of 6-in.-Thick Pressure Vessels, Series 3: Intermediate Test Vessel V-8*, USNRC Report NUREG/CR-0675 (ORNL/ NUREG-58), December 1979.

54. R. D. Cheverton and S. K. Iskander, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Application of Static and Dynamic Crack Arrest Theory to TSE-4*, USNRC Report NUREG/CR-0767 (ORNL/NUREG-57), June 1979.

55. J. A. Williams, Hanford Engineering Development Laboratory, Richland, Wash., *Tensile Properties of Irradiated and Unirradiated Welds of A533 Steel Plate and A508 Forgings*, USNRC Report NUREG/CR-1158 (ORNL/Sub/79-50917/2), July 1979.

56. K. W. Carlson and J. A. Williams, Hanford Engineering Development Laboratory, Richland, Wash., *The Effect of Crack Length and Side Grooves on the Ductile Fracture Toughness Properties of ASTM A533 Steel*, USNRC Report NUREG/CR-1171 (ORNL/Sub/79-50917/3), October 1979.

57. P. P. Holz, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Flaw Preparations for HSST Program Vessel Fracture Mechanics Testing; Mechanical-Cyclic Pumping and Electron-Beam Weld-Hydrogen Charge Cracking Schemes*, USNRC Report NUREG/CR-1274 (ORNL/NUREG/TM-369), May 1980.

58. S. K. Iskander, Computer Sciences Div., Union Carbide Corp. Nuclear Div., Oak Ridge, Tenn., *Two Finite Element Techniques for Computing Mode I Stress Intensity Factors in Two- or Three-Dimensional Problems*, USNRC Report NUREG/CR-1499 (ORNL/NUREG/CSD/TM-14), February 1981.

59. P. B. Crosley and E. J. Ripling, Materials Research Laboratory, Glenwood, Ill., *Development of a Standard Test for Measuring K_{Ia} with a Modified Compact Specimen*, USNRC Report NUREG/CR-2294 (ORNL/Sub/81-7755/1), August 1981.

60. S. N. Atluri, B. R. Bass, J. W. Bryson, and K. Kathiresan, Computer Sciences Div., Oak Ridge Gaseous Diffusion Plant, Oak Ridge, Tenn., *NOZ-FLAW: A Finite Element Program for Direct Evaluation of Stress Intensity Factors for Pressure Vessel Nozzle-Corner Flaws*, USNRC Report NUREG/CR-1843 (ORNL/NUREG/CSD/TM-18), March 1981.

61. A. Shukla, W. L. Fourney, and G. R. Irwin, University of Maryland, College Park, Md., *Study of Energy Loss and Its Mechanisms in Homalite 100 During Crack Propagation and Arrest*, USNRC Report NUREG/CR-2[illegible] (ORNL/Sub/79-7778/1), August 1981.

62. S. K. Iskander, R. D. Cheverton, and D. G. Ball, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *OCA-I, A Code for Calculating the Behavior of Flaws on the Inner Surface of a Pressure Vessel Subjected to Temperature and Pressure Transients*, USNRC Report NUREG/CR-2113 (ORNL/NUREG-84), August 1981.

63. R. J. Sanford, R. Chona, W. L. Fourney, and G. R. Irwin, University of Maryland, College Park, Md., *A Photoelastic Study of the Influence of Non-Singular Stresses in Fracture Test Specimens*, USNRC Report NUREG/CR-2179 (ORNL/Sub/79-7778/2), August 1981.

64. B. R. Bass, S. N. Atluri, J. W. Bryson, and K. Kathiresan, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *OR-FLAW: A Finite Element Program for Direct Evaluation of K-Factors for User-Defined Flaws in Plate, Cylinders, and Pressure-Vessel Nozzle Corners*, USNRC Report NUREG/CR-2494 (ORNL/CSD/TM-165), April 1982.

65. B. R. Bass and J. W. Bryson, Oak Ridge Natl. Lab., Oak Ridge Tenn., *ORMGEN-3D: A Finite Element Mesh Generator for 3-Dimensional Crack Geometries*, USNRC Report NUREG/CR-2997, Vol. 1 (ORNL/TM-8527/V1), December 1982.

66. B. R. Bass and J. W. Bryson, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *ORVIRT: A Finite Element Program for Energy Release Rate Calculations for 2-Dimensional and 3-Dimensional Crack Models*, USNRC Report NUREG/CR-2997, Vol. 2 (ORNL/TM-8527/V2), February 1983.

67. R. D. Cheverton, S. K. Iskander, and D. G. Ball, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *PWR Pressure Vessel Integrity During Overcooling Accidents: A Parametric Analysis*, USNRC Report NUREG/CR-2895 (ORNL/TM-7931), February 1983.

68. D. G. Ball, R. D. Cheverton, J. B. Drake, and S. K. Iskander, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *OCA-II, A Code for Calculating Behavior of 2-D and 3-D Surface Flaws in a Pressure Vessel Subjected to Temperature and Pressure Transients*, USNRC Report NUREG/ CR-3491 (ORNL-5934), February 1984.

69. A. Sauter, R. D. Cheverton, and S. K. Iskander, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Modification of OCA-I for Application to a Reactor Pressure Vessel with Cladding on the Inner Surface*, USNRC Report NUREG/CR-3155 (ORNL/TM-8649), May 1983.

70. R. D. Cheverton and D. G. Ball, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *OCA-P, A Deterministic and Probabilistic Fracture-Mechanics Code for Application to Pressure Vessels*, USNRC Report NUREG/CR-3618 (ORNL-5991), May 1984.

71. J. G. Merkle, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *An Examination of the Size Effects and Data Scatter Observed in Small Specimen Cleavage Fracture Toughness Testing*, USNRC Report NUREG/CR-3672 (ORNL/TM-9088), April 1984.

72. C. E. Pugh et al., Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Heavy-Section Steel Technology Program—Five-Year Plan FY 1983–1987*, USNRC Report NUREG/CR-3595 (ORNL/TM-9008), April 1984.

73. D. G. Ball, B. R. Bass, J. W. Bryson, R. D. Cheverton, and J. B. Drake, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Stress Intensity Factor Influence Coefficients for Surface Flaws in Pressure Vessels*, USNRC Report NUREG/CR-3723 (ORNL/CSD/TM-216), February 1985.

74. W. R. Corwin, R. G. Berggren, and R. K. Nanstad, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Charpy Toughness and Tensile Properties of Neutron Irradiated Stainless Steel Submerged-Arc Weld Cladding Overlay*, USNRC Report NUREG/CR-3927 (ORNL/TM-9309), September 1984.

75. C. W. Schwartz, R. Chona, W. L. Fourney, and G. R. Irwin, University of Maryland, College Park, Md., *SAMCR: A Two-Dimensional Dynamic Finite Element Code for the Stress Analysis of Moving CRacks*, USNRC Report NUREG/CR-3891 (ORNL/Sub/79-7778/3), November 1984.

76. W. R. Corwin, G. C. Robinson, R. K. Nanstad, J. G. Merkle, R. G. Berggren, G. M. Goodwin, R. L. Swain, and T. D. Owings, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Effects of Stainless Steel Weld Overlay Cladding on the Structural Integrity of Flawed Steel Plates in Bending, Series 1*, USNRC Report NUREG/CR-4015 (ORNL/TM-9390), April 1985.

77. R. H. Bryan, B. R. Bass, S. E. Bolt, J. W. Bryson, D. P. Edmonds, R. W. McCulloch, J. G. Merkle, R. K. Nanstad, G. C. Robinson, K. R. Thoms, and G. D. Whitman, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Pressurized-Thermal-Shock Test of 6-in.-Thick Pressure Vessels. PTSE-1: Investigation of Warm Prestressing and Upper-Shelf Arrest*, USNRC Report NUREG/CR-4106 (ORNL-6135), April 1985.

78. R. D. Cheverton, D. G. Ball, S. E. Bolt, S. K. Iskander, and R. K. Nanstad, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Pressure Vessel Fracture Studies Pertaining to the PWR Thermal-Shock Issue: Experiments TSE-5, TSE-5A, and TSE-6*, USNRC Report NUREG/CR-4249 (ORNL-6163), June 1985.

79. R. D. Cheverton, D. G. Ball, S. E. Bolt, S. K. Iskander, and R. K. Nanstad, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Pressure Vessel Fracture Studies Pertaining to the PWR Thermal-Shock Issue: Experiment TSE-7*, USNRC Report NUREG/CR-4304 (ORNL-6177), August 1985.

80. R. H. Bryan, B. R. Bass, S. E. Bolt, J. W. Bryson, J. G. Merkle, R. K. Nanstad, and G. C. Robinson, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Test of 6-in.-Thick Pressure Vessels. Series 3: Intermediate Test Vessel V-8A—Tearing Behavior of Low Upper-Shelf Material*, USNRC Report NUREG/CR-4760 (ORNL-6187), May 1987.

81. R. D. Cheverton and D. G. Ball, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *A Parametric Study of PWR Pressure Vessel Integrity During Overcooling Accidents, Considering Both 2-D and 3-D Flaws*, USNRC Report NUREG/CR-4325 (ORNL/TM-9682), August 1985.

82. E. C. Rodabaugh, E. C. Rodabaugh Associates, Inc., Hilliard, Ohio, *Comments on the Leak-Before-Break Concept for Nuclear Power Plant Piping Systems*, USNRC Report NUREG/CR-4305 (ORNL/Sub/82-22252/3), August 1985.

83. J. W. Bryson, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *ORVIRT.PC: A 2-D Finite Element Fracture Analysis Program for a Microcomputer*, USNRC Report NUREG/CR-4367 (ORNL-6208), October 1985.

84. D. G. Ball and R. D. Cheverton, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Adaptation of OCA-P, A Probabilistic Fracture-Mechanics Code, to a Personal Computer*, USNRC Report NUREG/CR-4468 (ORNL/CSD/TM-233), January 1986.

85. J. W. Bryson and B. R. Bass, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *ORMGEN.PC: A Microcomputer Program for Automatic Mesh Generation of 2-D Crack Geometries*, USNRC Report NUREG/CR-4475 (ORNL-6250), March 1986.

86. G. D. Whitman, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Historical Summary of the Heavy-Section Steel Technology Program and Some

Related Activities in Light-Water Reactor Pressure Vessel Safety Research, USNRC Report NUREG/CR-4489 (ORNL-6259), March 1986.

87. C. Inversini and J. W. Bryson, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *ORPLOT PC: A Graphic Utility for ORMGEN.PC and ORVIRT.PC*, USNRC Report NUREG/CR-4633 (ORNL-6291), June 1986.

88. J. J. McGowan, R. K. Nanstad, and K. R. Thoms, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Characterization of Irradiated Current-Practice Welds and A533 Grade B Class 1 Plate for Nuclear Pressure Vessel Service*, USNRC Report NUREG/CR-4880 (ORNL/TM-10387), July 1988.

89. K. V. Cook and R. W. McClung, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Flaw Density Examinations of a Clad Boiling Water Reactor Pressure Vessel Segment*, USNRC Report NUREG/CR-4860 (ORNL/TM-10364), April 1987.

90. D. J. Naus, B. R. Bass, C. E. Pugh, R. K. Nanstad, J. G. Merkle, W. R. Corwin, and G. C. Robinson, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Crack-Arrest Behavior in SEN Wide Plates of Quenched and Tempered A 533 Grade B Steel Tested Under Nonisothermal Conditions*, USNRC Report NUREG/CR-4930 (ORNL-6388), August 1987.

91. D. B. Barker, R. Chona, W. L. Fourney, and G. R. Irwin, University of Maryland, College Park, Md., *A Report on the Round Robin Program Conducted to Evaluate the Proposed ASTM Standard Test Method for Determining the Plane Strain Crack Arrest Fracture Toughness, K_{Ia}*, of Ferritic Materials, USNRC Report NUREG/CR-4966 (ORNL/Sub/79-7778/4), January 1988.

92. W. H. Bamford, Westinghouse Electric Corporation, Pittsburgh, Pa., *A Summary of Environmentally Assisted Crack-Growth Studies Performed at Westinghouse Electric Corporation Under Funding from the Heavy-Section Steel Technology Program*, USNRC Report NUREG/CR-5020 (ORNL/Sub/82-21598/1), May 1988.

93. R. H. Bryan, B. R. Bass, S. E. Bolt, J. W. Bryson, W. R. Corwin, J. G. Merkle, R. K. Nanstad, and G. C. Robinson, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Pressurized-Thermal-Shock Test of 6-in.-Thick Pressure Vessels. PTSE-2: Investigation of Low Tearing Resistance and Warm Prestressing*, USNRC Report NUREG/CR-4888 (ORNL-6377), December 1987.

94. J. H. Giovanola and R. W. Klopp, SRI International, Menlo Park, Calif., *Viscoplastic Stress-Strain Characterization of A533B Class 1 Steel*, USNRC Report NUREG/CR-5066 (ORNL/Sub/87-SA193/1), September 1989.

95. L. F. Miller et al., Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Neutron Exposure Parameters for the Metallurgical Test Specimens in the Fifth Heavy-Section Steel Technology Irradiation Series Capsules*, USNRC Report NUREG/ CR-5019 (ORNL/TM-10582), March 1988.

96. Canceled.

97. D. J. Naus, J. Keeney-Walker, and B. R. Bass, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *High-Temperature Crack-Arrest Behavior in 152-mm-Thick SEN Wide Plates of Quenched and Tempered A 533 Grade B Steel*, USNRC Report NUREG/CR-5330 (ORNL/TM-11083), April 1989.

98. K. V. Cook, R. A. Cunningham, Jr., and R. W. McClung, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Detection and Characterization of Indications in Segments of Reactor Pressure Vessels*, USNRC Report NUREG/CR-5322 (ORNL/TM-11072), August 1989.

99. R. D. Cheverton, W. E. Pennell, G. C. Robinson, and R. K. Nanstad, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Impact of Radiation Embrittlement on Integrity of Pressure Vessel Supports for Two PWR Plants*, NUREG/CR-5320 (ORNL/TM-10966), February 1989.

100. D. J. Naus, J. Keeney-Walker, B. R. Bass, S. K. Iskander, R. J. Fields, R. deWitt, and S. R. Low III, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *SEN Wide-Plate Crack-Arrest Tests Utilizing A 533 Grade B Class 1 Material: WP-CE Test Series*, USNRC Report NUREG/CR-5408 (ORNL/TM-11269), November 1989.

101. D. J. Naus, J. Keeney-Walker, B. R. Bass, S. K. Iskander, R. J. Fields, R. deWitt, and S. R. Low III, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *High Temperature Crack-Arrest Tests Using 152-mm-Thick SEN Wide Plates of Low Upper-Shelf Base Material:*

Tests WP-2.2 and WP-2.6, USNRC Report NUREG/CR-5450 (ORNL/TM-11352), February 1990.

102. Canceled.

103. D. J. Naus, J. Keeney-Walker, B. R. Bass, G. C. Robinson, S. K. Iskander, D. J. Alexander, R. J. Fields, R. deWitt, S. R. Low, C. W. Schwartz, and I.-B. Johansson, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Crack-Arrest Behavior in SEN Wide Plates of Low Upper-Shelf Base Metal Tested Under Nonisothermal Conditions: WP-2 Series*, USNRC Report NUREG/CR-5451 (ORNL-6584), August 1990.

104. T. L. Dickson, R. D. Cheverton, and D. K. Shum, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Inclusion of Unstable Ductile Tearing and Extrapolated Crack-Arrest Toughness Data in PWR Vessel Integrity Assessment*, USNRC Report NUREG/CR-5473 (ORNL/TM-11450), May 1990.

105. T. J. Theiss, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Recommendations for the Shallow-Crack Fracture Toughness Testing Task Within the HSST Program*, USNRC Report NUREG/CR-5554 (ORNL/TM-11509), September 1990.

106. J. G. Merkle, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *An Overview of the Low Upper Shelf Toughness Safety Margin Issue*, USNRC Report NUREG/CR 5552 (ORNL/TM-11314), August 6, 1990.

107. D. K. M. Shum, J. G. Merkle, J. Keeney-Walker, and B. R. Bass, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Analytical Studies of Transverse Strain Effects on Fracture Toughness for Circumferentially Oriented Cracks*, USNRC Report NUREG/CR-5592 (ORNL/TM-11581), April 1991.

108. J. D. Landes, *Extrapolation of the J-R Curve for Predicting Reactor Vessel Integrity*, USNRC Report NUREG/CR-5650 (ORNL/Sub/89-99732/1) January 1992.

109. J. Keeney-Walker, B. R. Bass, and J. D. Landes, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *An Investigation of Crack-Tip Stress-Field Criteria for Predicting Cleavage-Crack Initiation*, USNRC Report NUREG/CR-5651 (ORNL/TM-11692) September 1991.

110. G. R. Irwin, University of Maryland, "Use of Thickness Reduction to Estimate Values of K," USNRC Report NUREG/CR-5697 (ORNL/Sub/79-7778C/5). Prepared at University of Maryland for Martin Maarietta Energy Systems, Inc., Oak Ridge Natl. Lab., November 1991.

111. Pedro Albrecht, University of Maryland, *Limit Pressure Analysis of PTSE-2 Vessel*, USNRC Report NUREG/CR-5698 (ORNL/Sub/79-7778/6), prepared at University of Maryland for Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn. (to be published).

112. James W. Dally, William L. Fourney, and George R. Irwin, University of Maryland, *Lower-Bound Initiation Toughness with a Modified-Charpy Specimen*, USNRC Report NUREG/CR-5703 (ORNL/Sub/79-7778/7), prepared at Univeristy of Maryland for Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn. November 1991.

113. S. K. Iskander, G. C. Robinson, W. R. Corwin, B. C. Oland, D. J. Alexander, and K. V. Cook, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Experimental Results of Tests to Investigate Flaw Behavior of Mechanically Loaded Stainless Steel Clad Plates*, USNRC Report NUREG/CR-5785 (to be published).

114. S. T. Rolfe, University of Kansas, "Interpretive Report on the Application of Shallow-Flaw CTOD Test Data to the Structural Margin Assessment of Reactor Pressure Vessels with Flaws," USNRC Report NUREG/CR-5767 (ORNL/Sub/90-SH640/1) November 1991.

115. D. E. McCabe, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., "Comparison of Weibull and β_{Ic} Analysis of Transition Range Fracture Toughness Data," USNRC Report NUREG/CR-5788 (ORNL/TM-11959) January 1992.

116. This report.

1 Introduction

In early August 1990, the Nuclear Regulatory Commission (NRC) requested[1] that Oak Ridge National Laboratory (ORNL) coordinate and participate in a review of a report entitled, *Reactor Pressure Vessel Evaluation Report for Yankee Nuclear Power Plant*, YAEC No. 1735, July 1990,[2] which was prepared by the Yankee Atomic Electric Company (YAEC). The review was to cover primarily the pressurized-thermal-shock (PTS) analysis described in Sect. 6 and the upper-shelf-energy analysis described in Sect. 3. The request also indicated that Idaho National Engineering Laboratory (INEL) would provide thermal/hydraulic input, and Pacific Northwest Laboratory (PNL) would provide input with regard to the VISA code,[3] which was used by YAEC for the probabilistic fracture-mechanics analysis.

The NRC request also specified a completion date of September 17, 1990, a deliverable in the form of a draft letter report on that date, and a planning meeting on August 9, 1990. This meeting was held as scheduled, but because of prior commitments and delays in establishing a subcontract and in obtaining necessary information from YAEC, the completion date for the draft was eventually changed to November 5, 1990. A final report was to be issued on an unspecified date, and this publication constitutes the final report.

The team members contributing directly to the ORNL-coordinated effort are indicated below along with their intended areas of responsibility.

R. D. Cheverton, ORNL: (1) coordination of the efforts of the 10 reviewers; (2) collection and transmittal to the NRC (Pat Sears) of all questions for the utility; (3) contributions to the probabilistic fracture-mechanics analysis review, and (4) preparation of a letter report and transmittal of same to the NRC by November 5, 1990, that contains the contributions of each of the 10 reviewers.

T. L. Dickson, ORNL: (1) check input to the fracture-mechanics analyses; (2) investigate the validity of the probabilistic fracture-mechanics codes OCA-P[4] and VISA-II;[3] (3) evaluate appropriateness of flaw density, flaw-size distribution function, flaw-aspect ratios, vessel region division, and stress-intensity-factor influence coefficients used in the Yankee Rowe VISA-II calculation; and (4) calculate the conditional probability and frequency of failure for the Yankee Rowe vessel using OCA-P.

J. G. Merkle, ORNL: (1) evaluation of the methodology used for including fracture-toughness upper shelf and (2) consultation on radiation-damage and fracture mechanics issues.

R. K. Nanstad, ORNL: evaluation of (1) fracture-toughness curves for unirradiated material, (2) calculation of RT_{NDT}, and (3) surveillance program.

D. L. Selby, ORNL: consultation with regard to definition of postulated PTS transients and estimation of their frequencies.

D. A. Bozarth, SAIC:[a] evaluation of the methodology for estimating mean values of the calculated frequency of failure.

J. W. Minarick, SAIC: evaluation of completeness of list of postulated PTS transients and review of estimated frequencies.

K. A. Williams, SAIC: (1) evaluation of the thermal/hydraulic mixing analyses for transients involving stagnation in one or more loops and (2) evaluation of the appropriateness of RETRAN[5] for the pressurized-thermal-shock (PTS) transient thermal/hydraulic analyses.

F. A. Simonen, PNL: (1) comparison of the YAEC version of VISA-II and PNL's version, (2) evaluation of input to the fracture-mechanics analyses, (3) participation in the comparison of OCA-P and VISA-II, and (4) evaluation of vessel inspection program.

L. W. Ward, INEL: (1) evaluation of adequacy of modeling used for RETRAN analyses, (2) comparison of RETRAN and version of RELAP-5[6] used for NRC/ORNL IPTS studies of H. B. Robinson plant,[7] and (3) consultation with regard to definition of postulated transients.

INEL and specific members of SAIC and ORNL were included because of their earlier involvement in the development of the integrated-pressurized-thermal-shock (IPTS) methodology.[7]

To a large extent the adequacy and accuracy of the YAEC evaluation were judged on the basis of the methodology developed as a part of the NRC/ORNL IPTS study,[7] the NRC PTS Rule (10CFR50.61),[8] and the *NRC Regulatory Guide 1.154*,[9] which identifies acceptable IPTS-evaluation methodologies and a target maximum permissible value ("primary acceptance criterion") of the calculated frequency of vessel failure (through-wall-cracking).

Primary sources of information pertaining to the review were the YAEC report (YAEC No. 1735); *Regulatory Guide 1.99*, Rev. 2 (radiation-damage correlations); *Reg. Guide 1.154*; 10CFR50.61; radiation-damage evaluations performed by G. R. Odette

[a]Science Applications International Corporation

(University of California) and A. L. Hiser, Jr. (NRC), who were not specified as members of the above team; the Yankee Rowe emergency operating procedures (EPOs); and Refs. 3, 4, and 7.

This report constitutes a compilation of the contributions made by each of the team members (included as appendices), a summary and discussion of the findings, and indications of information that is believed necessary for a more thorough review. Some of this information was requested earlier but has not yet been received.

2 Scope of Review

The scope of the review includes a review of "all" aspects of the PTS evaluation, upper-shelf-energy considerations, low temperature over pressurization (LTOP), and vessel inspection. The PTS evaluation includes: (1) postulation of PTS transients and estimation of their frequencies of occurrence; (2) thermal/hydraulic analyses to obtain the downcomer coolant temperature, primary-system pressure and vessel inner-surface fluid-film heat transfer coefficient, each as a function of time in the transient; (3) radiation-induced increase in the reference nil-ductility transition temperature (RT_{NDT}) for the vessel plate and weld material [this requires knowledge of the vessel fast-neutron fluences, operating temperatures and chemistry (Cu and Ni)]; (4) a probabilistic fracture-mechanics analysis to determine the conditional probability of vessel failure for each transient believed to be a significant contributor to the frequency of failure; (5) a summation of the frequencies of failure for each transient to obtain the overall frequency of failure; and (6) an uncertainty analysis, or equivalent, to obtain a "mean" value of the frequency for comparison with the value corresponding to the "primary acceptance criterion" in *Reg. Guide 1.154*. Each of these items was considered in the review.

The scope of the review also included an independent calculation by ORNL of the frequency of vessel failure. For this analysis, best-estimate inputs were used to obtain a best estimate of the conditional probability and frequency of vessel failure. The inputs were best estimates in the sense that in ORNL's opinion they represented the most likely values based on all data available to ORNL at the time of the independent analysis. This approach is consistent with that used in the NRC/ORNL IPTS studies,[7] which provided an NRC-accepted probabilistic methodology for evaluating PWR pressure vessel integrity. As additional plant-specific data are obtained, it is likely that the best-estimates will change also.

3 PTS Transients and Their Frequencies (Appendices A and B)

Questions of particular concern with regard to this subject matter are (1) have the actual dominant transients been postulated, and (2) are the estimated frequencies of occurrence of the transients that are suspected of being dominant realistic or at least conservative?

The consensus of the reviewers is that insufficient information was available to make an accurate judgement with regard to the selection of transients. Even so, if consideration of a single transient or category of transients indicates an excessively high frequency of failure, than consideration of other transients may not be necessary. The reviewers followed this line of thinking in addition to making numerous comments, suggestions, and estimates regarding definition of transients and their frequencies (Appendices A and B).

The YAEC report identifies a small-break LOCA (SBLOCA-7) as the dominant transient and assigns a frequency of occurrence to this transient of $\sim 5 \times 10^{-4}$/yr. As indicated in Appendix A, the reviewers suggest a more realistic value of $1 - 2 \times 10^{-3}$, which is considered to be a mean value. If other LOCAs are included to account for their contribution in a conservative manner, assuming that SBLOCA-7 represents the most severe of the LOCAs, the effective frequency is increased to $\sim 4 \times 10^{-3}$.

4 PTS Transient Thermal/Hydraulics (Appendices A and B)

The question of particular concern regarding the YAEC thermal/hydraulic analysis is whether the PTS transient described by the calculated primary-system pressure, downcomer coolant temperature, and vessel inner-surface heat-transfer coefficient is likely to be more severe than indicated. The reviewers believe that the transient described in the Yankee report is a best estimate, but the actual transient is much more likely to be more severe than less severe. With regard to temperature and pressure, the severity of the transient is more likely to be greater than less. The heat-transfer coefficient, on the other hand, is more likely to be less, and this would tend to reduce the severity; however, based on sensitivity studies in Ref. 7, it is believed that the reduction attributed to the heat-transfer coefficient would not be significant.

5 Radiation Effects (Appendix C)

5.1 Increase in RT_{NDT}

There are two values of RT_{NDT} of particular interest with regard to 10CFR50.61 and *Reg. Guide 1.154*. For 10CFR50.61, a + 2σ (two standard deviations) value is needed for comparison with the PTS screening criteria. For *Reg. Guide 1.154*, a mean value and a distribution are needed for use in a probabilistic fracture-mechanics analysis.

ORNL and YAEC estimates for 10CFR50.61 +2σ values, minus the 2σ, are given in Table C.2 of Appendix C for 1990. Assuming 2$\sigma \cong$ 60°F, it is apparent that all values exceed the screening criteria, which are 270°F for axial flaws and 300°F for circumferential flaws. As required by *Reg. Guide 1.99*, Rev. 2, the copper concentration in the welds was assumed to be 0.35 wt% because measurements are not available. Based on the BR-3 weld chemical-composition data, the concentration of nickel was assumed to be 0.7 wt%.[a]

"Best estimate" values of RT_{NDT} for the upper axial weld were obtained using *Reg. Guide 1.99*, Rev. 2, with an addition of 44°F in the ORNL analyses to account for a lower irradiation temperature. (*Reg. Guide 1.99*, Rev. 2, is based on an irradiation temperature of 550°F, while the irradiation temperature for the Yankee vessel is ~506°F. The lower temperature results in a greater damage rate, everything else being equal.) As indicated in Appendix C, an irradiation-temperature correction factor of 1°F/1°F is believed to be an appropriate best estimate for the materials, fluences, and temperatures of interest.

Appendices C and D indicate that in the absence of specific data for the Yankee welds the best estimate of the Cu concentration in the welds is 0.29 wt%, and that 1σ = 0.07 wt%. Based on the BR-3[a] data, the best estimate of the Ni concentration is 0.7 wt%. Appendix D also indicates that the best-estimate fast-neutron fluence for the inner surface of the upper axial weld is 1.24×10^{19} n/cm^2. This fluence, with an appropriate attenuation formula and the above chemistry, were used in the above scheme to calculate the increase in RT_{NDT} caused by radiation damage (ΔRT_{NDT}) in the ORNL probabilistic fracture-mechanics analysis of Yankee Rowe (Appendix D). (Since the time that these calculations were performed, an updated set of fluences became available but were not included herein. The most recent values are slightly less than those used in this study.)

5.2 Decrease in the Upper-Shelf Energy

There are two specific concerns with regard to upper-shelf energy. One is whether the vessel satisfies the low-upper-shelf analysis for Levels A, B, and C loading conditions in accordance with criteria recommended by the ASME Section XI Working Group on Flaw Evaluation. The other pertains to the selection of upper-shelf fracture toughness values for the probabilistic fracture mechanics analysis.

Time did not permit a review of the calculated stress-intensity-factor (K_I) values corresponding to load levels A, B, and C; however, the J-R curves used for comparison with the K_I values were reviewed. As indicated in Appendix C, ORNL believes there is adequate margin for each of the loading levels, assuming, of course, that the K_I values are correct.

An appropriate upper-shelf fracture-toughness value for use in the probabilistic fracture-mechanics analysis was estimated by ORNL to be ~140 ksi$\sqrt{\text{in.}}$ for the upper axial weld (Appendix C). The YAEC report used a value of 200 ksi$\sqrt{\text{in.}}$[2], which was also used for the ORNL IPTS studies.[7] ORNL sensitivity studies associated with this review indicate that the effect on the conditional probability of vessel failure [P(F|E)] of the difference between 140 and 200 ksi$\sqrt{\text{in.}}$ is insignificant.

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[a]The BR-3 and Yankee Rowe reactor pressure vessels were fabricated by the same manufacturer, at the same time, and with similar materials.

6 PTS Fracture Mechanics (Appendices D and E)

6.1 Comparison of the PNL and YAEC Versions of VISA-II (Appendix E)

Four categories of differences can be considered: methodology, details, input, and errors. Formal documentation of the Yankee Rowe version of the code was not available for review, and this prevented a comprehensive comparison of basic methodology. A phone conversation, however, revealed no differences in basic methodology, although there were three differences in detail: the YAEC version included (1) residual stresses in the welds, (2) a more accurate representation of *Reg. Guide 1.99*, Rev. 2, and (3) a somewhat different set of K_I values corresponding to pressure loading.

With regard to K_I calculations, it is not clear whether the K_I influence coefficients in the YAEC version correspond to $R/w^a = 7$ (appropriate for Yankee) or $R/w = 10$ (built into the PNL version of VISA-II). A comparison in Appendix E indicates that this difference would not affect initiation and arrest of shallow flaws ($a/w < 0.5$), which are the ones of primary concern.

The input parameters for the Yankee calculations were reviewed item by item for consistency with *Reg. Guide 1.154* and PNL's recommendations for application of VISA-II.[3] While several details of the Yankee inputs differed from those used in prior NRC studies, sensitivity calculations indicate that these differences should not have a major impact on calculated failure probabilities. Of course, inputs for pressures, temperatures, and radiation-induced embrittlement do have very significant impacts. These inputs are discussed in Appendix D.

The ORNL review of the PNL version of VISA-II revealed three errors, and it is assumed that these errors also existed in the YAEC version of VISA-II. These errors are discussed further in Sect. 6.2.

6.2 Comparison of OCA-P and the PNL Version of VISA-II

Prior to this review, the VISA-II and OCA-P codes had not been reviewed in detail since 1984. Because both codes are being used by utilities and others for evaluating vessel integrity, and especially because VISA-II is being used in connection with the Yankee life-extension studies, it was prudent at this time to carefully review both codes and compare one against the other. This effort was intended as a further evaluation of the validity of both codes.

Reviews of both codes were performed by flow-charting, down to a fine level of detail, the probabilistic methodologies, and by making comparison calculations for wall temperatures, stresses, and K_I values. The temperature and stress comparisons, which involved comparison with independent, validated, commercial codes, indicated that the VISA-II and OCA-P subroutines are valid.

OCA-P and VISA-II both use influence coefficient and superposition techniques for calculating K_I values, but the details are different. The OCA-P procedure is more accurate, but the differences normally are not significant and are believed not to be significant for the Yankee analysis performed thus far.

The detailed comparison of the two codes, by means of flow charting, revealed three errors in VISA-II, two of which were almost trivial but one of which results in an excessive number of stable arrests and thus in an underestimate of P(F|E). These errors have been corrected in the PNL version but presumably were not corrected in the YAEC version. One comparison calculation (Appendix D) indicates that correcting the errors increases P(F|E) by a factor of ~10, but for the specific Yankee analysis, the difference is believed to be less.

After the above corrections were made to VISA-II, OCA-P and VISA-II were compared by using both to calculate the "Rancho Seco" transient (Fig. D.1, Appendix D) with $R/w = 10$. "All" input was the same, and only one region of the vessel, containing a single flaw, was considered. The temperature and stress distributions agreed very well, and the K_I values agreed reasonably well, particularly for $a/w^a < 0.5$. The number of initial initiations agreed within 12%, and the values of P(F|E) were within 3%. The number of arrests for OCA-P were three times greater than for VISA-II, because of the difference in K_I values for $a/w > 0.5$, but were a factor of ~10 less than the number of initiations, in which case the difference in arrests has very little effect on P(F|E). Thus, the tentative conclusion is that OCA-P and the corrected PNL version of VISA-II agree well, and both appear valid with respect to what they were intended to do. It is important to remember, however, that there are choices to be made in important input/modeling parameters that can result in significantly different values of P(F|E). Flaw density, its uncertainty, and

[a] Ratio of vessel radius to wall thickness.

[a] Crack depth/wall thickness.

surface length of flaw at arrest are three of the more important choices.

6.3 ORNL OCA-P Analysis of Yankee Rowe (Appendix D)

The ORNL OCA-P analysis of Yankee Rowe used the neutron fluences that correspond to 1990 and the region definitions and volumes given in Ref. 10. P(FIE) was calculated for the upper axial weld only, and the Cu and Ni concentrations for this region (Cu = 0.29, 1σ = 0.07; Ni = 0.7) were best-estimate values taken from Ref. 11. The number of flaws corresponding to a mean value of P(FIE) was essentially the same as that used in the YAEC analysis (one flaw in the region). A uniform tensile stress of 6 ksi was included to simulate a residual stress, and the transient calculated was the SBLOCA-7 transient described in Ref. 2. With reference to Fig. D.9 (Appendix D),

P(FIE) (base case)[a] = 5.0×10^{-4}, and

P(FIE) (w/repressurization)[b] = 1.2×10^{-3}.

Again with reference to Fig. D.9 (Appendix D), these values should be increased by a factor of ~1.7 to include the residual stress. To convert these values from "best estimates" to mean values, they must be multiplied by 45, the ratio of mean to best-estimate flaw density given in Ref. 7. The best-estimate flaw density is 1 flaw/m^3 (Ref. 7), and a flaw density of 45 flaws/m^3 corresponds to ~1 flaw in the Yankee vessel upper weld. [If there were more than 1 flaw in the region calculated, OCA-P might overestimate P(FIE) because of double counting.]

If the same flaw density is assumed for all regions of the vessel, and if an approximate correction is made for double counting, the contribution to P(FIE) of regions other than the upper axial weld will at least double the value obtained for the latter region.

Applying all of the above factors yields

P(FIE) (mean, w/o repressurization)>$5.0 \times 10^{-4} \times 1.7 \times 45 \times 2 = 8 \times 10^{-2}$

P(FIE) (mean, w/repressurization) >$1.2 \times 10^{-3} \times 1.7 \times 45 \times 2 = 2 \times 10^{-1}$.

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[a]Upper axial weld only, no residual stress, 1 flaw/m^3, no repressurization.

[b]Upper axial weld only, no residual stress, 1 flaw/m^3, repressurization to 1550 psi at 20 min.

7 ORNL Estimation of Frequency of Failure for Yankee Rowe

The frequency of failure of the vessel is calculated as follows:

$$\varphi(F) = \sum^{i} [\varphi_i(I) \prod^{j} P_{ij}(B)] P_i(F|E) \ ,$$

where

$\varphi(F)$ = total frequency of failure (failures/reactor yr),

$\varphi_i(I)$ = initiator frequency for i^{th} transient,

$P_{ij}(B)$ = branch probability for j^{th} branch, i^{th} transient, and

$\varphi_i(E)$ = $\varphi_i(I) \prod^{j} P_{ij}(B)$ = frequency of the PTS transient (event).

For the SBLOCA-7 transient, $\varphi(E) = \varphi(I) = 2 \times 10^{-3}$/yr, or 4×10^{-3} if the transient conservatively bounds all other similar LOCAs. As indicated in Appendix A, both values are considered to be reasonable mean values.

Using the lower of the two,

$$\varphi(F)\ (\text{SBLOCA-7}) > 2 \times 10^{-3} \times 8 \times 10^{-2} = 2 \times 10^{-4}/\text{yr}.$$

With repressurization as described above,

$$\varphi(F)(\text{SBLOCA-7R}) > 2 \times 10^{-3} \times 2 \times 10^{-1} = 4 \times 10^{-4}/\text{yr}.$$

These values are substantially greater than the value of 5×10^{-6} failures/yr referred to in *Reg. Guide 1.154* as the "primary acceptance criterion" for the PTS mean frequency of vessel failure.

8 Conclusions

Values of RT_{NDT} calculated for Yankee Rowe in accordance with the rules in 10CFR50.61 for comparison with the PTS-Rule screening criteria are substantially greater than the screening criteria values.

The PNL version of VISA-II and the OCA-P probabilistic fracture-mechanics codes, which are referenced in *Reg. Guide 1.154*, are in good agreement and are valid for their intended purpose. (During this review, an error was found in VISA-II that has subsequently been corrected. The above statement pertains to the corrected version.)

The 1990 mean frequency of failure calculated by ORNL for the Yankee Rowe vessel is $>2 \times 10^{-4}$/yr and thus exceeds the value corresponding to the "primary acceptance criterion" in *Regula-tory Guide 1.154* (5×10^{-6}/reactor yr). As stated in the Reg. Guide, however, this does not necessarily mean that the vessel is unsafe to operate.

There are many unanswered questions regarding details of the YAEC IPTS-type[7] evaluation of vessel integrity. It seems unlikely, however, that answers will substantially alter the above estimated values of $\varphi(F)$.

9 References

1. L. C. Shao, NRC, to W. D. Adams, ORO, "FY 1990 Nuclear Regulatory Research Order No. 60-90-476 for Oak Ridge National Laboratory," August 2, 1990.[a]

2. Yankee Atomic Electric Co., *Reactor Pressure Vessel Evaluation Report for Yankee Nuclear Power Plant*, YAEC No. 1735 (July 1990).[a]

3. F. A. Simonen et al., *VISA-II – A Computer Code for Predicting the Probability of Reactor Pressure Vessel Failure*, NUREG/CR-4486 (PNL-5775), Pacific Northwest Lab., Richland, WA (March 1986).[b]

4. R. D. Cheverton and D. G. Ball, *OCA-P, A Deterministic and Probabilistic Fracture Mechanics Code for Application to Pressure Vessels*, NUREG/CR-3618 (ORNL-5991), Union Carbide Corp., Nuclear Division, Oak Ridge National Lab., Oak Ridge, TN (May 1984).[b]

5. J. A. McFadden et al., *RETRAN-02-A — Program for Transient Thermal-Hydraulic Analysis of Complex Fluid Flow Systems, Volume 1: Theory and Numerics (Revision 4)*, EPRI-NP-1850-CCMA-A, November 1988.[c]

6. C. D. Fletcher et al., *RELAP5 Thermal-Hydraulic Analyses of Pressurized Thermal Shock in Sequences for the H. B. Robinson Unit 2 Pressurized Water Reactor*, NUREG/CR-3977, September 1984.[b]

7. D. L. Selby et al., *Pressurized Thermal Shock Evaluation of the H.B. Robinson Nuclear Power Plant*, NUREG/CR-4183 (ORNL/TM-9567), Martin Marietta Energy Systems, Inc., Oak Ridge National Lab., Oak Ridge, TN (September 1985).[b]

8. *Code of Federal Regulations*, Title 10, Part 50, Section 50.61 and Appendix G.[b]

9. U.S. Nuclear Regulatory Commission, *Regulatory Guide 1.154*, "Format and Content of Plant-Specific Pressurized Thermal Shock Safety Analysis Reports for Presurized Water Reactors".[b]

10. Written communication from Yankee Atomic Electric Company to Mr. William Russell of United States Nuclear Regulatory Commission (September 28, 1990).[a]

11. K. E. Moore and A. S. Heller, *B&W 177-FA Reactor Vessel Beltline Weld Chemistry Study*, BAW-1799, B&W Owner's Group Materials Committee (July 1983).[a]

[a]Available from NRC Public Document Room for a fee.
[b]Available for purchase from GPO Sales Program.
[c]Available for purchase from organization sponsoring publication cited, and/or from authors and/or recipients (documented letters).

[a]Available from NRC Public Document Room for a fee.
[b]Available for purchase from GPO Sales Program.

Appendix A

SAIC Review of YAEC No. 1735

Contents

Appendix A.1

Review of Statistical Issues in Pressurized Thermal Shock Evaluation Report YAEC 1735

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Summary

The following comments are those concerns raised by statistical issues within the subject report. It is recognized that not all of the comments contained herein are of critical importance to the overall conclusions reached in the submittal. However, it is this reviewer's position that statistical treatment of engineering data should always be in accordance with accepted statistical methods.

Overall, the methodology used in the report is an applicable approach to the evaluation of PTS risk. However, several major limitations prevent the report from providing the necessary basis to make a final determination on the validity of the conclusions drawn therein. Principal among these is simply a lack of sufficient detail throughout from which an independent peer review can be accomplished. Examples are the lack of supporting data for correlations; justification for assumptions, such as distributions for fracture mechanics input parameters and the lack of sufficient description of the flaw density distribution chosen. These shortcomings are more specifically enumerated in the individual comments that follow.

A further major lacking with respect to the *NRC Reg. Guide 1.154* is any discussion whatsoever of an effect of uncertainties in data and engineering calculations on the final results, nor any data on the sensitivity of the vessel failure probabilities to these parameters. These two areas are specifically enumerated as being required to be addressed in the Reg. Guide. Further discussion of this point is also made in an individual comment.

Comments

1. Pp. 3-7 and Fig. 3-5- J_{Ic} Correlation with Cv

   Data for both transverse (T-L) and longitudinal (L-T) welds are plotted and fit with a linear regression line vs. Cv in Fig. 3-5. However, the data points are not identified as to which are T-L and which are L-T. Consequently, it is not possible to judge whether a single regression line is appropriate for both sets of data.

   From the fact that the lower "two sigma" limit curve shown is a straight line parallel to the fitted line, it is clear that standard regression techniques were not used to estimate the lower confidence limit for a prediction from the regression line. This is clear since the confidence interval for a linear regression is quadratic.[1]

   The data from this figure were estimated and a regression line and lower 95% confidence level were estimated. The regression line is quite similar to that provided in the figure, indicating that the data were read from the figure accurately. The lower confidence interval obtained from the regression at 35 and 57 ft-lbs are ~90 and 200 in.-lb/in.[2], respectively. These values are 40% and 20% lower than the values used.

   While it appears that this analysis is not of major significance to the overall PTS evaluation, the use of proper statistical methodology throughout an analysis is important.

2. Pp. 5-21, Fig. 5-9 and Appendix B. Arrhenius Relation

   Data for the correlation that are stated to be in Appendix B are not provided. Appendix B references Sect. 5.4.2 instead of 5.3.2. It is not clear what the purpose of the correlation is. If it is to show equivalences of slopes, then the resulting regression slopes and estimated variances in slopes should be reported.

3. Pp. 6-57. Small LOCA Initiating Event Frequency

   Insufficient discussion of the Bayesian update procedure used to estimate the SBLOCA event frequency is presented to allow for adequate review. While the reduction in frequency owing to the update is not really numerically significant (20%), the actual prior distribution used and the update technique should be sufficiently described.

   This comment addresses only the lack of information for the methodology used for this estimate.

Other comments will address the applicability of the data base used to estimate the actual initiating event frequency.

4. Pp. 6-58. Initiating Event Frequencies

Calculation of mean frequencies from the assumed lognormal distributions is correctly done although the mean/median ratio for the error factor of 30 is higher than necessary. This reviewer calculated that correction to be 8.48 rather than 9.06. However, the use of the geometric and/or arithmetic mean presented in many places is not clear. It appears that the mean from the assumed distribution should be used. Clarification of this point is needed.

5. Pp. 6-208. K_{Ic} and K_{Ia} Curves

Discussion of the applicability of these data to the Yankee vessel and the uncertainties inherent in utilizing the "mean" values is required.

6. Pp. 6-208. Flaw Density Distribution

The initial number of flaws in a region of interest directly influences the probability of vessel failure by introducing initiation sites for crack propagation. The initial flaw density in the YNPS analysis is stated to be 1 flaw/m^3, and it is also stated that the number of flaws in the total irradiated weld and plate material is five. This obviously implies a total irradiated material volume of 5 m^3. The analysis then assumes that one flaw exists in each of the (coincidentally) five vessel regions. The second assumption implies that the volume of each region is one cubic meter. Irradiated volumes for each of the five regions are not provided in the report but they have been obtained through MMES.[a] The volumes for the regions and the effective flaw density based on the assignment of one flaw per region are as follows:

Region	Volume (m^3)	Effective Flaw Density (1/region)
Upper Plate	3.51	0.28
Lower Plate	1.30	0.77
Circum. Weld	0.085	11.8
Upper Axial Weld	0.018	55.6
Lower Axial Weld	0.0068	147.0

The widely differing volumes cause a marked bias in the relative importance of the various regions to the overall probability of vessel failure. An overall unbiased estimate of total vessel failure probability is not possible since conditional probabilities for all regions are not provided. However, it is clear that the probability for failure conditional on a single flaw must be at least two orders of magnitude less than that of the axial welds for their contributions to the total vessel failure probability to be equivalent.

The H. B. Robinson analysis utilized the initial flaw-density distribution also quoted as the basis for the YNPS submittal. The interpretation of that reference is significantly different between the two analyses, however. The H. B. Robinson interpretation was that the value of one flaw per cubic meter is the most probable value for the flaw density and that the actual flaw density could be much larger (or smaller) than this. For this reason, a right-truncated lognormal distribution was used therein to describe the initial flaw density. The mean (average) flaw density under that model was ~46/m^{-3}. The particular form of the distribution chosen was not intended to be the only possible interpretation. However, it was intended all available information applicable to a particular vessel be carefully considered in specifying a justifiable flaw density. In view of the essentially linear nature of the vessel failure probability on the initial flaw density, the discrepancy in the stated assumptions and a justification for the limiting flaw density distribution used should be provided. In particular, since from Tables 6.7-5 and 6.7-8 it appears that only the lower plate was considered in the vessel failure probability, demonstration that the other areas are not significant is necessary.

7. Pp. 6-209. Normal Distributions

The truncation of fluence values in the fracture mechanics simulations at the one sigma values seems unjustified. Other truncations for material properties are at least three standard deviations such that the effect of the truncation is not significant.

8. Pp. 6-209. Results of Analysis

The net result of the analysis is presented as being representative of a "mean value" estimate. This estimate may be more accurately classified as a **mean conditional** on the particular values of the thermal-hydraulic boundary conditions and particular input distributions used in the fracture mechanics calculations. The estimation of these parameters is consequently of importance. In the H. B. Robinson analysis and reflected in *Reg. Guide 1.154* it was recognized that uncertainty is inherent in the estimates of the parameters owing to limitations in available data and calculational techniques as well as the effects of other necessary engineering approximations (such as binning of thermal hydraulic transients, for example). The technique recommended therein is an uncertainty analysis of the effect of the significant parameters on the estimation of the overall frequency of failure. This was accomplished for the H. B. Robinson analysis by use of a Monte Carlo simulation, and the technique

[a] R. D. Cheverton, Oak Ridge National Laboratory, personal communication.

is recommended in *Reg. Guide 1.154* for several reasons; most significant of which are the complexity of the analysis and the extreme nonlinearity of the fracture mechanics model results to variations in input values.

Estimation of system performance for nonlinear problems is known to be biased by exclusion of the nonlinear terms.[2] In small systems, systems in which the nonlinearity is not extreme, or, if the uncertainties in parameters are small, the effect of the nonlinearities may be negligible. For a PTS analysis, none of the above conditions are met. In the H. B. Robinson analysis, the net effect was to raise the estimate of the mean by a factor of ~250 owing to this effect and the inclusion of the previously mentioned mean flaw density of 45 m^{-3}. The inclusion of uncertainties for estimates of the means of the significant fracture mechanics variables, neutron fluence, and thermal hydraulic bounding conditions contributed a factor of roughly five out of that total for the distributions used therein. A smaller factor is due to the combination of event tree sequence frequencies and branch probabilities, but the major effect is due to the nonlinearity of the fracture mechanics results. Justification for not including uncertainties in these significant parameters for this analysis is necessary or an estimation of this effect on the presented results is required.

References

1. N. R. Draper and H. Smith, *Applied Regression Analysis*, John Wiley & Sons, Inc., New York, 1966, p. 22-24.[a]

2. G. H. Hahn and S. S. Shapiro, *Statistical Models in Engineering*, John Wiley & Sons, Inc., New York, 1967, p. 228ff.[a]

[a]Available from public and special technical libraries.

[a]Available from public and special technical libraries.

Appendix A.2

Thermal-Hydraulic Behavior in Small-Break LOCAs of Significance to Pressurized Thermal Shock (PTS) with Consideration of the Yankee Rowe Nuclear Power Station

SAIC Report No. 91-6501 (Final)

Executive Summary

General consideration has been given to small-break LOCA thermal-hydraulics of significance to PTS, with emphasis on the potential to proceed to stagnation of primary loop natural circulation flow. Injection of cold (~120°F) safety makeup water into a "stagnant" downcomer region will produce a rapid, perhaps severe, cooling of the pressure vessel wall. An independent analytical procedure was developed to quantify the transient thermal response of the downcomer mixed-mean fluid temperature and wall heat transfer in a manner similar to the REMIX code. Calculations specific to the Yankee Nuclear Power Station plant were performed to evaluate the YAEC submittal using the REMIX code. The following conclusions and recommendations can be made based upon the present independent PTS thermal-hydraulic evaluations.

- Small-break (1-1/2 to 3 in.) LOCA transients should be expected to proceed to primary coolant flow stagnation. YAEC has correctly considered such a limiting scenario for the Yankee plant. A flow stagnation scenario should be considered for all PTS evaluations, including the H. B. Robinson and Calvert Cliffs plants; it appears that this may not have been adequately addressed for these plants.

- Analyses conducted with the present independent methodology and a review of the REMIX code manual indicates that REMIX has been applied by YAEC as the code developers intended. The current analysis is in excellent agreement with the YAEC REMIX calculation. It was concluded that this transient should be considered as a best-estimate result owing to the extensive REMIX assessment basis. However, it was also concluded that any significantly less severe cooldown is unlikely for this scenario with primary loop flow stagnation. Thus, the REMIX mixed-mean downcomer fluid temperature is an upper-bound but represents the best-estimate for expected behavior. While the report YAEC-1735 did not provide complete thermal-hydraulic details, these have subsequently been provided by letter to the USNRC and considered in the present evaluations in this final report (but not in previous draft versions).

- Yankee plant-specific design features could be important to the fluid mixing process, especially to the fluid behavior adjacent to the pressure vessel wall. The appropriateness of REMIX assumptions for the YNPS geometry may need to be further considered.

Preface

This report was previously distributed twice in draft form, including Rev. 1. These two versions considered only the limited information contained in the report YAEC-1735 on the SBLOCA thermal-hydraulics. In June and July 1991, Yankee Atomic provided complete details of their REMIX evaluation (included as Attachments A and B of this report). The new YAEC information pertained to primary coolant system geometry, initial plant conditions at flow stagnation, boundary conditions on safety injection flow, and REMIX details such as downcomer mixed-mean fluid temperature.

This new information necessitated re-analysis by SAIC as well as minor modifications of the conclusions and recommendations. This FINAL version is being provided after consideration of this complete information.

1 Introduction and Background

Extensive safety assessment research, both experimental and analytical, was conducted during the past decade on the pressurized thermal shock (PTS) issue. This work resulted in rule making, 10CFR50.61, "Fracture toughness requirements for protection against pressurized thermal shock." The U.S. Nuclear Regulatory Commission (NRC) has received a submittal under this rule for the Yankee Nuclear Power Station (YNPS), "Pressure Vessel Evaluation Report," Yankee Atomic Electric Company (YAEC) Report No. 1735, July 1990.[1] Their evaluation considered the individual "PTS risk" from a spectrum of hypothetical accident initiators and concluded that the dominant event is a small-break loss-of-coolant accident (SBLOCA) (~1-1/2-in. diameter break). The SBLOCA sequence being risk significant is in agreement with conclusions for the NRC's assessment of similar "baseline plants," H. B. Robinson and Calvert Cliffs, which previously underwent significant evaluation. SBLOCAs tend to be risk dominant because of the potential for severe (rapid) temperature cooldown of vessel materials while at significant pressure. This situation occurs due to the fact that for a range of small (~ 1 to 3 in.) breaks, primary coolant loop flow stagnation can occur at significant pressure (~ 800 psi) accompanied by an extended period of safety injection (SI) of cold (~ 120°F) water into the downcomer region. The YAEC predicted downcomer pressure and fluid temperature are shown by Figs. A.1 and A.2, respectively. The downcomer cooldown rate (0.4 to 1.3°F/s) for Yankee is significantly greater than that considered "prototypical" from the H. B. Robinson and Calvert Cliffs baseline PTS studies.[2,3] This large cooldown rate, in concert with YNPS materials and neutron fluence, has raised concerns over a large through-the-wall-crack probability for this SBLOCA scenario. There is additional concern over the specific YNPS temperature history of Fig. A.2, arising due to the calculated dichotomy in downcomer cooling rates before and after 200 s--reduction in cooldown by a factor of three. The transient results of these figures required a switch from the system simulation (PETRAN) to the loop-downcomer empirical model (REMIX) at 150 s.

2 Scope and Objectives

In order to independently evaluate the calculated behavior in the YNPS, as well as to qualitatively consider differences between H.B. Robinson and Calvert Cliffs behavior from Yankee, the present work has been performed; initial consideration was provided in "Review of YAEC-1735, Reactor Pressure Vessel Evaluation Report."[4] The scope of the present work is to provide a qualitative description of SBLOCA thermal hydraulics behavior including controlling phenomena and then to provide quantitative comparisons on cooldown potential in YNPS relative to the earlier baseline plant studies. This work draws from insights gained from previous evaluations[2,3,5-9] as well as performs new, independent calculations for YNPS to help explain plant-specific behavior and to clarify expected deviation from those earlier studies.

The overall objectives are to provide a narrative describing generic thermal-hydraulic behavior in SBLOCAs of PTS significance and then to provide "audit calculations" for Yankee as well as comparisons with H. B. Robinson and Calvert Cliffs. There are three specific objectives addressed in the following sections. First, to provide a narrative of qualitative thermal-hydraulic transient behavior leading to flow stagnation and to identify plant-specific parameters pertinent to cooldown behavior. Second, to quantify the cooldown potential of Yankee relative to H. B. Robinson and Calvert Cliffs, including a formulation of bounding downcomer cooldown behavior. Third, to evaluate the Yankee behavior during the stagnant loop flow regime when fluid-fluid mixing dominates the thermal response. It is the intent of the author to provide a review useful to those with limited thermal-hydraulics background to help them comprehend generic plant response and to provide plant-specific perspectives to aid in evaluation of this first "PTS plant submittal" to the NRC.

3 Generic Thermal-Hydraulic Behavior in SBLOCA and Controlling Phenomena

Extensive reactor systems analyses with modern thermal-hydraulic computer codes, TRAC[3] and RELAP5,[8] have identified small-break LOCAs as important scenarios with respect to pressurized thermal shock.[2,3] These thermal-hydraulic analyses revealed that there may be special concern for SBLOCAs which result in stagnation of the primary coolant flow while at significant pressure. Such a scenario could result in severe overcooling and pressurized transients owing to sustained periods of cold (120°F) safety injection water into downcomer water that has been isolated from core and steam generator (reverse) heat sources. The following narrative presents a description for "generic" SBLOCA transients with particular attention on controlling phenomena. This is intended to provide the reader with a qualitative "picture" of transient thermal-hydraulic behavior in such a risk-dominant PTS scenario.

Pressurized water reactors are designed to ensure that core heat removal capability is maintained in the event that pumping capacity is lost, that is, to ensure natural circulation of the primary coolant. Driving forces that sustain the natural circulation are differential pressure "heads" arising from the cooling of water in steam generators located above hotter water in the core. The density differences and elevation changes can drive a significant flow of primary coolant water. In consideration of PTS scenarios, this natural circulation has a two-fold beneficial effect on mitigating the overcooling transient. First, the circulating water maintains the vessel wall with heat from the core as well as "reverse" heat transfer from the steam generator secondaries. The second effect is to promote mixing of the cold safety injection water with the entire primary system water mass. Thus, natural circulation can greatly mitigate overcooling. If natural circulation is interrupted, the stagnate configuration loses these two beneficial effects and significantly more severe overcooling will result.

An interruption of natural circulation will occur if there is a "break" in coolant fluid stream continuity, i.e., a void region forms and interrupts the siphon effect. It is possible for a "void" (steam) to form after the blowdown from a small-break LOCA; this void normally accumulates in the highest region of the system, for example, the U-tubes of the steam generators. The primary coolant circulation will remain stagnated, thereby setting the stage for an overcooling transient unless this steam void is collapsed by condensation or system repressurization.

For a SBLOCA scenario to be of extreme PTS significance then, there must be both a flow stagnation and a significant primary system pressure. If the break size is small, the system will remain pressurized, but steam voiding will not occur and neither will flow stagnation. If the break size is large, flow stagnation will occur but be accompanied by depressurization to low pressure. Thus, there is a spectrum of break sizes with a lower and upper limit that may be expected to envelope SBLOCAs of special PTS significance. A simple procedure has been developed by this author and Professor Theofanous[7] to determine the minimum break size that can produce primary loop flow stagnation on a plant-specific basis. This "mapping" is possible after realizing that interruption of natural circulation occurs due to a break in the primary circuit's liquid continuity. This will occur if the primary system sustains a loss of liquid arising from the break flow exceeding the water inflow from the safety injection pumps. Since both of these boundary flows depend on primary pressure, additional consideration must be given to the transient thermal-hydraulic behavior for the small-break LOCA.

An overall description is now presented for the system transient thermal-hydraulic response for a SBLOCA with a break size that leads to stagnation while at significant pressure. The scenario of particular significance for PTS is an accident initiation while the plant is at so-called "hot zero power" with a break size of typically 1-1/2- to 3-in. diameter. The primary system pressure will typically fall rapidly but then stabilize for an extended period; the behavior for Yankee shown in Fig. A.1 is a typical response. This shows the expected SBLOCA "pressure signature"; an initial pressure of over 2000 psia with a decrease to 700 to 900 psia over 3 to 6 min. The important feature is that the pressure "holds" at a significant value for an extended period. This is a consequence of primary water flashing at its saturation temperature while being augmented by reverse heat transfer from the steam generator secondary side to the primary water. This heat transfer maintains the stagnant fluid in the steam generator primaries at the saturation temperature and therefore maintains the pressure via boiling. The ensuing steam will then form a "void" region at the top of the system (U-tubes) and interrupt natural circulation. This pressure plateau value can easily be computed on a plant-specific basis; it is simply the saturation pressure corresponding to the liquid temperature of the steam generator secondary (shell side). For PWRs, this is typically in the range of 800 to 1000 psia while at hot zero power. Knowing this plateau pressure will then allow one to compute a plant-specific minimum break size in a SBLOCA that will cause flow stagnation. The break outflow (Q_{Break}) can be approximated by

$$Q_{Break} = h_{fg}\left(\frac{1}{TC_{pl}}\right)^{1/2} A_{Break,\,min}\,, \qquad \text{(A.1)}$$

where h_{fg} is the latent heat of evaporation, T is the water temperature, C_{pl} is the specific heat, and A is the minimum break area. The primary system liquid volumetric inflow can be computed from the plant-specific high-pressure-injection (HPI) head-flow curves evaluated at this pressure. This will allow for a calculation of the minimum break area in Eq. (A.1) that results in a primary system net liquid volume loss, and ultimately flow stagnation.

Downcomer fluid temperature response is directly controlled by the primary systems' coolant flow behavior. Prior to primary depressurization and subsequent boiling, natural circulation with heat sources tends to mitigate cooldown from injection of the cold HPI water. However, almost immediately after flow stagnation occurs, the downcomer fluid temperature begins a rapid decrease. The YNPS behavior of Fig. A.2 is qualitatively representative of this effect, that is, minimal cooling early on, and then rapid cooling after stagnation. Indeed, this figure shows very rapid (-1.5°F/s) cooling immediately after stagnation and then reduced cooling due to wall heat transfer and warm fluid mixing. As will be discussed in the following sections, the YNPS cooldown is greater than that of the "baseline" Calvert Cliffs and H. B. Robinson behavior.

The long-term SBLOCA thermal-hydraulic behavior is controlled by hot and cold fluid-fluid mixing with the absence of bulk loop circulation. Figure A.3 (taken from Ref. 8) conceptually illustrates the flow behavior in the downcomer and cold leg regions. There are three key phenomena of importance to the thermal-hydraulic cooldown behavior during this stagnation period. The downcomer cooldown is essentially controlled by the inflow of a cold stream from the loops and mixing within the downcomer region with perhaps strong coupling to warmer water in the loops and lower plenum. The first phenomenon is the "flow splitting" of injected HPI water; some fraction of the cold injection water may flow away from the vessel mitigating the downcomer cooldown. The second phenomenon is an entrained, backflow of relatively warmer water from the downcomer region into the top of the cold leg. This effectively warms the inflowing water to the downcomer, also mitigating the cooldown. The third, and by far the most important phenomenon, is fluid-fluid mixing between the downcomer flows and the lower plenum water.

In summary, this section has provided a generic description of thermal-hydraulic behavior for a limiting SBLOCA. Indeed, it has shown that for a specific range of break sizes, flow stagnation can occur at pressure producing a severe overcooling. The transient proceeds with a rapid blowdown to a pressure plateau controlled by reverse heat transfer from the steam generator secondaries' liquid. The minimum break area that produces this behavior on a plant-specific basis can easily be estimated based on a balance between break outflow and safety injection inflow. During this early period, there is significant natural circulation loop flow mitigating downcomer cooldown. After flow stagnation occurs, the cooldown can become severe due to loss of both heat sources and the bulk convective mixing with the entire primary water mass. The stagnant downcomer cooldown rate is controlled by three phenomena of fluid-fluid mixing. However, the cooldown during this long-term stagnant regime can be bounded through a simple energy balance, as shown in the following section.

4 Parametric Evaluations of Downcomer Fluid and Vessel Wall Thermal Transient Behavior

Evaluations of the pressure vessel wall fracture mechanics are closely coupled to the transient thermal-hydraulic behavior in the downcomer. In particular, it is necessary to determine the pressure vessel wall temperature response to the boundary conditions of the surface heat transfer coefficient and fluid temperature. During the early period of natural circulation within the primary coolant system, RELAP5, RETRAN, or TRAC "systems codes" are traditionally employed. However, once loop stagnation occurs, these codes are inappropriate due to the inability to correctly represent "stagnant" mixing of cold and warm water regions; the flow behavior is dominated by complex turbulent mixing driven by buoyancy rather than momentum effects. This flow behavior has been the subject of extensive experimental and analytical studies. As a result of these studies, the REMIX computer code has been developed to evaluate downcomer response to safety injection of cold water into a "stagnant" system.[9] Yankee Atomic employed REMIX to quantify the YNPS behavior, as shown by Fig. A.2. In order to provide an independent calculational audit of these results, the present analyses have been conducted. The following sections outline the methods, parametric studies, and Yankee evaluation with the present model.

4.1 Methodology

The present study focused on parametric evaluations of the mixed-mean fluid temperature in the vessel downcomer region. The results are then expected to be comparable with the corresponding REMIX value, T_m. The present work did not attempt to predict REMIX-simulated safety injection backflow and detailed mixing with various fluid regimes, but rather treated these phenomena parametrically to obtain an enveloping transient response. Furthermore, the current model does not address the pluming effect (treated by REMIX) that calculates colder temperatures in the plume below the cold leg penetrations; that is, mixing region 4 of Fig. A.3.

4.1.1 Fluid Thermal Energy Balance

The "mixing cup" temperature (T_{MIX}) with a fluid region (control volume) due to instantaneous mixing of an incoming (colder) fluid stream of flow rate $\dot{m}_{HPI}$ and temperature T_{HPI} is given by

$$M\frac{dT_{MIX}}{dt} = \dot{m}_{HPI}\left(T_{HPI} - T_{MIX}\right) + \frac{\dot{Q}_{wall}}{C_{pl}}, \qquad (A.2)$$

where M is the mass of fluid in the mixing region and $\dot{Q}_{wall}$ is the wall heat transfer. If wall heat transfer is ignored, that is adiabatic, this equation can be integrated to give

$$\frac{T_{mix} - T_{HPI}}{T_{init} - T_{HPI}} = e^{-\dot{m}\frac{t}{M}}, \qquad (A.3)$$

where T_{init} is the initial fluid temperature. However, to realistically evaluate reactor behavior for PTS scenarios, the wall heat transfer must be evaluated and Eq. (A.1) integrated numerically, using to appropriate initial and boundary conditions.

4.1.2 Wall Heat Transfer

Quantification of the temporal response of $\dot{Q}_{wall}$ requires calculation of the vessel wall heat diffusion behavior as well as the surface heat transfer coefficient. For the present work, the wall heat transfer has been evaluated by solving a one-dimensional, finite-difference model subjected to a uniform initial temperature, an adiabatic boundary condition at the exterior surface, and a known (transient) internal heat flux boundary condition at the internal downcomer fluid face (i.e., $hA\Delta T$).

The above model of coupled wall and fluid transient thermal response has been numerically implemented into a small PC computer program. This program computes a mixed-mean downcomer fluid temperature subjected to input boundary and initial conditions. The code has been used to parametrically evaluate the influence on fluid and wall temperature transient of controlling parameters, including

- fluid mass participating in the "stagnant" mixing problem, such as the cold legs, inlet annulus, downcomer, and/or lower plenum regions;
- safety injection flow rate into the downcomer; and
- wall heat transfer coefficients.

4.2 Parametric Evaluations

This section presents the results of parameter analyses using the above transient thermal model with conditions similar to those of the Yankee plant. Table A.1 lists the parameters selected for the parametric evaluation. Specific YNPS results are given in Sect. 5.

4.2.1 Heat Transfer Coefficient

Figure A.4 shows the calculated natural convection heat transfer coefficient as a function of temperature difference (ΔT) between the wall and the fluid. As shown, the value also depends on the water temperature (transport properties). For the practical range of plant condition during the cooldown, the heat transfer coefficient varies between about 100 and 500 Btu/h-ft^2-°F. Higher coefficients will tend to mitigate the downcomer water cooldown; however, this represents the most severe thermal shock to the pressure vessel wall and thus is "conservative" from a safety perspective. Figure A.5 illustrates the parametric effect of wall heat transfer coefficient on temperature of the mixed-mean downcomer water. This calculation used the "baseline values" of Table A.1. It can be concluded that for wall heat transfer coefficients greater than 400 Btu/h-ft^2-°F, a "conduction limited" process is governing. That is, the wall surface is in thermal equilibrium with the fluid and heat transfer is limited by heat diffusion from the vessel wall material itself. A value of 400 Btu/h-ft^2-°F was selected for the NRC's H. B. Robinson PTS evaluation[2] and is used as the present baseline in subsequent calculations.

4.2.2 Safety Injection Flow Rate

Flow distribution of safety injection [high pressure injection (HPI)] water in the cold legs is qualitatively illustrated by Fig. A.3. Flow behavior is controlled by buoyancy effects, that is, by Froude number similarity criteria. It is likely that some fraction of the HPI water will "backflow" away from the vessel, tending to mitigate the cooldown, at least during the initial rapid cooling regime. REMIX has an empirical model that determines the backflow fraction (and corresponding "entrainment" of warmer water). Figure A.6 illustrates the parametric effect of reducing inflow rate to the "fluid mixing volume." It can be concluded that the cooling is significantly reduced only if at least one-half of the HPI water flows away from the vessel.

4.2.3 Mixing Water Volume

The present analysis for the mixed-mean fluid temperature (as well as REMIX) assumes that a single (large) volume participates in the hot-cold fluid mixing process. Figure A.7 illustrates the parametric effect of varying the mixing volume assuming baseline parameters for other variables. For the Yankee plant, the included volumes represent the following regions of the primary system:

200 ft^3	-	Inlet annulus below top of cold legs and downcomer region
333 ft^3	-	Above regions plus cold legs between injection point and vessel
800 ft^3	-	Above regions plus lower plenum.

Figure A.7 demonstrates that the mixed-mean fluid temperature is strongly dependent on the assumed fluid regions participating in the mixing process. For the Yankee plant, it quantifies the substantially mitigating effect of including the lower plenum volume (467 ft^3) in the mixing process.

5 Yankee Nuclear Power Station Audit Calculation

The analytical model discussed in the previous section has been used to formulate a Yankee Nuclear Power Station plant-specific evaluation. The objective was to evaluate the reasonableness of the YAEC REMIX results using the independent calculational tool of the previous chapter. Specifically, this effort was to evaluate the YAEC downcomer fluid mixed-mean temperature (i.e., that corresponding with T_m of REMIX). As noted in the preface, detailed information on the REMIX model, input, and calculational results were provided by YAEC in a letter report June 26, 1991 (Attachment A) and through a telefax on July 5, 1991 (Attachment B). This information and teleconferences with YAEC and NRC staff have greatly clarified their assumptions and results for the SBLOCA scenario. Based upon this information, the following is now known:

a) Primary coolant flow stagnation was calculated (by RETRAN) to occur at 150 s with the system downcomer water at 476°F. The REMIX calculation began at this time.

b) The downcomer fluid temperature presented in YAEC-1735 is the REMIX value T_{jump} (a plume temperature) not the warmer mixed-mean temperature, which has now also been provided (Curve 2 of the YAEC 6/26/91 Letter, Attachment A).

c) REMIX values for Yankee geometry, materials, initial conditions, and detailed output are now available (see Attachment B).

SAIC's computer code that calculates the mixed-mean downcomer fluid temperature was used with consideration of this new REMIX information. The following changes were made from the previous calculations:

a) The system metal mass was expanded to also include the lower plenum region and the (double-sided) thermal shield.

b) Metal thermal conductivities and diffusivities were based upon the REMIX YAEC values.

c) Mixing volumes were compared to YAEC values and found to be in nearly exact agreement with that used in the previous analysis; however, the total SI flow is now injected into the total REMIX fluid volume.

d) The YAEC REMIX model includes the flow of heat from the core region; this is not included in the SAIC model thereby producing a slightly greater cooldown.

e) The transient was initiated at 150 s reactor time with the fluid at 476°F.

f) Safety injection flow was at 120°F (baseline) and parametrically evaluated at 170°F.

g) Comparisons were made with YAEC REMIX T_m values; the mixed-mean fluid temperature was taken from the YAEC output listing.

SAIC's new results are compared with the YAEC value in Fig. A.8. Excellent agreement exists between the YAEC REMIX results and the SAIC simplified model. The deviation at 1200 s is less than 20°F and likely occurs due to YAEC's correct inclusion of heat flow from the core region.

The effect of preheating the safety injection water to 170°F is quantified in Fig. A.9 by comparison to the YAEC REMIX at 120°F.

Consideration of the YAEC results and the present independent analysis leads to the following conclusions. The early time period (0-150 s) cooldown is realistic and consistent with expected fluid behavior during the transition to primary coolant system bulk flow stagnation. The dramatic decrease in cooldown at 200 s, shown in the report YAEC-1735, is due to inaccurate plotting of REMIX results. The long-term cooldown under stagnant conditions (after 150 s) has been correctly simulated by YAEC with the REMIX code. REMIX has been shown to be in very good agreement with a wide range of experimental data in an extensive assessment project.[10] The YAEC results (Fig. A.8) must therefore be considered as best-estimate results for the mixed-mean downcomer fluid temperature. Fluid temperatures below the cold legs (plume region) are lower than these values; the YAEC REMIX values for this region are those given in report YAEC-1735 (also Fig. A.2 of this report). There is no reason to expect that any less severe downcomer fluid temperature transient could occur in this SBLOCA scenario with the given initial and boundary conditions (e.g., tripped main coolant pumps). The only known omission of a heat source from the REMIX calculations is from hot water in the barrel baffle region. Thus, it is concluded that YAEC (REMIX) calculated downcomer fluid temperatures are both a best-estimate and likely an upper bound value for anticipated thermal-hydraulic behavior. This result being simultaneously an upper bound and a best-estimate needs clarification. It is best-estimate owing to the validity of REMIX per the extensive assessment basis.[10] It is an upper bound in that there are only minimal thermal or fluid phenomenon that would lead to any less severe cooldown for this transient. Indeed, this is the essence of the

REMIX phenomenological assumptions and it is well validated through comparisons to extensive experimental data.

Finally, it is noted that certain Yankee plant-specific design features may serve to influence the fluid-fluid mixing process relative to that expected of larger plants, e.g., Calvert Cliffs. These unique features are revealed by the pressure vessel vertical cross section of Fig. A.10. For the Calvert Cliffs plant, the downcomer gap is roughly 10 in., while for the Yankee plant it is about 3 in. This much narrower gap may reduce lower plenum water mixing and influence thermal-hydraulic behavior in the downcomer. Another concern is the influence of the Yankee geometric details in the downcomer inlet annulus region. As shown by Fig. A.10, the upper core support barrel has an outer diameter significantly smaller than the downcomer region. This Yankee feature could also affect the downflow of colder water in the downcomer and the vessel wall cooldown.

6 Comparisons Between Yankee, H. B. Robinson, and Calvert Cliffs

This section will provide quantitative comparisons between Yankee, H. B. Robinson, and Calvert Cliffs of the downcomer cooldown rates shortly after flow stagnation. As discussed previously, the most severe cooling transient is expected to occur in a SBLOCA scenario leading to complete stagnation. This is postulated to occur when the break liquid outflow exceeds the safety injection inflow. The minimum break area and diameter for this condition have been computed for the Yankee, H. B. Robinson, and Calvert Cliffs plants using Eq. (A.1). The comparison is made in Table A.2; note that all three plants have a similar break size of about 1-1/2 in. This is because all three plants have similar safety injection (HPI) capacity and would have similar break outflows, near 100 lb_m/s or 1.5 ft^3/s. Breaks of this size and greater would result in primary liquid levels dropping, and subsequent flow stagnation. However, if the break is too large (≥4 in.), then depressurization will likely occur before a severe cooldown can occur.

In order to provide a useful, albeit incomplete, perspective on the relative PTS cooldown potential between Yankee, H. B. Robinson, and Calvert Cliffs, the initial cooldown rates are computed. The largest rate of cooling will occur shortly after flow stagnation, that is, before large volumetric mixing and wall heat transfer become important. The mixed-mean fluid temperature is then given by Eq. (A.3). We can compute the initial cooling rate to be the time derivative of this equation, namely

$$\frac{dT_{mix}}{dt} = -(T_{mix} - T_{HPI})\frac{\dot{m}_{HPI}}{M} \quad . \qquad (A.4)$$

If the total fluid mixing volume is based upon that of the cold legs, inlet annulus, and downcomer, the initial cooling rates for the three plants are given in Table A.2. The geometric plant values are only approximate with the intended purpose to illustrate the relative cooldown potential between plants. Further-more, the hypothetical calculation assumes all plants stagnate at 480°F primary circuit water temperature. All three plants have nearly the same HPI capacity, about 1.5 ft^3/s at ~130°F. However, the volume of Yankee (as illustrated by the downcomer volume) is significantly smaller than the other two plants used in the NRC's PTS study. Thus, the initial cooling rate in Yankee is 1.2°F/s, in H. B. Robinson 0.6°F/s, and in Calvert Cliffs, 0.2°F/s. This serves to qualitatively illustrate that Yankee should be expected to undergo a more severe thermal shock than the other two plants. However, it is not clear that the PTS evaluations for H. B. Robinson[2] or Calvert Cliffs[3] used thermal transients as severe as would occur under flow stagnation. Insufficient time has been available to thoroughly address the extensive thermal-hydraulic results contained in Refs. 2 and 3.

7 Summary and Conclusions

General consideration has been given to small-break LOCA thermal-hydraulics of significance to PTS, with emphasis on the potential to proceed to stagnation of primary loop natural circulation flow. For break sizes in the range of 1-1/2 to 3 in. in diameter, liquid outflow will typically exceed the safety injection capacity (at 700 psi) producing a "break" in liquid continuity and thus interrupt natural circulation. Injection of cold (120°F) safety makeup water into a "stagnant" downcomer region will produce a rapid, perhaps severe, cooling of the pressure vessel wall. An independent analytical procedure was developed to quantify the transient thermal response of the downcomer fluid temperature and wall heat transfer. This model predicts a mixed-mean downcomer fluid temperature in a manner similar to the REMIX code. This model was used to parametrically evaluate the influence on fluid temperature of variations in wall heat transfer, safety injection flowrates, and water volumes participating in the mixing process. Calculations specific to the Yankee Nuclear Power Station plant were performed to evaluate the YAEC submittal using the REMIX code. Consideration was then given to the potential for both less and more severe cooldown transients. The following conclusions and recommendations can be made based upon the present independent PTS thermal-hydraulic evaluations.

a) Small-break (1-1/2 to 3 in.) LOCA transients should be expected to proceed to primary coolant flow stagnation. YAEC has correctly considered such a limiting scenario for the Yankee plant. However, the sequence frequency analysis should only consider small break initiators in this range; that is, smaller and larger breaks will have either a much less severe downcomer fluid cooldown or will depressurize to a low pressure. A flow stagnation scenario should be considered for all PTS evaluations, including the H. B. Robinson and Calvert Cliffs plants; it appears that this may not have been adequately addressed based upon cursory review of Refs. 2 and 3.

b) After flow stagnation occurs (3-6 min), the initial downcomer fluid cooling can be very severe, over 1.5°F/s. However, the cooldown will soon be moderated due to mixing with hot water in the cold legs, inlet annulus, downcomer, and lower plenum. The specific cooldown behavior is controlled by fluid-fluid mixing phenomena in these primary system regions. REMIX has been shown to have remarkable predictive capability for such PTS behavior.[10]

c) Yankee Atomic Electric Company has employed the REMIX code to determine the transient thermal-hydraulic behavior in the downcomer region. SAIC has developed an independent tool to evaluate the mixed-mean downcomer fluid temperature. Comparison between the two codes show excellent agreement. It is concluded that YAEC has correctly applied REMIX to evaluate the SBLOCA sequence. Hence, the results provided by YAEC should be considered best-estimate values. The mixed-mean fluid temperature of Fig. A.8 should be considered as both best-estimate and as an upper-bound. However, the original downcomer fluid thermal response of YAEC-1735 (Fig. A.2) represents the colder plume region below the cold legs.

d) The Yankee plant has design features and operational characteristics that are unique. These include a "recessed" upper barrel, a narrow downcomer/thermal shield region, and a large safety injection flow for a "small" plant. The potential impact, if any, on REMIX hydrodynamic and phenomenological modeling and assumptions should be addressed. However, the REMIX code has been well assessed and must be considered as representing best-estimate behavior.

8 Recommendations

YAEC has provided complete details from the SBLOCA REMIX calculation, including plume temperatures in the downcomer regions below the cold legs. The fracture mechanics technical experts should ensure that appropriate fluid temperatures have been used in their evaluations.

Thermal-hydraulic SBLOCA transients used in the PTS studies on the H. B. Robinson and Calvert Cliffs plants need to be reviewed to ensure that adequate consideration was given to flow stagnation scenarios.

A possible need exists for research on the influence of plant-specific features on the PTS thermal-hydraulic behavior and the inherent assumptions of REMIX. Specifically, (older) plants with narrow downcomer gaps and large HPI flows could pose unique considerations not covered in the assessment basis of REMIX.

9 References

1. *Reactor Pressure Vessel Evaluation Report*, Yankee Atomic Electric Company Report No. 1735, July 1990.[a]

2. *Pressurized Thermal Shock Evaluation of the H. B. Robinson Unit 2 Nuclear Power Plant*, NUREG/CR-4183. March 1985.[b]

3. A. D. Spriggs et al., *TRAC-PF1 Analysis of Potential Pressurized Thermal Shock Transients at Calvert Cliffs, Unit 1*, NUREG/CR-4109, February 1985.[b]

4. D. P. Bozarth, K. A. Williams, and J. W. Minarick, "Review of YAEC-1735, Reactor Pressure Vessel Evaluation Report," SAIC letter to R. D. Cheverton, Oak Ridge National Laboratory, November 2, 1990.[c]

5. K. Iyer and T. G. Theofanous, "Decay of Buoyancy-Driven Stratified Layers with Applications to Pressurized Thermal Shock: Reactor Predictions," *Proceedings of 1985 NHTC, Aug. 4-7, 1985, Denver, CO*, pp. 358-371 (also NUREG/CR-3700, May 1984).[b]

6. K. Iyer and T. G. Theofanous, "Flooding-Limited Thermal Mixing: The Case of High-Fr Injection," *Third Int. Conf. on Reactor Thermal Hydraulics*, Newport, RI, Oct. 15-18, 1985, Paper 13E, Nuclear Science and Engineer-ing, V108, Issue No. 2, pp. 198-207, June 1991.[a]

7. T. G. Theofanous, J. L. LaChance, and K. A. Williams, *The Thermal Hydraulics of SBLOCAs Relative to Pressurized Thermal Shock*, NUREG/CR-5135, October 1988.[b]

8. C. D. Fletcher et al., *RELAP5 Thermal-Hydraulic Analyses of Pressurized Thermal Shock in Sequences for the H. B. Robinson Unit 2 Pressurized Water Reactor*, NUREG/CR-3977, September 1984.[b]

9. K. Iyer, H. P. Nourbakhsh, and T. G. Theofanous, *REMIX: A Computer Program for Temperature Transients Due to High Pressure Injection After Interruption of Natural Circulation*, NUREG/CR-3701, May 1986.[b]

10. T. G. Theofanous and H. Yan, *A Unified Interpretation of One-Fifth to Full-Scale Thermal Mixing Experiments Related to Pressurized Thermal Shock*, NUREG/CR-5677, February 1991.[b]

[a]Available from NRC Public Document Room for a fee.
[b]Available for purchase from GPO Sales Program.
[c]Available for purchase from organization sponsoring publication cited, and/or from authors and/or recipients (documented letters).

[a]Available from public and special technical libraries.
[b]Available for purchase from GPO Sales Program.

Table A.1 Thermal and Hydraulic Parameters Used for the Parametric Evaluations

High Pressure Injection Temperature, °F	130
High Pressure Injection Flow, lb/s	45, 67, 90*
Initial Wall Temperature at Flow Stagnation, °F	480
Pressure Vessel Wall Area, ft^2	460
Downcomer Water Temperature at Stagnation, °F	400
Fluid Volumes Participating in "Stagnant" Mixing, ft^3	200, 333, 800*
Wall Heat Transfer Coefficient, Btu/h·ft^2·°F	0, 400*, 105

*Best estimate at time of study.

Table A.2 PTS Comparison Betw..n YNPS, H. B. Robinson, and Calvert Cliffs PWRs

	Plant		
	Yankee Rowe	H. B. Robinson	Calvert Cliffs
PLANT PARAMETERS*			
Hot Zero Power (MWt)	0.5	8.3	9.4
Downcomer Volume (ft^3)	86	184	706
Cold Leg Flow Area (ft^2)	1.6	4.1	4.6
Cold Leg Diameter (ft)	1.5	2.3	2.5
Saturation Pressure in S. G. (psia)	750	1088	911
HPI Flow (at above P) (ft^3/s)	1.5	1.6	1.9
Assumed Downcomer Temp at Stagnation (°F)	480	480	480
HPI Water Temp (°F)	130	130	130
CALCULATED PARAMETERS			
Minimum Break Area for Stagnation ($in.^2$)	1.2	1.3	1.6
Minimum Break Diameter for Stagnation (in.)	1.2	1.3	1.4
Initial Downcomer Cooldown with Perfect HPI Mixing (°F/s)	1.2	0.6	0.4

*Numbers are approximate and used to illustrate relative values between plants.

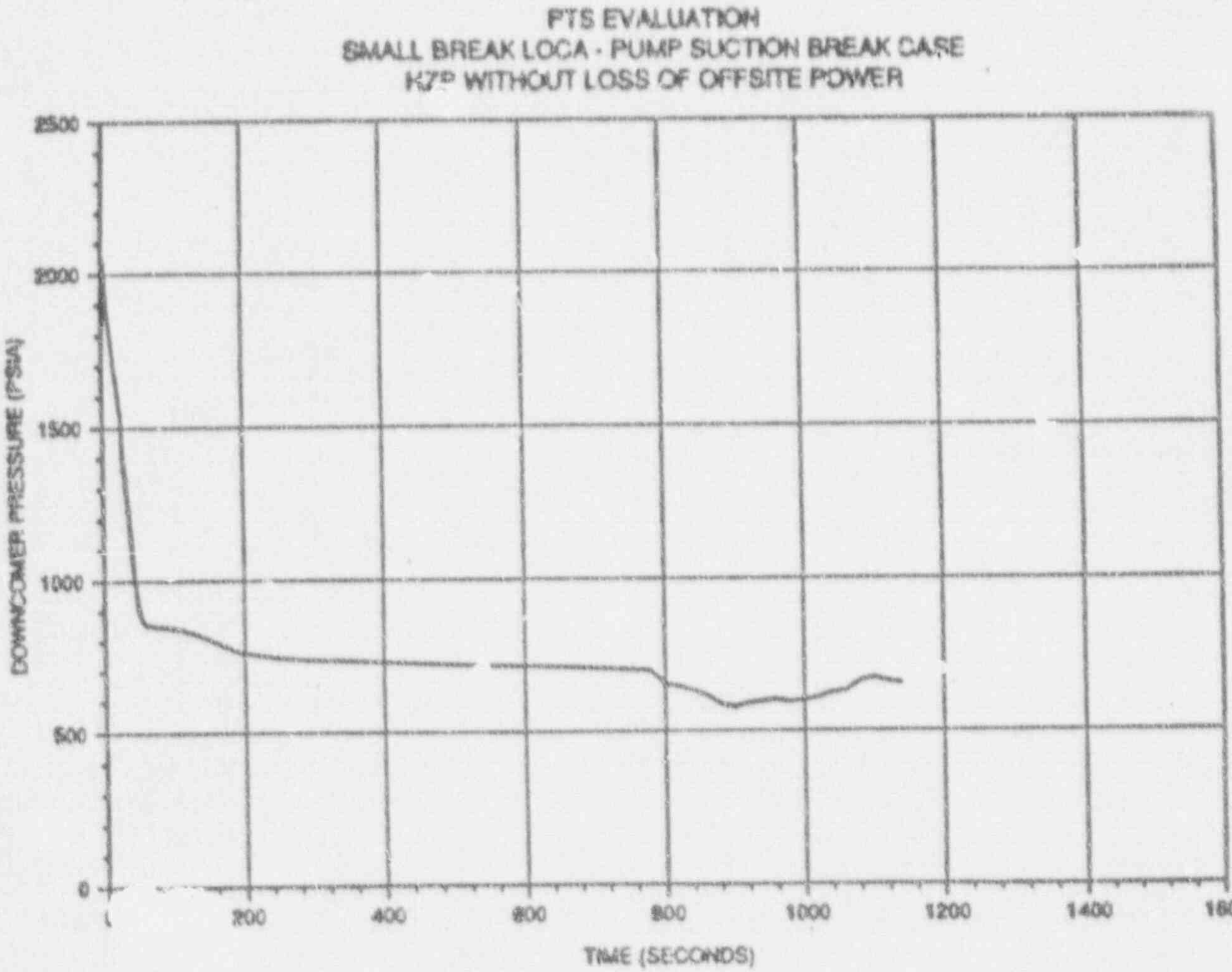


Fig. A.1. YNPS SBLOCA downcomer pressure from YAEC-1735.

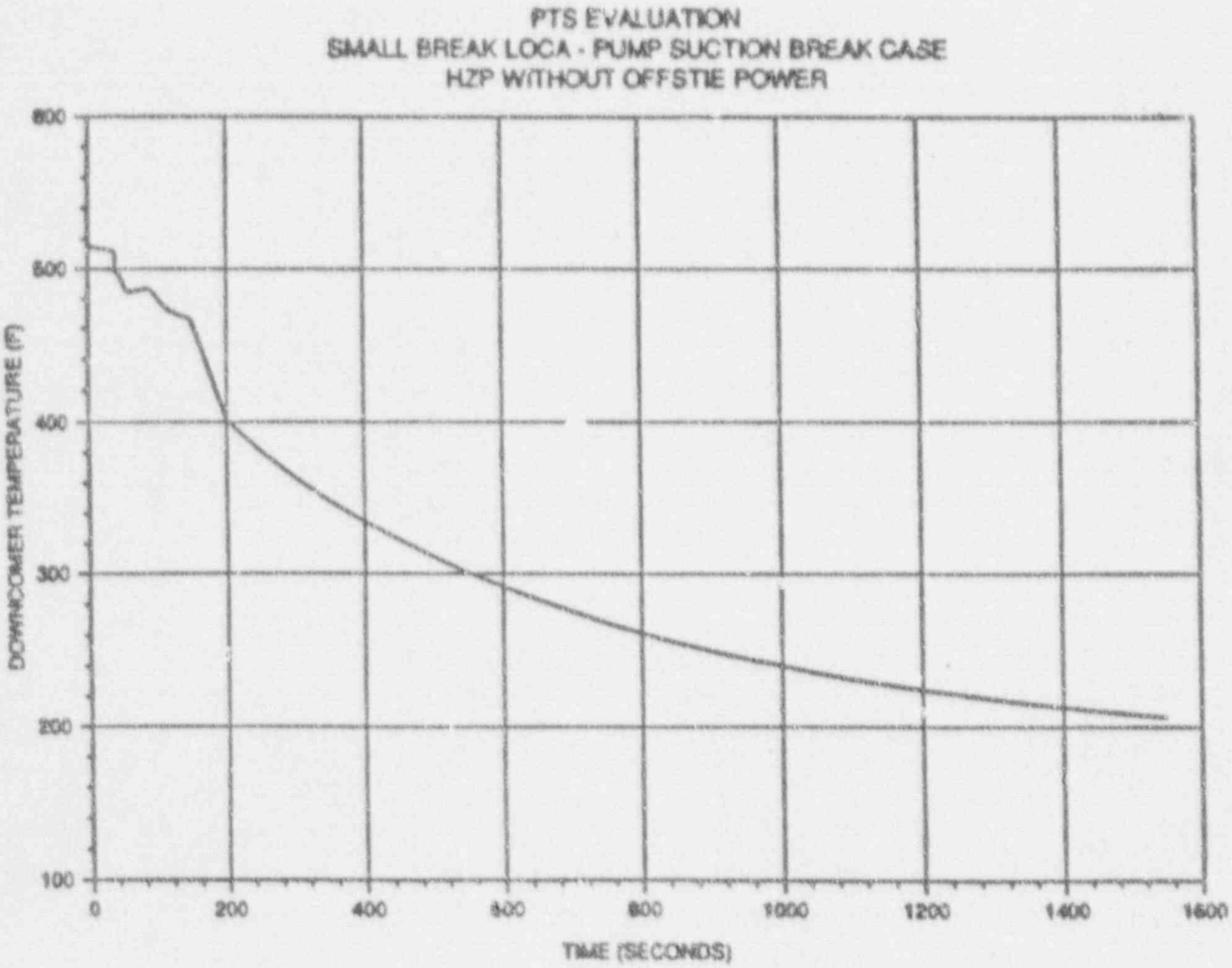


Fig. A.2. YNPS SBLOCA downcomer temperature from YAEC-1735.

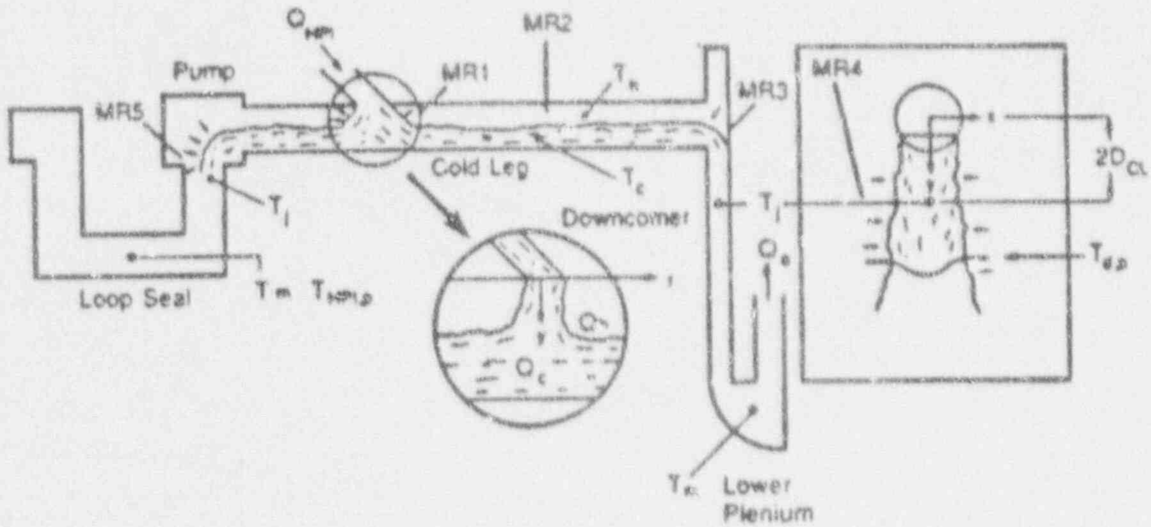


Fig. A.3. Conceptual definition of flow regimes in the cold leg and downcommer regions due to HPI water (Theofanous et al.).

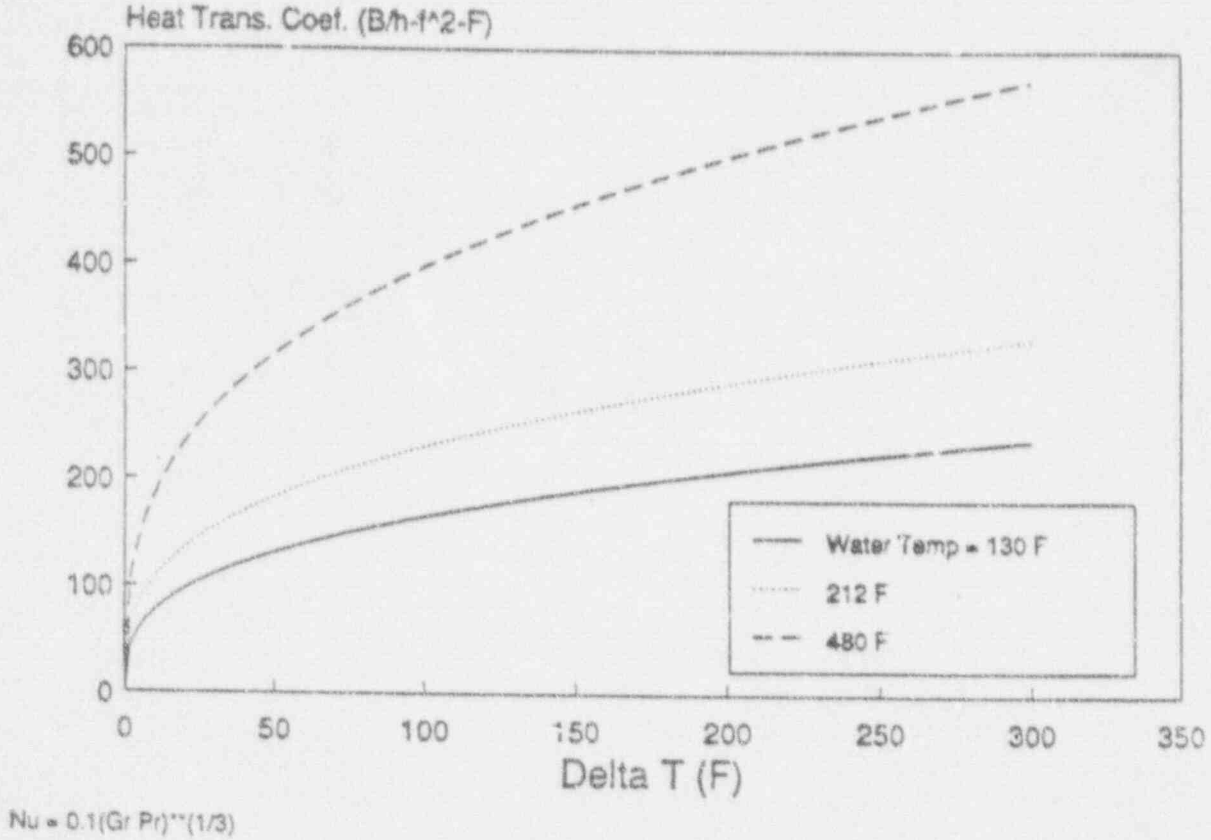
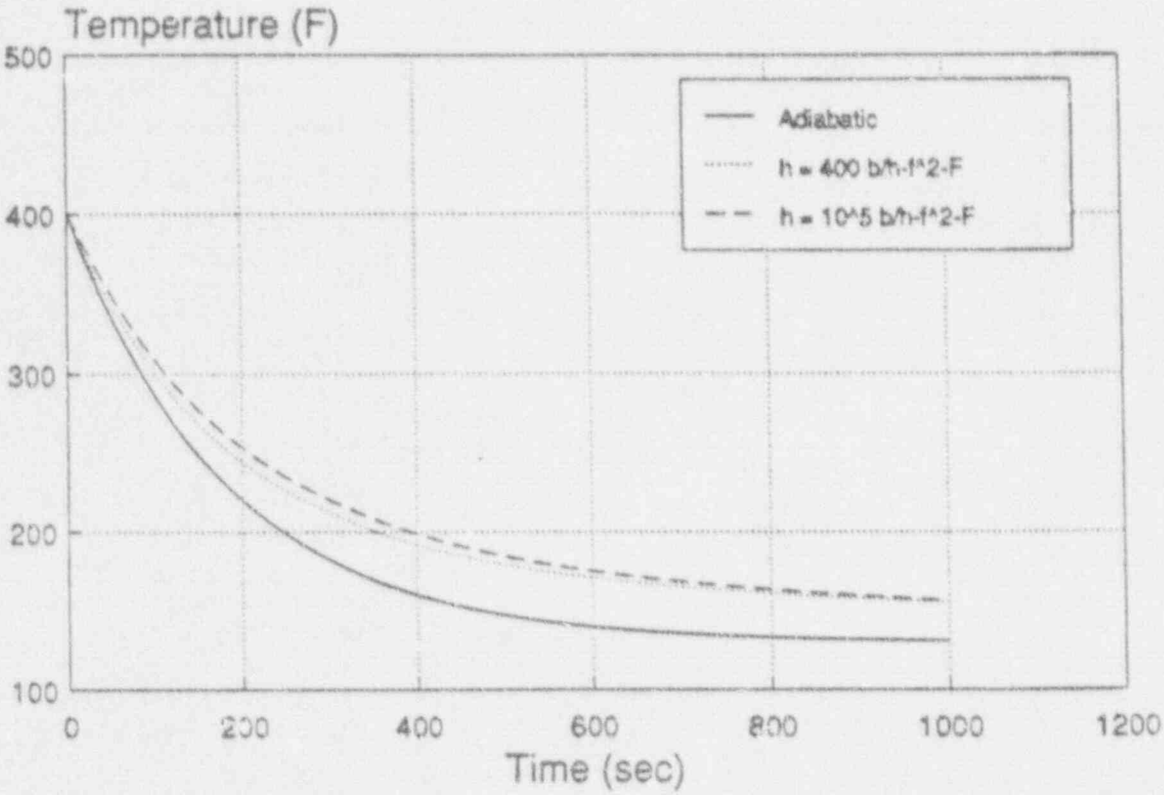


Fig. A.4. Natural convection wall heat transfer coefficient vs differential fluid to wall temperature.



HPI Flow = 90 lbm/sec

Fig. A.5. Downcomer fluid temperature parametric influence of wall heat transfer coefficient.

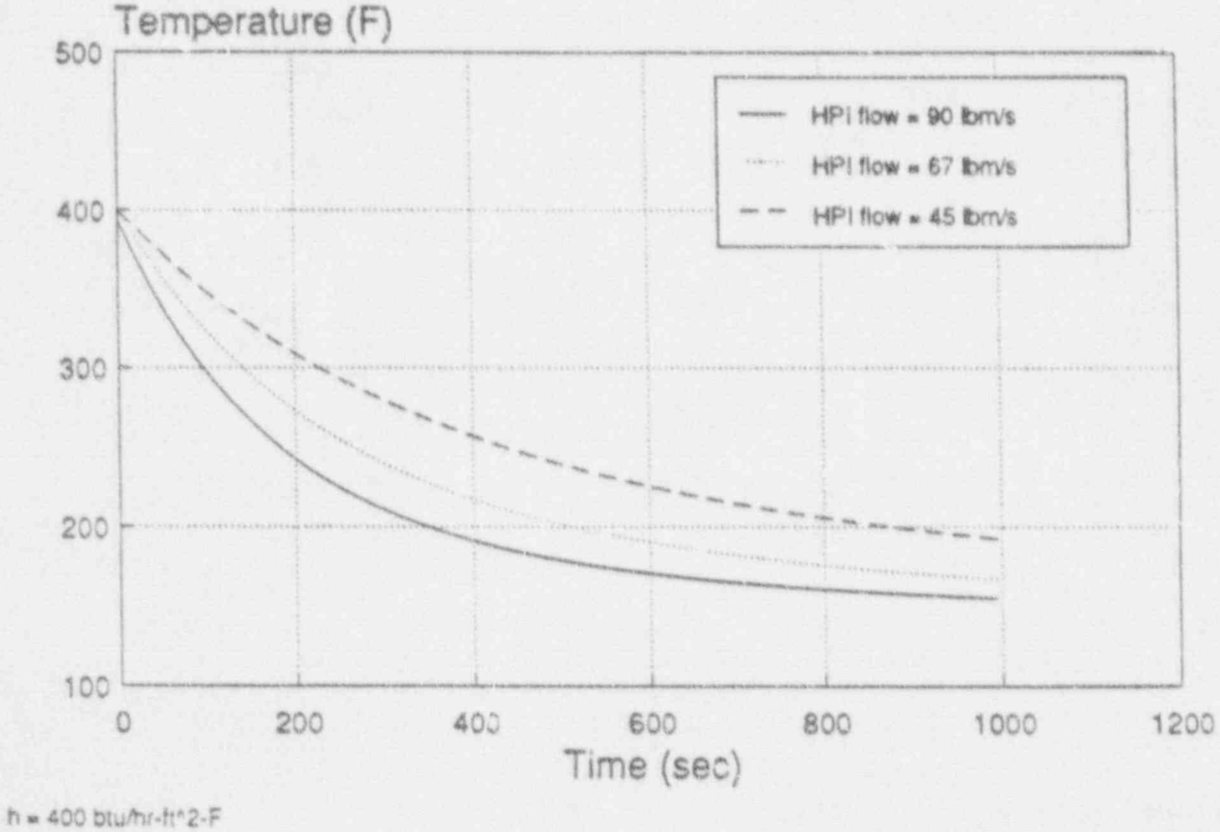


Fig. A.6. Downcomer fluid temperature parametric influence of HPI flow rate.

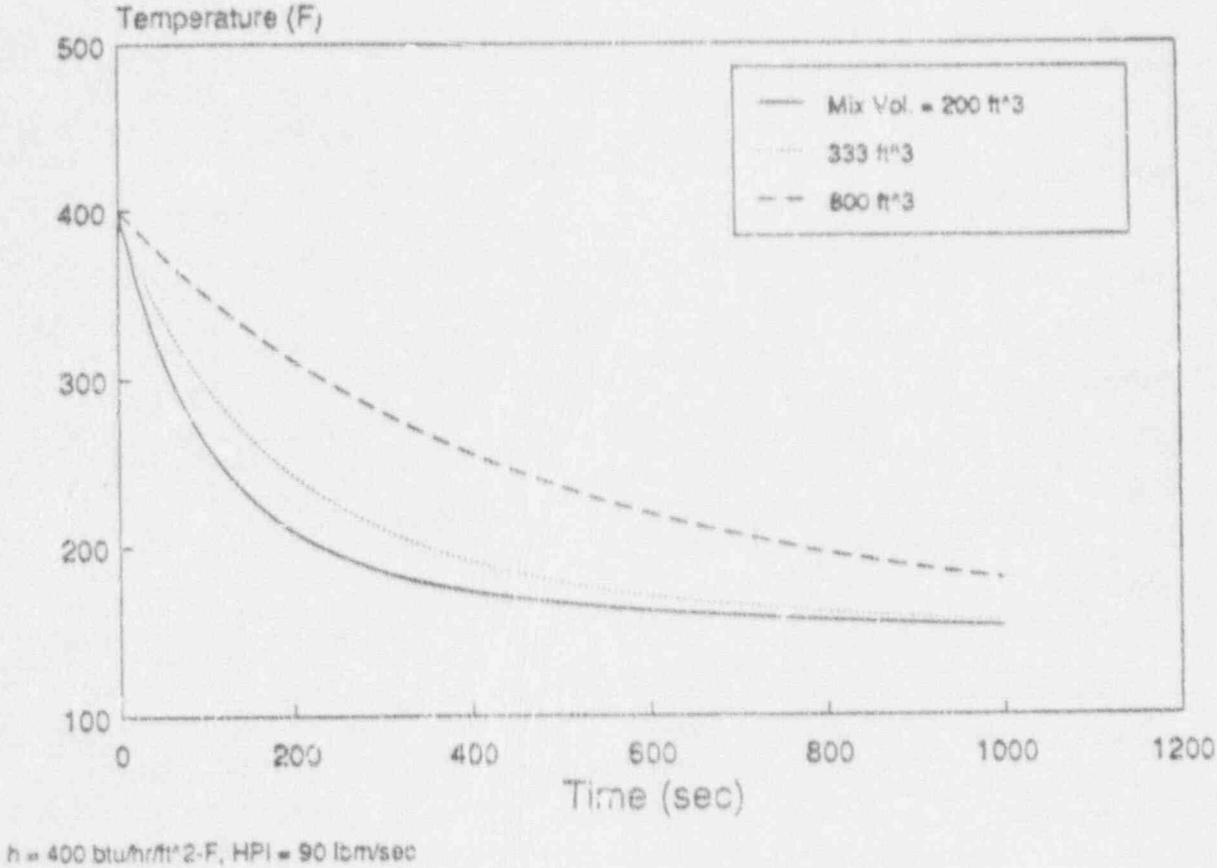


Fig. A.7. Downcomer fluid temperature parameter influence of water mixing volume.

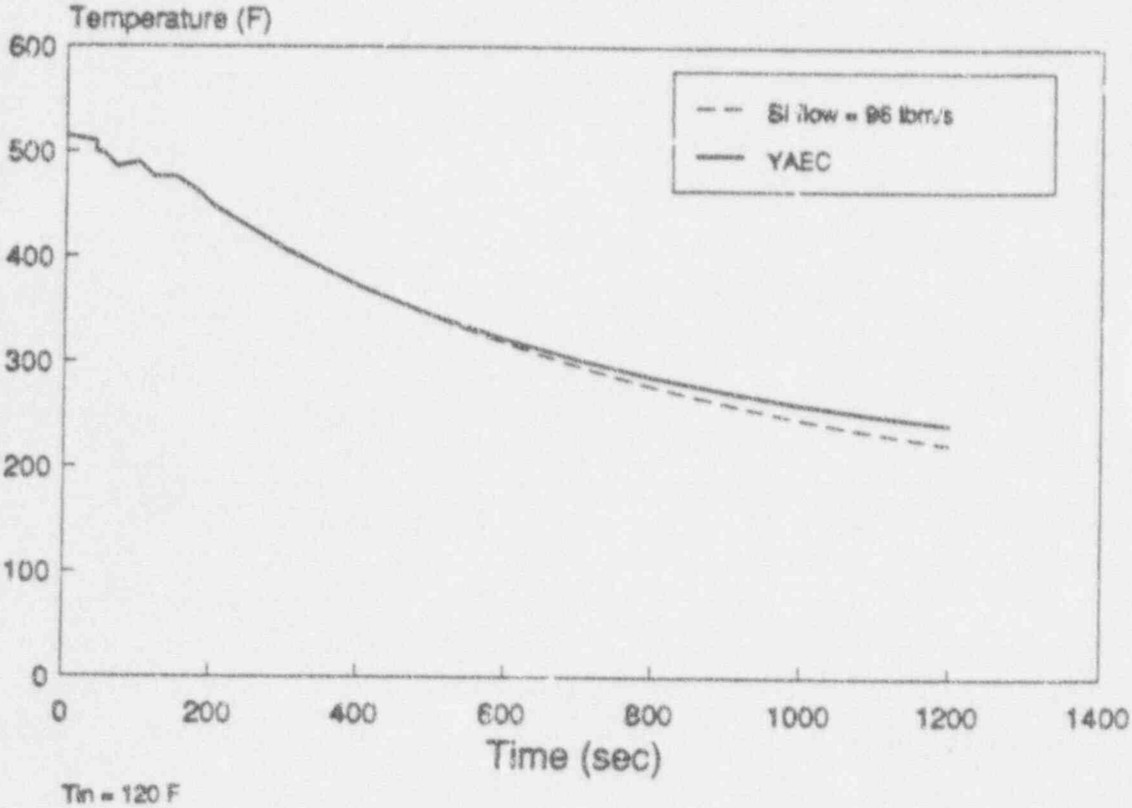


Fig. A.8. Comparison between the mixed-mean fluid temperatures in the SBLOCA sequence.

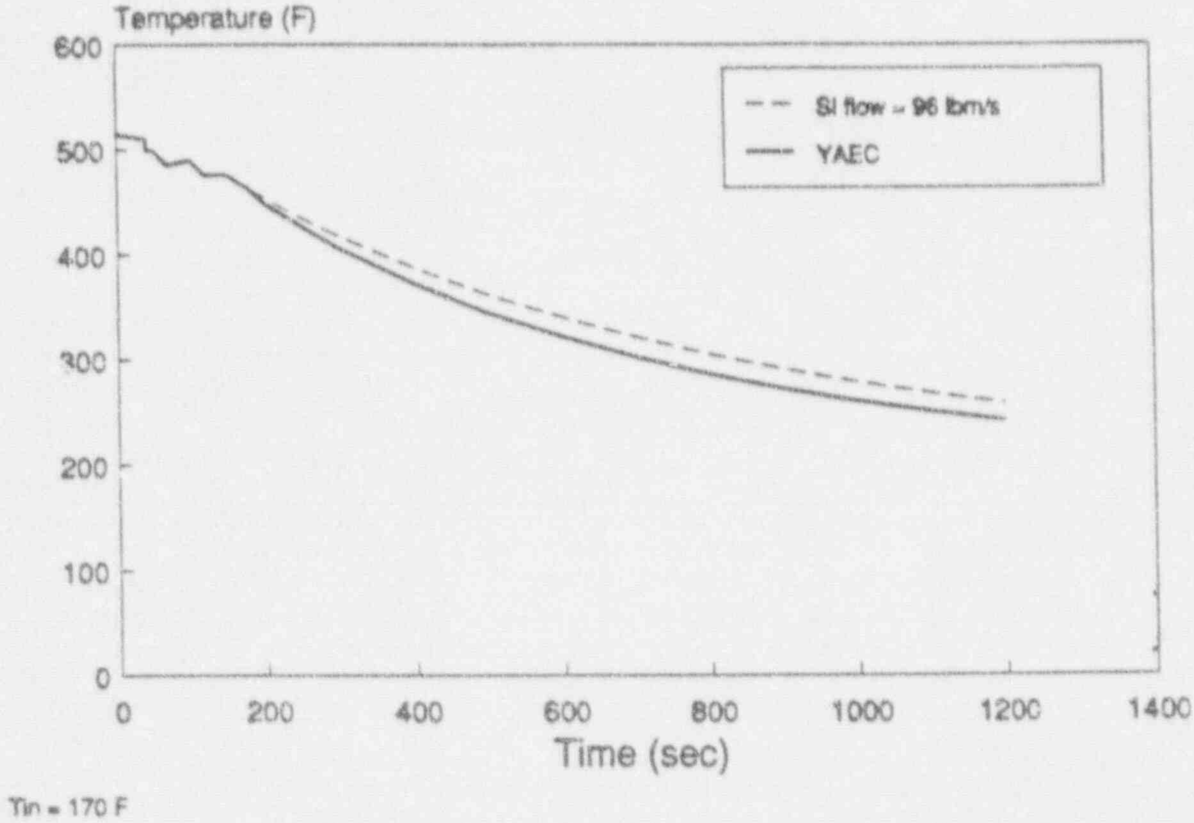


Fig. A.9. Parametric influence on the mixed-mean fluid temperature of 170°F safety injection water.

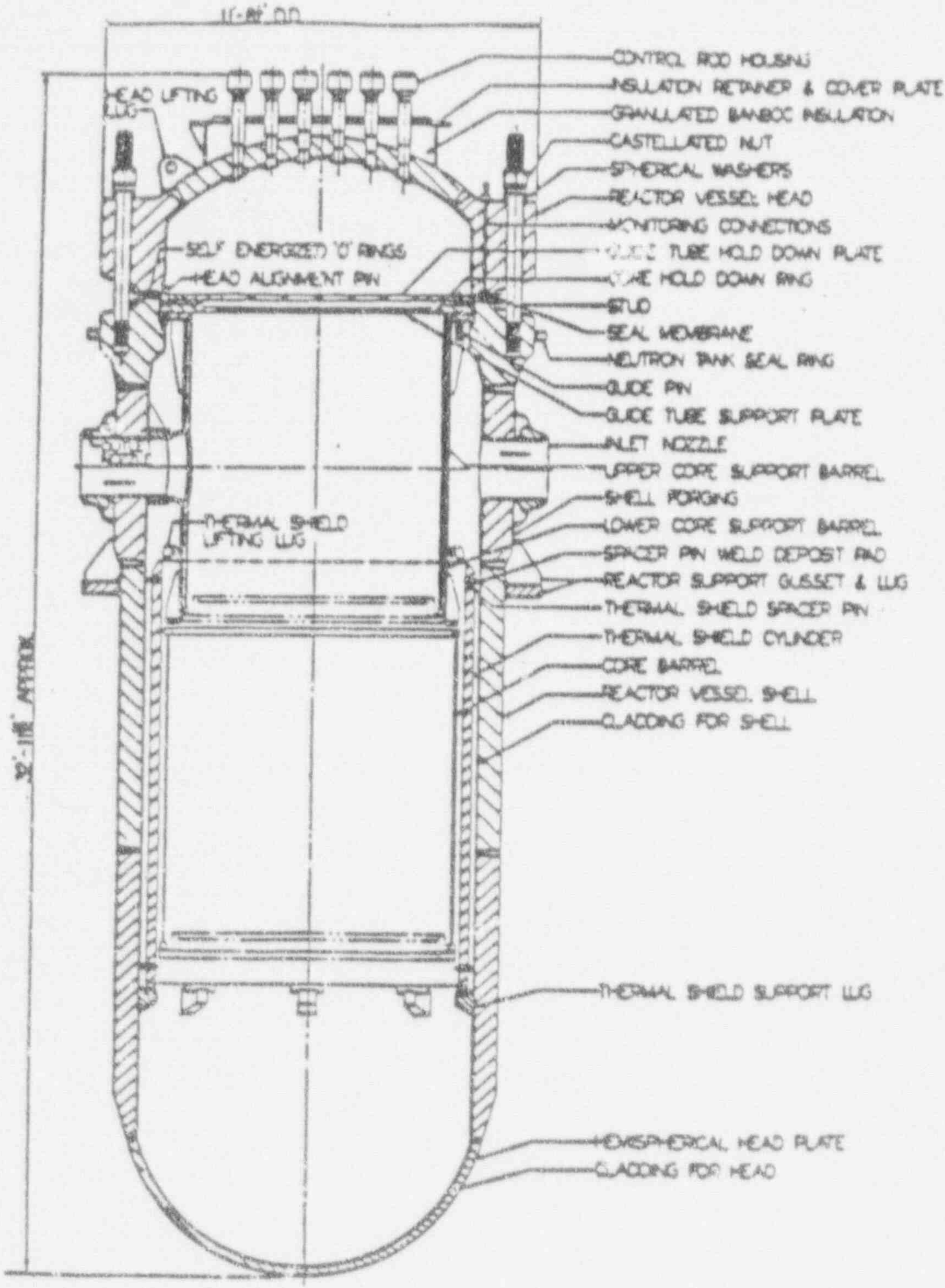


Fig. A.10. YNPS pressure vessel vertical cross section.

Attachment A
to SAIC Report No. 91-6501 (Appendix A.2)

Letter from J. D. Haseltine (YAEC) to
U‹. ›C, BYR 91-082, June 26, 1991

YANKEE ATOMIC ELECTRIC COMPANY

580 Main Street, Bolton, Massachusetts 01740-1398

June 26, 1991
BYR 91-082
RV 91-006

United States Nuclear Regulatory Commission
Document Control Desk
Washington, DC 20555

Reference: (a) License No. DPR-3 (Docket No. 50-29)
(b) USNRC Letter, P. Sears to G. Papanic, dated June 20, 1991

Subject: Request for Additional Information Concerning REMIX Calculations (TAC 80535)

Dear Sirs:

Enclosed is our response to the information requested in Reference (b).

We trust you will find this information satisfactory. If you need additional information, please feel free to contact us.

Very truly yours,

YANKEE ATOMIC ELECTRIC COMPANY

John D. Haseltine

John D. Haseltine
Project Director

GP/msi/tlp/C72\10
Enclosures

c: USNRC Region 1
USNRC Resident Inspector, YNPS
B. Elliot (NRC, NRR)
W. Russell (NRC, NRR)

Attachment (Enclosure for BYR 91-082)

In our July 1990 submittal, the limiting small break LOCA (1-5/16 in. pump suction) analysis conservatively assumed loop stagnation. Under these conditions, the REMIX code was used to predict the temperature distribution of the injection plume in the downcomer region. The application of REMIX to the Yankee ECCS design is conservative. Based on the injection velocities consistent with the Yankee ECCS design, more complete mixing would occur in the cold leg than predicted from REMIX. This would result in a warmer plume temperature than reported in the July 1990 submittal.

Due to the unique geometry of the Yankee vessel, the mixing volume in the REMIX model included the lower plenum. The lower plenum acts as a mixing volume in the Yankee vessel because of the thermal shield and core barrel geometry. The thermal shield is relatively close to the reactor vessel wall (~2-in. gap). As a result, the plume emanating from the cold leg would be contained between the thermal shield and core barrel. This plume would also pass through the core barrel region. Therefore, before reaching the vessel wall the plume would mix with fluid in the lower plenum region. Thus, the vessel wall under these conditions would see a temperature closer to the mixed-mean temperature calculated with REMIX.

The results reported in our July 1990 submittal for the downcomer temperature were conservatively based on the upper plume temperature predicted from REMIX and not the mixed mean temperature. In response to your recent request (6/20/91) we have evaluated the impact of not crediting the lower plenum mixing volume in the REMIX calculation. The results of our evaluation are presented in the attached figure.

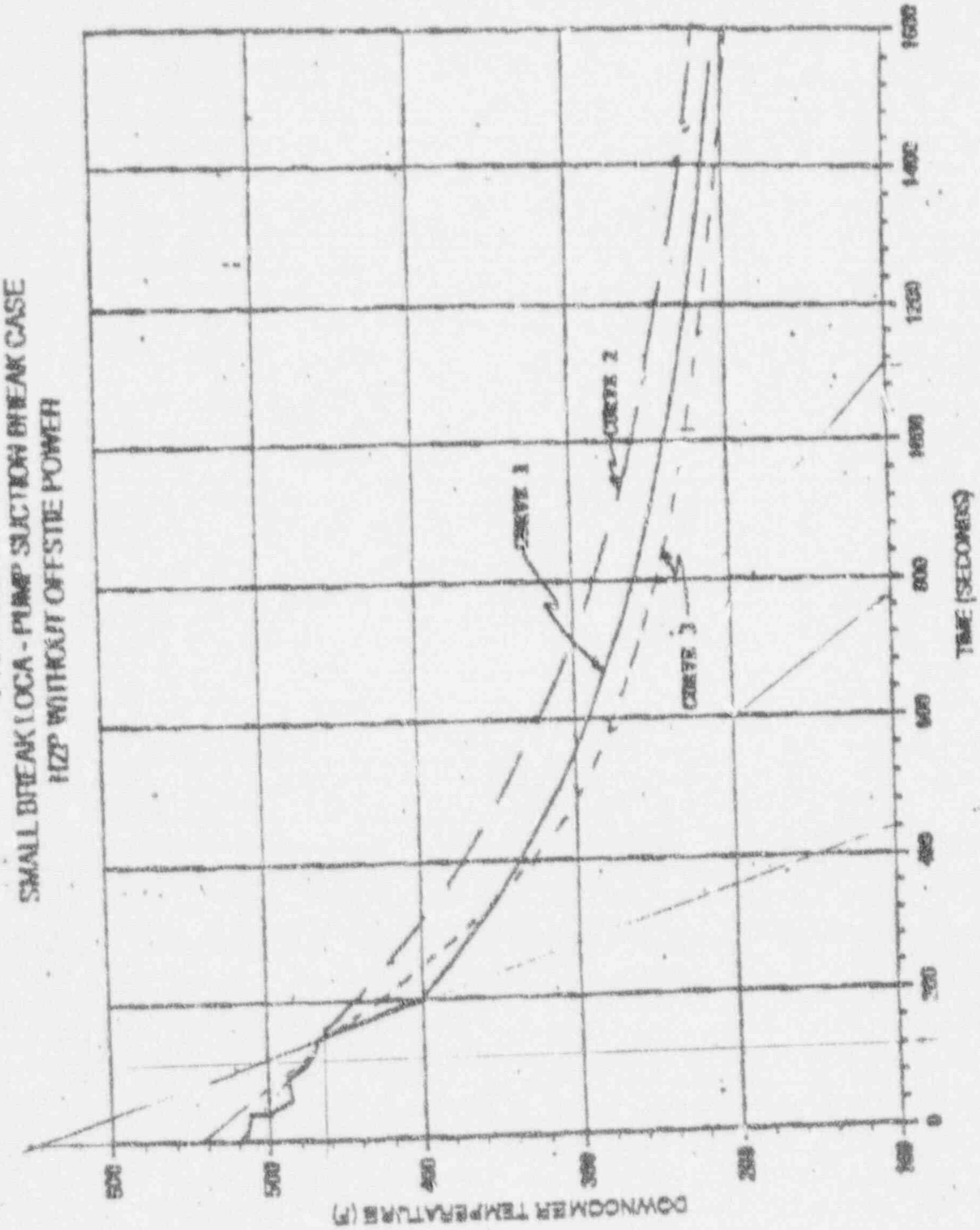
The attached figure provides three curves:

1) The original downcomer temperature response for the limiting small break LOCA based on the upper plume temperature near the cold leg nozzle.
2) The downcomer temperature response based on mixed-mean temperature as a result of the thermal-shield and core barrel geometry,
3) The downcomer temperature response based on mixed-mean temperature without crediting the lower plenum volume.

The attached figure shows that accounting for the unique geometry of the Yankee thermal-shield and core barrel is equivalent to crediting the lower plenum as a mixing volume. Thus, if we were not to credit the lower plenum as a mixing volume, conservatisms included in our July 1990 submittal would offset the impact resulting in a similar downcomer temperature response.

It should also be pointed out that the application of REMIX is conservative, and the assumption of stagnation leads to a conservative vessel temperature response. Based on the higher injection velocities consistent with the Yankee ECCS design, more complete mixing would occur in the cold leg than predicted with REMIX resulting in a warmer plume temperature than reported in our July 1990 submittal.

Because of the unique geometry of the Yankee vessel thermal-shield and core barrel region the lower plenum volume should be included in the mixing volume in the REMIX calculation. Even if the lower plenum was not credited in the REMIX calculation, based on the above stated conservations, the vessel temperature response reported in our July 1990 submittal remains bounding.



SBLOCA Downcomer Temperature - RCP Suction Break

Attachment B
to SAIC Report No. 91-6501 (Appendix A.2)

In response to our teleconference on July 5, 1991, we provide the following:

Question: When did the REMIX calculation start?
Answer: The REMIX calculation started at 150 s.

Question: What was the initial temperature assumed in the REMIX calculation?
Answer: The initial temperature was 476°F.

Question: What were the total SI flow rate and the SI water temperature used in the REMIX calculation?
Answer: 0.89 ft^3 per loop, and 120°F

Question: What was the thermal shield heat transfer area?
Answer: 153 ft^2 for both sides of the thermal shield, this represents a one quarter segment of the thermal shield.

Question: What was the volume assumed in REMIX?
Answer: Total volume = 264 ft^3, and mixing volume = 203 ft^3.

```
RUN CONDITIONS

031 - SBLOCA MCP SUCTION - NO OPER ACTION (A = 0.0094 FT**2)

THSO =    476.00  THPI    [illegible]

AQHPI =   .00E+00  BQHPI =   .39E+00

DIMENSIONS FOR MIXING COMPUTATIONS

VOL =    264.10  VOLM =    203.00

DI =       .188  DCL =     1.344  BCL =    21.600  WD =      .271

COMPUTATIONAL PARAMETERS

TIN =    150.00  TMAX =   6000.00  DELT =     30.00TIMPR =   3000.00DTIMPR =

RATIO =        .80  BETA =       .80

DIMENSIONS AND PROPERTIES FOR HEAT TRANSFER

DELCL =   .16E+00  DELDC =   .66E+00  DELTS =   .13E+00  DELLP =   .32E+00
DELP =  .18E+00  DELLS =  .18E+00  DELCB=   .83E-01DDCC =   .00E-02

ACLH =   165.000  ADCH =    62.300  ATSH =   153.000  ALPH =    14.800
APH =      .000  ALSH =  101.000ACBH =   217.000

ALCL =   .11E-03  ALDC =   .11E-03  ALTS =   .11E-03  ALLP =   .11E-03
ALP =   .47E-04  ALLS =   .11E-03  ALCB =   .47E-04ALDCC =   .47E-04

AKCL =   .67E-02  AKDC =   .67E-02  AKTS =   .67E-02  AKLP =   .67E-02
AKP =   .28E-02  AKLS =   .67E-02  AKCB =   .28E-02ALDCC =   .28E-02

HCL =   .14E+00  HDC =   .14E+00  HLP =   .14E+00
HP =   .14E+00  HLS =   .14E+00  HCB =   .14E+00  HO =   .55E-03  TO=      80.0

NODES AND CLAD PARAMETERS

M = 51  MP2 = 21  MP3 = 21

IDCCL =  1  IDCDC =  3  IDCTS =  1       IDCLP =  3
IDCP =  1  IDCLS =  1  IDCCB =  1

TINS =     .3300  AKINS =  .747E-05  ALFINS =  .31[illegible]E-05
```

TIME	TM	TH	TC	T JUMP	MDC	UC	UH	FR MPI
200.000	449.117	502.290	313.886	396.784	.140	1.288	.244	12.67
250.000	426.366	476.804	298.372	378.908	.140	1.232	.238	13.40
300.000	406.453	455.358	285.081	360.003	.140	1.177	.233	14.08
350.000	388.897	435.306	273.283	344.573	.140	1.142	.228	14.78
400.000	372.713	418.391	262.722	331.216	.140	1.095	.224	15.43
450.000	358.234	402.444	253.600	319.091	.140	1.068	.220	16.09
500.000	345.064	388.362	245.080	308.107	.140	1.030	.216	16.73
550.000	333.046	374.705	237.322	297.770	.140	1.007	.213	17.40
600.000	322.080	363.090	230.270	288.711	.140	.972	.210	18.02
630.000	311.870	351.865	224.183	280.363	.140	.957	.207	18.65
700.000	302.715	342.023	218.429	272.810	.140	.930	.204	19.25
750.000	294.204	332.818	213.073	265.761	.140	.905	.201	19.85
800.000	286.369	324.596	208.278	139.456	.140	.881	.199	20.42
850.000	278.148	316.443	203.962	253.484	.160	.868	.197	21.01
900.000	273.487	308.492	199.794	247.621	.140	.855	.194	21.63
950.000	266.337	302.517	196.248	243.006	.140	.833	.193	22.12
1000.000	260.855	295.605	192.665	237.959	.140	.822	.190	22.72
1050.000	255.401	288.914	189.687	233.787	.140	.811	.189	23.249
1100.000	250.840	284.762	186.860	229.825	.140	.791	.187	23.742
1150.000	246.036	279.639	184.011	226.088	.140	.780	.186	24.257
1200.000	241.867	275.075	181.641	222.752	.140	.770	.184	24.737
1250.000	238.001	271.126	179.721	219.919	.140	.768	.183	25.169
1300.000	234.414	267.137	177.510	216.958	.140	.751	.181	25.820
1350.000	231.084	263.172	175.465	214.056	.140	.761	.180	26.092
1400.000	227.992	259.885	173.688	211.810	.140	.732	.179	26.520
1450.000	225.119	256.827	172.279	209.480	.140	.728	.178	26.884
1500.000	222.447	253.559	170.465	207.031	.140	.715	.177	27.312
1550.000	219.962	250.834	169.030	205.023	.140	.707	.176	27.683
1600.000	217.649	248.066	167.857	203.149	.140	.706	.175	28.071
1650.000	215.496	245.745	166.628	201.434	.140	.698	.174	28.405
1700.000	213.487	243.697	165.632	199.981	.140	.693	.173	28.700
1750.000	211.615	241.523	164.348	198.305	.140	.682	.173	29.038
1800.000	209.869	239.673	163.336	196.928	.140	.674	.172	29.335
1850.000	208.239	237.876	162.475	195.519	.140	.674	.171	29.659
1900.000	206.716	235.213	161.549	194.005	.140	.673	.170	30.029
1950.000	205.294	234.308	160.861	193.134	.140	.666	.170	30.214
2000.000	[illegible] .968	232.320	160.096	191.874	.140	.665	.169	30.335
2050.000	[illegible]	231.586	159.683	191.320	.140	.661	.169	30.662
2100.000	201.557	230.206	158.781	190.208	.140	.651	.168	30.905
2150.000	200.466	229.046	158.318	189.438	.140	.651	.168	31.112
2200.000	199.445	227.785	157.589	188.476	.140	.644	.168	31.341
2250.000	198.487	226.818	157.087	187.636	.140	.643	.167	31.575
2300.000	197.589	225.086	156.523	186.690	.140	.643	.166	31.844
2350.000	196.746	224.025	156.104	185.988	.140	.643	.166	32.047
2400.000	195.954	223.372	155.708	185.568	.140	.638	.166	32.135
2450.000	195.210	222.477	155.280	184.847	.140	.636	.165	32.349
2500.000	194.510	221.673	154.966	184.317	.140	.635	.165	32.509
2550.000	193.852	221.385	154.854	184.126	.140	.635	.165	32.566
2600.000	193.232	220.743	154.284	183.526	.140	.626	.165	32.695
2650.000	192.648	219.601	153.737	182.718	.140	.623	.164	32.928
2700.000	192.098	218.865	153.434	182.235	.140	.622	.164	33.080
2750.000	191.579	218.171	153.188	181.780	.140	.622	.164	33.225
2800.000	191.090	218.293	153.235	181.861	.140	.623	.164	33.199
2850.000	190.627	217.669	152.788	181.336	.140	.616	.164	33.331
2900.000	190.190	217.101	152.571	180.964	.140	.615	.163	33.451
2950.000	189.777	216.543	152.359	180.600	.140	.615	.163	33.571
3000.000	189.387	216.016	152.158	180.258	.140	.615	.163	33.685

CENTERLINE TEMPERATURES AT DOWNCOMER LOCATIONS,

TIME = 3000.000

HEIGHT FROM CL CENTRE	TEMPERATURE
1.344	168.207
3.194	180.987
6.988	184.028
11.587	185.851
21.098	187.569
28.068	188.826

DIMENSIONLESS THICKNESS	TEMPERATURE
.000	201.994
.004	203.695
.008	205.393
.012	207.090
.112	224.513
.260	247.982
.408	268.482
.556	285.260
.704	297.713
.852	305.411
1.000	308.068

```
   TIME      .TM        TH         TC       T JUMP    HC      UC      UH    FR HPI

 3050.000   189.317   218.101   151.810   178.658       .140    .615    .163 33.88
 3100.000   188.667   214.870   151.722   179.507       .140    .615    .162 33.93
L AND XR DO NOT BRACKET A ROOT
Stop - Program terminated.
```

Appendix A.3

Review of Accident Sequence Identification and Quantification in the Yankee Rowe Pressurized Thermal Shock Analysis

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The following comments have been developed based on a review of the Yankee Rowe (YNPS) PTS analysis submittal, "Reactor Pressure Vessel Evaluation Report," YAEC No. 1735, July 1990. This review made use of the Yankee Rowe Updated FSAR, emergency operating procedures currently applicable to the plant, the PTS analysis of H. B. Robinson performed by ORNL for NRC (NUREG/CR-4183), and data developed in the NUREG-1150 program. Major questions exist concerning analysis assumptions, the completeness and appropriateness of the YNPS accident sequences, and the estimated frequencies for modeled sequences.

General Comments

1. The limited documentation of the bases for screening accident initiators and quantifying the sequence split fractions prevents detailed review and verification. Numerous accident sequence split fractions are justified by references to system event trees and fault trees. These trees are not provided. Also, the frequencies of support states on which various split fractions are conditioned are not provided; therefore, reproduction of the sequence frequencies is not possible.

2. The overall resolution of initiating event selection for sequence development was significantly coarser than the resolution used in the PTS analysis of H. B. Robinson. For example, the YNPS analysis considered only two full-size steam line breaks and only one small-break LOCA. Other events were screened out based on frequency or consequence.

3. No attempt is made to systematically bound the PTS risks stemming from initiating events and accident sequences that are screened out on the grounds of frequency or consequence. There is often ambiguity as to whether a given initiating event is to be conservatively grouped with another initiator for which the anticipated consequence is more severe, or whether it is being excluded from further consideration. For the initiating events and accident sequences that are explicitly screened out, no attempt is made to determine their aggregate contribution to risk. Consideration of residual PTS risk played an important role in the ORNL analyses of Oconee, Calvert Cliffs, and H. B. Robinson.

4. There appear to be inconsistencies between the current operating procedures and the sequences modeled in the PTS analysis. For example, except for bleed and feed cooling following failure of the safety injection pumps, the charging pumps are assumed in the analysis to be tripped as a result of the safety injection signal associated with most initiating events and accident sequences which were analyzed. Because of this, MCS repressurization was limited in the analysis to the SI pump shutoff head. However, restart of the charging pumps (which could pressurize the MCS to the primary relief valve setpoint) and reenergization of the pressurizer heaters are specific procedural steps following termination of SI and in a situation where all steam generators blow down.

Initiating Event and Accident Sequence Selection

1. In the reported review of YNPS PRA event and fault trees by the PTS analysts on pp. 6-26, it is not clear what criteria were used to identify potential overcooling initiating events and accident sequences based on models that were presumably developed to address the potential for core damage. Related to this, Table 6.5.2.1-4 was derived based on a review of the categorization in NUREG/CR-3862, "Development of Transient Initiating Event Frequencies for Use in Probabilistic Risk Assessments." However, the focus of this reference was to support development of core damage PRAs by identifying transient events which caused scrams. Because of this, transients listed in NUREG/CR-3862 may not adequately bound all transient classes with the potential for overcooling.

2. Section 6.6 states that only those events resulting in a cooldown from hot soaked conditions with a rate in excess of 200°F/h. and a relatively high MCS pressure were considered capable of posing a PTS concern. While this is consistent with the YNPS Critical Safety Function Status Tree F-0.4, INTEGRITY (which does not recognize an imminent PTS condition for MCS cold leg temperatures >280°F), it may be a nonconservative threshold for transient evaluation. Replacement of the cooldown rate screening criterion (with, for example, a criterion incorporating cooldown

magnitude) could have significant impact on the YNPS PTS risk profile.

Since transients with less severe cool-downs are far more frequent, their PTS risk could dominate even if the probability of through-wall crack is several orders of magnitude less. Exclusion of all less severe transients from further analysis should be carefully justified.

3. Consistent with the above comment, consequence and frequency arguments are used to screen out from further consideration PTS sequences that are initiated by stuck-open secondary side relief valves and small main steam line breaks. It should be noted that:

   a. In the NRC's H. B. Robinson PTS study, the initiating event frequency associated with stuck-open secondary valves and small secondary side ruptures is relatively high (2E-2/yr). If there are arguments to reduce the corresponding YNPS frequencies relative to the Robinson number to a degree that warrants the exclusion of this initiating event, they need to be provided in greater detail.

   b. In the YNPS PTS Study, a rationale for excluding sequences involving stuck open secondary valves (Sect. 6.6.3.1.1) is that they result in cooldown rates which are less than the screening criterion of 200°F/h. However, the Robinson study indicates a significant magnitude of cooldown for such sequences. In fact, the class of sequences that is PTS risk-dominant at Robinson involves stuck open secondary valves following reactor trip.

Such sequences account for a total through wall crack frequency of ~1E-8/yr on that plant. The rationale given for excluding plant trip as an initiator in the YNPS Study does not address the issue of the potential for the consequent sticking open of steam dump valves or secondary side safety valves if these are challenged. Numerous stuck open secondary side safeties have been historically observed in the industry.

4. Yankee Rowe is one of the few commercial nuclear plants in the United States with main coolant isolation valves. Operation of these valves may affect PTS sequences for the plant. For example:

   a. Successful isolation of a small-break LOCA in a main coolant loop could result in repressurization to the normal plant operating pressure. While closure of the loop valves to isolate a break is not addressed in the operating procedures, closure of the PORV or its block valve to isolate a transient-induced LOCA is included in several procedures. It would seem to be intuitive for an operator to attempt to isolate a LOCA.

   b. A cold water accident may be possible if the valves on an isolated loop are suddenly opened (operator error or spurious valve operation). Such an event could result in asymmetric cooling of the reactor vessel, combined with a rapid increase in reactivity. Is this type of accident possible at YNPS and, if so, what are its PTS consequences?

5. There is apparently no consideration (pp. 5-41) of sequences initiated by loss of main feedwater followed by actuation of cold emergency feedwater (EFW). These sequences were addressed in the Robinson study.

6. While loss of control air is discussed in the report, it is not specifically addressed. The updated FSAR notes that the charging pump fluid drive speed is controlled by a pneumatic signal based on pressurizer level. What is the charging pump speed on loss of air? If the charging pumps fail to high-speed then this initiator may require additional scrutiny since the main feed control valves fail-as-is on loss of air.

Human Reliability Considerations

1. While the HEP curves used in the YNPS analysis appear to be fairly conservative, their application in the main steam line break (MSLB) and small-break LOCA event trees appears to have generated optimistic HRA estimates. For example, without knowledge of the detailed application, it cannot be determined what degree of credit has been given in the HEP estimates by using the seven modifying factors listed on pp. 6-83. The following are some specific concerns related to the HEP estimates:

   a. On pp. 6-108 and 6-125, the HEP estimates in the 1E-7 to 1E-5 range for cooldown control and system realignment for recirculation seem low. For comparison, the NUREG-1150 analyses generally avoid the use of failure probabilities less than 1E-3 for any single operator action.

   b. On pp. 6-163, where the HEP estimates are discussed in more detail, the steps leading to the 1E-7 probability estimate for failure of the operator to control cooldown (with feedwater isolation successful and no SG blowdown) are not given. For the scenarios in which one or more SGs blow down, the HEP derivations provided on pp. 6-163 reveal that, effectively, operator error has not been accounted for in modeling recovery from the failure to isolate affected SGs. Hardware failures therefore dominate. This assumption is that the hardware failure probability of 3E-3 is significantly higher than operator error probability. This requires justification.

c. The probabilities attached to OYO-OYC are conditional on the occurrence of a previous operator error (feedwater isolation). As such, 1E-2 is low. For comparison, in the H. B. Robinson PTS analysis, no credit is given for AFW control if there has been failure to isolate the affected SGs. In general, the assignment of operator error probabilities conditioned on the occurrence of a previous error in an accident sequence should be conservative. For comparison, the approach adopted in NUREG-1150 is not to give credit for second and subsequent errors (in aggregate) of more than a factor of 0.1, i.e. cut sets with multiple errors are generally assigned HEPs of no less than 1E-4.

In general, it appears that in the YNPS PTS analysis, the HEP probability/time correlation may have been applied to individual operator actions in each sequence and the resultant probabilities then multiplied together. The HEP curves are more appropriately applied to the combination of actions required to provide a given function within a single sequence (e.g. feedwater isolation and control). The detailed YNPS HRA calculations would need to be reviewed to assess the appropriateness of the HEP curve application.

Proposed screening requantification for HRA values:

a. OI - Failure of operator to isolate feedwater after trip. Replace probability of 1.7E-4 (or 1.3E-4 as stated on pp. 6-162) by 1E-2, a number reflecting typical assumptions for the failure probability associated with rule-based actions in the NUREG-1150 study. Also, increase FI (feedwater isolation) failure probability in the small-break LOCA event tree by two orders of magnitude.

b. OY - Failure to control cooldown. The basis for the probability of OY0 (failure to control cooldown given successful SG isolation) is not provided. Typical rule-based actions are assigned failure probabilities in the range 2E-3 to 5E-2 in NUREG-1150. Without knowledge of procedures and specific actions, the recommended screening value for OY0 is 1E-2.

c. For events OY1 - OY3 (failure to control cooldown given multiple SG blowdown), a human error probability should to be added to each recovery failure probability. Since recovery is conditioned on previous occur-rence of an error of commission in OY2 and OY3, the approach adopted in NUREG-1150 allows limited credit for success of a sub-sequent action. If the recovery HEP is set to 1E-1, this gives: OY1 = 1E-3 and OY2 = 1E-3. To OY3, a screening human error probability of 1E-2 should be added to give OY3 = 2.2E-2.

d. OYO - OYC are cooldown control events conditioned on failure to isolate feedwater. The assignment of a failure probability 1E-1 to each event would reflect the general NUREG-1150 approach of giving limited credit for operator actions following earlier operator errors in the same sequence.

Frequency and Branch Probability Estimation

1. On pp. 6-58, in the characterization of small-break LOCA frequencies, partitioning the pipe break frequency between the <1 in. and 1 in. to 2 in. ranges assumes that all small breaks are effectively guillotine, excluding scenarios involving small breaks in larger piping. This assumption is unjustified. A more appropriate treatment would be to retain the Bayesian updated WASH-1400 pipe rupture frequencies without the use of scale-down arguments.

   Also, on pp. 6-171 it is stated that small-break LOCAs are "limiting" loss of coolant accidents from a PTS consequence perspective. This does not preclude the possibility of significant risk contribution from sequences initiated by larger LOCAs. If larger LOCA sequences are not to be considered explicitly, their frequencies should be conservatively added to the small-break LOCA sequence frequencies.

   Proposed screening requantification: Replace the 5.24E-4/yr small-break LOCA initiating event frequency with a YNPS WASH-1400 update value of 2.1E-3. Alternately, a value of 1E-3/yr for small-break LOCA, as utilized in NUREG-1150 analyses (see Table 8.2-4 of NUREG/ CR-4550, Vol. 1, Rev. 1, "Analysis of Core Damage Frequency: Internal Events Methodology") could also be employed. Use of either of these values would be reasonable, considering the uncertainties associated with the estimates.

   Also, unless medium- and large-break LOCAs have been explicitly considered in the analysis, the frequencies for these initiators should be added to the revised small-break LOCA frequency to bound larger LOCA contributions. Based on NUREG-1150 data, medium and large LOCAs have a total frequency of 1.5E-3/yr.

2. Partitioning pipe rupture probability uniformly among pipe sections that has no dependence on

pipe size (pp. 6-59) seems inappropriate. A better assumption would be that of a uniform volume density of initial cracks in pipework welds. This would imply a partitioning of rupture frequency that is dependent on pipe size. The effect of the frequency distribution arguments used in the YNPS study is to scale down the pipe rupture frequencies in critical pipe sections. (Based on a footnote on pp. 6-78, it appears that these location-dependent scale-down arguments were not ultimately used. This requires confirmation.)

3. Various split fractions in the steam line break event tree (pp. 6-108) are conditioned on events that are neither defined previously in the event tree nor characterized as support states (e.g., event CNX which is conditioned on DC availability, and events GN/G2 conditioned on nonreturn valve (NRV) actuation train availability). In general, the heavy reliance of event tree quantification on plant fault tree and event tree models, which are not provided, allows only broad split fraction quantification checks. The absence of support state frequency data precludes checks on the sequence frequencies.

4. From the perspective of hardware reliability (pp. 6-108 and 6-125), probability assumptions regarding failure to isolate/control feedwater (OI, MSLB event tree, FI in small-break LOCA event tree) seem low given plant-specific experience. For example, among the last 10 years of LERs are two events involving loss of feedwater control (event date 11/27/80, and LER No. 86-012-00).

   Proposed screening requantification: While the implications of these LERs for event tree quantification requires more detailed systems/procedures knowledge, replacement of the OI/FI probability as described under Human Reliability Considerations should bound any modified hardware reliability estimates.

5. Failure to automatically trip the boiler feed pumps in the MSLB event tree (pp. 6-98 and 6-108) is assigned a probability of 2.8E-3 conditioned on DC power being available. According to Table 6.5.3.2-1, no credit is given in this number for manual actions to trip the feed pumps. Among the last 10 years of LERs is the report of a failure of the feed pumps to auto-trip. Without view of the referenced boiler feed pump trip fault tree, evaluation of the appropriateness of the probability used is not possible. Nevertheless, this number seems low based on plant experience.

   Proposed screening requantification: Increase of the BF0 probability by at least one order of magnitude (assuming one failure of feed pump auto trip in plant lifetime) would be appropriate in the MSLB event tree. Increase of the FI (feedwater isolation) failure probability in the small-break LOCA event tree (in addition to the increase suggested under Human Reliability Considera-tions) would also be appropriate.

6. Event G20*GN0 (pp. 6-107) is the blowdown of a single SG given a MSLB downstream of the NRV, with the NRV actuation train available. One NRV failure to close occurred in June 1982. If the NRVs are not tested monthly, then the NRV failure probability used in the analysis (5.88E-3) may be low. In addition, auto-closure of the NRVs has only recently been implemented. Does prior testing provide confidence in the reliability of fast closure as assumed in the analysis?

Credit for Operator Actions and Alternate Procedural Actions

1. It is not clear that excluding credit for various actions/systems available to provide feedwater (as identified in Table 6.5.3.2-1) in MSLB sequences is a conservative assumption in the context of PTS risk.

2. On pp. 6-120, exclusion of operator depressurization of the vessel in the small-break LOCA event tree to permit LPSI injection is not necessarily conservative relative to PTS potential. In general, discussion of each item for which "credit is not taken" relative to PTS (vs core damage potential) is warranted.

3. Emergency feedwater actuation/control (pp. 6-120) is not modeled in the small-break LOCA event tree. Since loss of feedwater control is a potential route to overcooling, the rational for this exclusion should be provided.

Appendix B

Technical Evaluation Report

Review of the YAEC Thermal Hydraulic Accident Sequence Analyses for Assessment of Pressurized Thermal Shock for the Yankee Nuclear Power Station

Contents

Review of the YAEC Thermal Hydraulic Accident Sequence Analyses for Assessment of Pressurized Thermal Shock for the Yankee Nuclear Power Station

Leonard W. Ward
Craig Kulberg

B.1 Introduction

This Technical Evaluation Report presents a review of the thermal hydraulic analyses performed by the Yankee Atomic Electric Company (YAEC) to address pressurized thermal shock for the Yankee Nuclear Power Station. The thermal hydraulic accident analysis events were discussed and presented in the YAEC Report No. 1735, entitled "Reactor Pressure Vessel Evaluation Report," dated July 1990. The thermal hydraulic analyses included the following events:

1) Main steam line break (5 cases)
2) Excessive feed flow (2 cases)
3) Small break LOCA (1 case)

In reviewing the above events, only the reactor coolant system pressure and downcomer temperature responses were provided. As such, there was insufficient information regarding the other key primary and secondary system transient response parameters to permit a thorough and proper review. A review of the information provided in the report, however, identified several major concerns which will require resolution. These concerns include the following:

1) The pressurizer nonequilibrium model used in the analyses did not properly account for the heat transfer governing the thermal conditions in this region during refill and repressurization. As a consequence, the approach used in modeling the thermal behavior in the pressurizer will tend to overpredict heat removal from the pressurizer fluid and underpredict peak pressure during refill. The effect of this nonconservative pressurizer modeling technique on those events which experience pressurization needs to be evaluated to demonstrate that the approach does not adversely affect the results nor change the conclusions presented in the report.

2) No justification was provided to demonstrate that the small break LOCA presented in the report is the worst case for Pressurized Thermal Shock (PTS) considerations. The analyses of a spectrum of breaks needs to be evaluated and discussed to demonstrate that the minimum temperature and maximum pressure response for the break presented in the report bounds that for a spectrum of break sizes.

3) The maximum break size which can be isolated was not presented nor discussed in the report.

Since the Emergency Operating Procedures do not prevent the operator from isolating the break, the response for this event should also be included for PTS evaluations.

Based on the above concerns, the thermal hydraulic analyses presented in the YAEC report are not acceptable for use in assuring the worst case has been identified for PTS evaluations of the Yankee Nuclear Power Station. Resolution of the above concerns in addition to obtaining responses to a request for additional information regarding the other events in the report would be needed to complete the PTS review for the Yankee Nuclear Power Station.

The discussion of the scope of the Idaho National Engineering Laboratory (INEL) review is described in Sect. B.2. A discussion of the major concerns regarding the review of the thermal hydraulic analyses contained in the YAEC report is presented in Sect. B.3, while the conclusions are given in Sect. B.4. Attachment A presents a Request for Additional Information (RAI) which would be needed to complete this review. Under normal review circumstances, the Technical Evaluation Report would be written upon receipt of the responses to the RAI. However, due to the limited review schedule and the unavailability of time to respond to the questions, the RAI is included as part of this evaluation.

B.2 Scope of INEL Review

The scope of the INEL effort consists of reviewing the accident analyses contained in the YAEC document No. 1735 entitled "Reactor Pressure Vessel Evaluation Report." The details of the review are summarized below.

The thermal hydraulic portion of the YAEC Report No. 1735 describing the accident analyses contained in Sect. 6.6 entitled "Thermal-Hydraulic Analyses for Representative Sequences" was reviewed. *Regulatory Guide 1.154* entitled "Format and Content of Plant-Specific Pressurized Thermal shock Safety Analysis Reports for Pressurized Water Reactors" was used as guidance for the review.

The sequences or accidents presented in the YAEC Report No. 1735 were reviewed to determine the technical adequacy and acceptability of the analyses. The review addressed the following major areas:

1) The RETRAN methodology — The methods were reviewed to assure the code used in the transient analyses properly treats the thermal and hydraulic behavior for application to PTS events.

2) Input model — A limited review of the nodal model was performed to evaluate the adequacy of the input model. Sensitivities of the input model such as nodalization of key reactor system coolant components, various input information including wall-to-coolant heat transfer coefficients, and initial conditions were evaluated.

3) Transients — The transients presented in Sect. 6.6 of the YAEC report were reviewed for their technical adequacy with consideration of items 1 and 2 above.

4) Completeness — The thermal hydraulic accident analyses were reviewed to assure that the limiting transient had been identified with justification provided to demonstrate that the worst case produced the minimum temperature and maximum pressure condition. Of particular importance is that the worst case initial conditions and appropriate operator actions and equipment/system responses have been properly accounted for in the spectrum of transient events. As such, a review of the key systems/equipment and operator actions from the appropriate Emergency Operating Procedures was also performed.

This effort does not include a review of the methods and models used to compute the mixing of the fluid in the injection section and downcomer regions of the vessel.

Following the initial review of YAEC Report No. 1735, additional supplemental information was requested in order to complete the review of the accident analyses. The request for this supplementary information was transmitted to the YAEC and included.

B.2.1 Materials Needed to Complete Transient Thermal Hydraulic Review

The YAEC reports identified as Refs. 6.6.5, 6.6.6, and 6.6.7 in YAEC Report No. 1735 dated July 1990.

YAEC Yankee Emergency Operating Procedures that address LOCA, overcooling events, and steam generator tube rupture.

Thermal hydraulic accident safety analysis sections of the Yankee FSAR describing the results of the LOCA, overcooling, and steam generator tube rupture events. These results should be applicable to the current plant cycle.

Detailed system descriptions for the following:

a. ECCS (high and low pressure safety injection pumps, accumulators) and any other injection systems which can deliver coolant to reactor coolant system. Provide head vs flow curves for injection systems and description of accumulators (cover gas pressure, elevation head, and tank liquid inventory).

b. Pressurizer, spray and heater systems, geometry of the internals, and the level control system.

c. Steam generator secondary safety relief valve pressures and capacities, ADV steam flow capacity and rated conditions, main/auxiliary/emergency feedwater system flows, and secondary inventory at 100%, 50% power, and HZP.

Because only RCS pressure and downcomer temperature were provided for each of the transients presented in Sect. 6.6 of the YAEC report, the information is insufficient for performing a thorough review of the thermal and hydraulic system response to these events. To facilitate the proper review of the transients, please provide the following plot information for each of the transients (including all cases for each event) discussed in Sect. 6.6:

a. steam generator pressure and liquid mass;
b. feedwater mass flow rate and secondary break mass flow rate;
c. total SI mass flow, break mass flow, and quality (include PORV flow and quality if appropriate);
d. pressurizer two-phase level, steam temperature, liquid temperature, and wall temperature;
e. upper head and upper plenum void fraction and fluid temperatures;
f. discharge leg and hot-leg mass flow rates, qualities, and temperatures;
g. core inlet, average, and outlet temperatures;
h. core inlet/outlet mass flow rates and qualities;
i. RETRAN parameters used as input to the mixing calculations if not included in above plots;
j. please provide RCS pressure and downcomer temperature for those cases where the information was not provided in the report.

Several statements were made regarding "previous" or "past" analyses in Sect. 6.6, but no references were provided. Please provide the documents describing the previous analyses. These include references to previous or past analyses on

- pp. 6-171, last paragraph regarding LOCA analyses.
- pp. 6-172, last paragraph in regards to the SGTR event.
- pp. 6-177, third paragraph regarding second MSLB case.
- pp. 6-180, first paragraph for sixth MSLB case.

Of the above information requested, only the following information was reviewed for this review:

a. topical report describing the RETRAN methodology;
b. the Yankee Emergency Operating Procedures; and
c. a YAEC submittal for the Reanalysis of the Main Steam Line Rupture Event - Cycle XVI, dated June 10, 1983.

Using the above materials and the results of the analyses of the thermal hydraulic events presented in the YAEC report, a Request for Additional Information (RAI) needed to complete the review effort is listed below. This information is normally evaluated prior to issuance of a TER; however, in view of the schedular constraints and the limited time within which the Utility can respond to such requests, the itemized list of questions is therefore contained in this report. The RAI is presented in the following section.

B.2.2 INEL Request for Additional Information

From a review of Sect. 6.6 of YAEC Report No. 1735, dated July 1990, additional information was identified that is needed to compete the assessment of the thermal hydraulic events contained in the report. The RAI is listed in Attachment A.

B.3 Review Findings and Discussion of Major Concerns

With consideration to the questions discussed in Attachment A, the major issues regarding this review include:

a) justification for the limiting small break LOCA;
b) isolation of a small break LOCA; and
c) treatment of pressurizer nonequilibruim thermodynamics.

The above major issues are discussed in detail below.

B.3.1 Justification for the Limiting Small Break LOCA

Insufficient information was presented in the report to justify that the 1-5/16-in. break is the most limiting break for PTS considerations. Furthermore, for break sizes ~2 in. and smaller, the RCS will need to be cooled down to shutdown cooling conditions. The operator procedures instruct the operators to initiate a cooldown to RHR conditions during a small break LOCA. During the cooldown the RCS will refill and repressurize quickly to a pressure where ECC injection flow into the RCS equals the flow out the break. Thus, as break size decreases, the refill will occur earlier in time and produce higher pressures after repressurization. The larger break sizes will refill and repressurize at lower temperatures but will repressurize to lower pressures than that for the smaller breaks. An analysis of the spectrum of breaks which experience refill and repressurization is expected to produce the RCS pressure responses illustrated in Fig. B.1. An evaluation of these break conditions is identified for the PTS evaluation for those breaks that refill and repressurize. Also, performing a cooldown will increase ECC flow into the RCS and result in potentially lower downcomer temperatures than that for the 1-5/16-in. break presented in the report.

B.3.2 Isolation of a Small Break LOCA

The possibility of a small break occurring that can be isolated during the event was also not discussed. The maximum break size that can be isolated was not presented nor discussed in the report. This worst break that can be isolated needs to be compared to the limiting small break LOCA that results in refill and repressurization of the RCS from item 1) above to assure the worst break has been analyzed. Also discuss the potential for the ECC and charging systems to pressurize the RCS should the RCS become refilled with ECC water after isolation.

B.3.3 Treatment of Pressurizer Nonequilibrium Thermodynamics

The RETRAN treatment of the pressurizer during insurges following refill of the RCS includes a two-region representation of the pressurizer. The upper region contains steam while the lower region accommodates the liquid. The RETRAN code allows one to model heat transfer between (1) the steam and the upper walls of the pressurizer and (2) between the upper steam and lower liquid regions. The YAEC modeled the heat transfer between the upper steam and lower liquid regions only using a heat transfer coefficient of 50 Btu/h·ft^2·°F. Because this method may not be representative of the actual heat transfer mechanisms that occur in the pressurizer during insurges, justification that this approach bounds the actual behavior in the pressurizer is needed. During insurges the pressur-izer will accumulate liquid thereby compressing the upper steam region which superheats. The dominant mechanism that controls peak pressure during insurges is therefore the pressurizer wall surface area in contact with the steam and the temperature difference between the walls and steam. Because the steam is nearly stagnant, the heat transfer coefficient is expected to be about 5–10 Btu/h-ft^2-°F. Because the surface of the liquid in contact with the steam quickly saturates, a thermal layer or barrier is created which insulates the upper steam region from the lower region containing the liquid. After several feet of liquid accumulates in the pressurizer, mixing near the surface becomes diminished and the upper steam region can be considered to be thermally insulated from the liquid for the remainder of the insurge. In view of these considerations, the YAEC method of modeling the heat transfer between the steam and liquid regions may be noncon-servative. Furthermore, modeling the lower liquid region as a single region presupposes perfect mixing in this region which also artificially lowers the liquid temperature as fluid is added during the insurge. As such, the use of a rather high heat transfer coefficient between the steam and liquid regions, coupled with an artificially low mixed mean temperature for the liquid, could result in lower peak pressures calculate for the PTS transients that experience refill. A more appropriate model would include a three region pressurizer consisting of two lower liquid regions and an upper steam region. In view of the YAEC modeling techniques, justification that the heat transfer coefficient of 50 Btu/h-ft^2-°F and use of two regions bounds the actual or expected behavior needs to be provided.

Lastly, the upper head region should also be modeled as a nonequilibrium region to properly treat the refill and repressurization process.

Based on the above concerns, the thermal hydraulic analyses presented in the YAEC report are not acceptable for use in assuring the worst case has been identified for PTS of the Yankee Nuclear Power Station. Resolution of the above concerns in addition to obtaining responses to the Request for Additional Information, presented in Attachment A regarding all of the events presented in the report, would be needed to complete the PTS review for the Yankee Nuclear Power Station.

B.4 Conclusion

A review of the thermal-hydraulic analyses presented in the YAEC Report No. 1735 was performed to evaluate the technical approach used as a basis to address Pressurized Thermal Shock for the Yankee Nuclear Power Station. The thermal/hydraulic analyses included the following events:

1) main steam line break (5 cases);
2) excessive feed flow (2 cases);
3) small break LOCA (1 case).

Because only the reactor coolant system pressure and downcomer temperature responses were provided for the above events, there was insufficient information regarding the other key primary and secondary system transient response parameters to permit a thorough and proper review. A review of the information provided in the YAEC report, however, identified several major concerns which will require resolution. These concerns included the following:

1) The pressurizer nonequilibrium model used in the analyses did not properly account for the heat transfer governing the thermal conditions in this region during refill and repressurization of the RCS. As a consequence, the approach used in modeling the thermal behavior in the pressurizer may tend to overpredict heat removal from the pressurizer steam region and underpredict peak pressure during refill. The effect of this nonconservative pressurizer modeling technique on those events which experience pressurization needs to be evaluated to demonstrate that the approach does not adversely affect the results nor change the conclusions presented in the report.

2) The justification was insufficient to demonstrate that the small-break LOCA presented in the report is the worst case for PTS considerations. The analyses of a spectrum of breaks needs to be provided to demonstrate that the minimum temperature and maximum pressure response for the break presented in the report bounds that for a spectrum of break sizes.

3) The maximum break size which can be isolated was not presented nor discussed in the report. Since the Emergency Operating Procedures do not prevent the operator from isolating the break, the response for this event should also be included for PTS evaluations. The operation of the ECC and charging systems following isolation should also be discussed in regard to the potential for additional pressurization of the RCS.

Based on the above concerns, the thermal hydraulic analyses presented in the YAEC report are not acceptable for use in assuring the worst case has been identified for PTS of the Yankee Nuclear Power Station. Resolution of the above concerns, in addition to obtaining responses to the Request for Additional Information regarding the other events in the report, is needed to complete the PTS review for the Yankee Nuclear Power Station.

Attachment A to Appendix B

Request for Additional Information

General Questions

On pp. 6-167, two criteria for evaluating cool-down events are identified. In regard to criterion 1), provide justification that transients with a cooldown rate less than 200°F/h need not be considered for PTS evaluations. For example, a cooldown rate slightly <200°F/h may result in a higher pressure/low temperature combination that is more limiting than that for the cases which are strictly limited to a cooldown of 200°F/h or more. This condition could occur following isolation of, or refill of, the Reactor Coolant System (RCS) following a small break.

Please provide the "previous analyses and engineering simulations" identified in item 3) on pp. 6-169.

During discussions between the INEL (L. Ward), the NRC (M. Mayfield), and the YAEC (P. Bergeron), the thermal hydraulic analyses used the RETRAN nonequilibrium two-region model in the pressurizer. A heat transfer coefficient of 50 Btu/h-ft^2-°F was used to model heat transfer between the upper steam region and the lower liquid region. No heat transfer was modeled between the pressurizer walls and upper steam region. During insurges of liquid into the pressurizer, the liquid will compress the steam causing the steam to superheat. The pressurizer walls in contact with the steam will act as a heat sink and influence the peak pressure achieved during the insurge. Because the steam is basically stagnant, heat transfer coefficients between the steam and pressurizer walls is of the order of 5-10 Btu/h-ft^2-°F. Because the steam is basically stagnant, heat transfer between the steam and liquid regions will cause a saturated layer to develop at the steam-liquid interface, the steam region will quickly become insulated from the lower liquid region. As such, there is very little or no heat transfer between the liquid and steam regions. The YAEC approach is therefore considered nonconservative since the model will have a tendency to overpredict heat removal from the upper steam region which will result in lower peak pressures computed during events where the RCS refills. Also, the use of single lower liquid region further acts to reduce peak pressure since any fluid entering this region will be perfectly mixed throughout the liquid region regardless of the amount of liquid in this region. This single region representation of the lower liquid region therefore will minimize the lower liquid region temperature and further enhance the heat removed from the upper steam region which produces lower peak pressures. This modeling technique is considered incorrect. More importantly, because of the nonconservative nature of the approach, additional justification is needed to assure the use of this approach bounds the expected thermal behavior of the pressurizer for those events which experience refill of the RCS. The following information is needed:

a) Please provide benchmarks justifying the ability of that model to predict pressurizer nonequilibrium behavior during liquid insurges and outsurges. Both separate effects and integral tests should be provided. Comparisons to plant data should also be provided if available.

) Show the effect of the use of the interfacial heat transfer coefficient of 50 Btu/h-ft^2-°F and the single lower liquid region representation on peak pressure predictions for a) above. Insurge transients with a range of inlet temperatures and liquid inventories similar to that expected for the Yankee plant should be provided.

Please describe the nonequilibrium thermodynamic modeling of the remainder of the reactor coolant system other than the pressurizer? Was the upper head of the reactor vessel modeled assuming nonequilibrium thermodynamics? If not, explain why nonequilibrium thermodynamics is not important to the repressurization process when many of the transients can develop a steam bubble in this region following refill of the RCS.

Describe the RETRAN nonequilibrium modeling of the fluid in the loop piping during injection. While perfect mixing of the ECC injection acts to enhance depressurization through condensation in the injection section, the addition of cold, relatively unmixed ECC fluid which inters the core region may reduce boiling and have a more significant effect on depressurization (further increasing ECC flow) and minimum temperature. Identify the RETRAN calculated parameters used in the REMIX code and the EPRI mixing model.

Loss-of-Coolant Accident

Provide justification that the 1-5/16-in. break is the limiting break size for PTS evaluations. Since larger break sizes can result in lower temperatures, provide the results of larger break sizes to show that combinations of minimum temperature and maximum pressure for these larger break sizes are bounded by the 1-5/16-in. break. Smaller breaks which require cooldown to shutdown cooling conditions and which will experience repressurization during the event should also be discussed. Since the EOP's do not identify when cool-down should be initiated if RCS pressure is high (i.e. >835 psig), the earliest time into the event that the operators would initiate a cooldown should be assumed and cooldown should be at the maximum allowable rate.

What is the maximum break size that can be isolated and what is the minimum temperature that could be achieved for this break? If such a small break LOCA is isolated just prior to refill, is there sufficient time for the operators to throttle ECC and charging flow to prevent RCS pressure from returning to full power operating pressure?

What operator actions are assumed in the LOCA analyses? How do these actions affect minimum temperature and maximum pressure achieved during LOCAs?

What systems can operate following a LOCA to minimize RCS temperature and maximize RCS pressure? Are let-down and auxiliary emergency spray systems available and could the pressurizer heaters actuate upon recovery of pressurizer level upon refill of the RCS by the ECCS and/or charging pumps?

Provide plots of the following for the 1-5/16-in. break:

a) pressurizer level;
b) hot and cold leg two-phase levels, flow rates, qualities, and temperatures;
c) total ECC mass flow rate, break mass flow rate, and quality;
d) steam generator pressures and levels;
e) upper head two-phase level and fluid temperatures; and
f) core void fraction.

Figure 6.6-4 presents downcomer pressure for the 1-5/16-in. break. If the operator initiated a cooldown using the steam generators at 15 min into the event, could the increased ECC addition result in lower downcomer temperatures than that presented in Fig. 6.6-5 and then upon refill of the RCS, could system pressure increase above that shown at the end of the pressure plot of Fig. 6.6-4? The analysis should be presented out to the time refill occurs and where the break flow equilibrates with injection flow. The results of the additional breaks requested above should also be carried out for this refilled condition.

Please describe the wall-to-coolant heat transfer model used for the primary system. Identify the regions that were modeled, the wall-to-coolant heat transfer coefficients, and the wall nodalization used for the conduction solution.

What is the earliest time the operators would initiate a cooldown of the RCS following those small break LOCAs where heat removal is needed? Please describe the method for cooldown of the RCS following a small break LOCA, and the precautions taken by the operator to prevent overpressurization of the system when the RCS has been cooled to shutdown cooling entry conditions.

What is the temperature of the ECC and charging water injected by the pumps and accumulators used in the LOCA analyses? What is that minimum allowable temperature of the ECC water source? What is the minimum temperature of the charging flow?

Please provide justification that the HZP condition is the worst initial condition for LOCA PTS evaluations. Please explain why all other modes of operation are not more limiting for PTS considerations?

Please explain the method used to cool the plant to shutdown cooling conditions following a small break LOCA with hot water in that pressurizer and a bubble and hot water in the upper head? Does the potential for RCS pressure behavior impact PTS as the operator attempts to reduce RCS pressure to shutdown cooling conditions by throttling ECC flow while also maintaining the minimum subcooling.

Provide justification that the suction leg break location is the worst location for this break? Include breaks in the hot leg piping in the justification.

Figure 6.6-5 shows the temperature decreasing at the end of the analysis. Please provide the remainder of the analysis showing the time at which the temperature reaches a minimum.

Steam Generator Tube Rupture

Please provide the analyses (referred to on pp. 6-112) that justifies that the most severe cooldown for the first 10 min of a tube rupture event occurs for a single guillotine tube rupture.

Please explain why pressure stabilizes at 1250 psia.

Was the tube rupture analysis carried out to the establishment of shutdown cooling? In particular, the plant must be cooled to shutdown cooling conditions for long term heat removal. Please demonstrate that during the cooldown the operator is able to maintain subcooling margin and not repressurize the RCS at the low temperatures necessary to initiate shutdown cooling. What precautions are taken to prevent inadvertent repressurization early in the event and late in the event when low temperature conditions are met for entry into shutdown cooling.

Please describe the initial conditions for the tube rupture analysis.

Opening of Secondary System Steam Valves

What are the initial conditions for the secondary valve opening transients? Identify all control systems that are active during these events. Also identify the operator actions for each event.

Why was the addition of feedwater precluded for these events?

Provide the basis for assuming the NRV closes for the opening of a single high-set MSSV but fails in the other valve closure events? What is the minimum temperature achieved if the NRV does not close, with and without feedwater addition? Please justify that omission of this event or provide the results of the analysis.

For each of these cases the cooldown rates were cited as a maximum "expected" cooldown rate; are these engineering judgements or are these conclusions based on calculations with the RETRAN code? Please explain.

What conditions are necessary for the operators to trip the main coolant pumps? What is the impact on these events (and the above requested event involving non-closure of the NRV) if the operator trips the main coolant pumps?

Main Steam Line Break

Please provide the following plot information for each of the steam line break cases:

a) steam generator pressure and liquid mass;
b) feedwater mass flow rate and break mass flow rate (include primary break information for LOCA and opened PORV);
c) SI flow;
d) pressurizer two-phase level, steam temperature, liquid temperature, and wall temperature;
e) upper head and upper plenum void fraction and fluid temperatures;
f) cold and hot leg loop mass flow rates, qualities, and temperatures;
g) core inlet, average, and outlet temperature;
h) core inlet and outlet mass flow rate;
i) RETRAN parameters used as input to the mixing calculations if not included in above plots; and
j) please provide RCS pressure and downcomer temperature for those cases where the information was not provided in the report.

Case 1 assumed a guillotine break of the 24-in. steam line. What discharge coefficient was used for case 1? What break size and discharge coefficient were assumed for the other cases? How was the break region nodalized? What critical flow model is included in RETRAN and how does the code model break flow that is not critical flow?

Please provide a list of operator actions assumed for each of the events.

Provide justification for not assuming additional NRV valve failures for case 1 when 2 and 4 NRV valves were assumed in the other cases? Are the choice of equipment failures and initial conditions best estimate or are they considered worst case assumptions.

List the minimum temperature and maximum pressure for each case that was used for PTS evaluation. What was the worst case? For example, case 6 included a minimum temperature of 140°F with pressures of 1550 and 1000 psia identified. What pressure was used in the PTS evaluation? Was case 6 carried out through refill and repressurization of the RCS? What is the size of the LOCA? What conditions are needed for the pressure to remain at 1550 psia for this case? What is the impact on this event of closure of the PORV when the downcomer is at its minimum temperature? What assumptions were made in regard to charging system operation?

How was the pressurizer level control system modeled?

On pp. 6-177, what does "minimal" feed mean?

The minimum temperature for case 4 is based on the emptying of the condenser hot well after which MCS temperature would begin to increase. What actions would be required to prevent the hot well from emptying or MCS temperature to increase at 7.5 min and if such conditions are possible, what minimum temperature would be achieved for this situation?

Case 5 shows the temperature in Fig. 6.6-16 decreasing at the end of the plot and it was stated to continue to decrease thereafter. Either carry out the analysis until temperature begins to increase or identify the minimum temperature with the EPRI mixing motel?

Feedwater

Please provide the information requested under Main Steamline Break.

Steam Generator Blowdown

Please provide the information requested under Main Steamline Break.

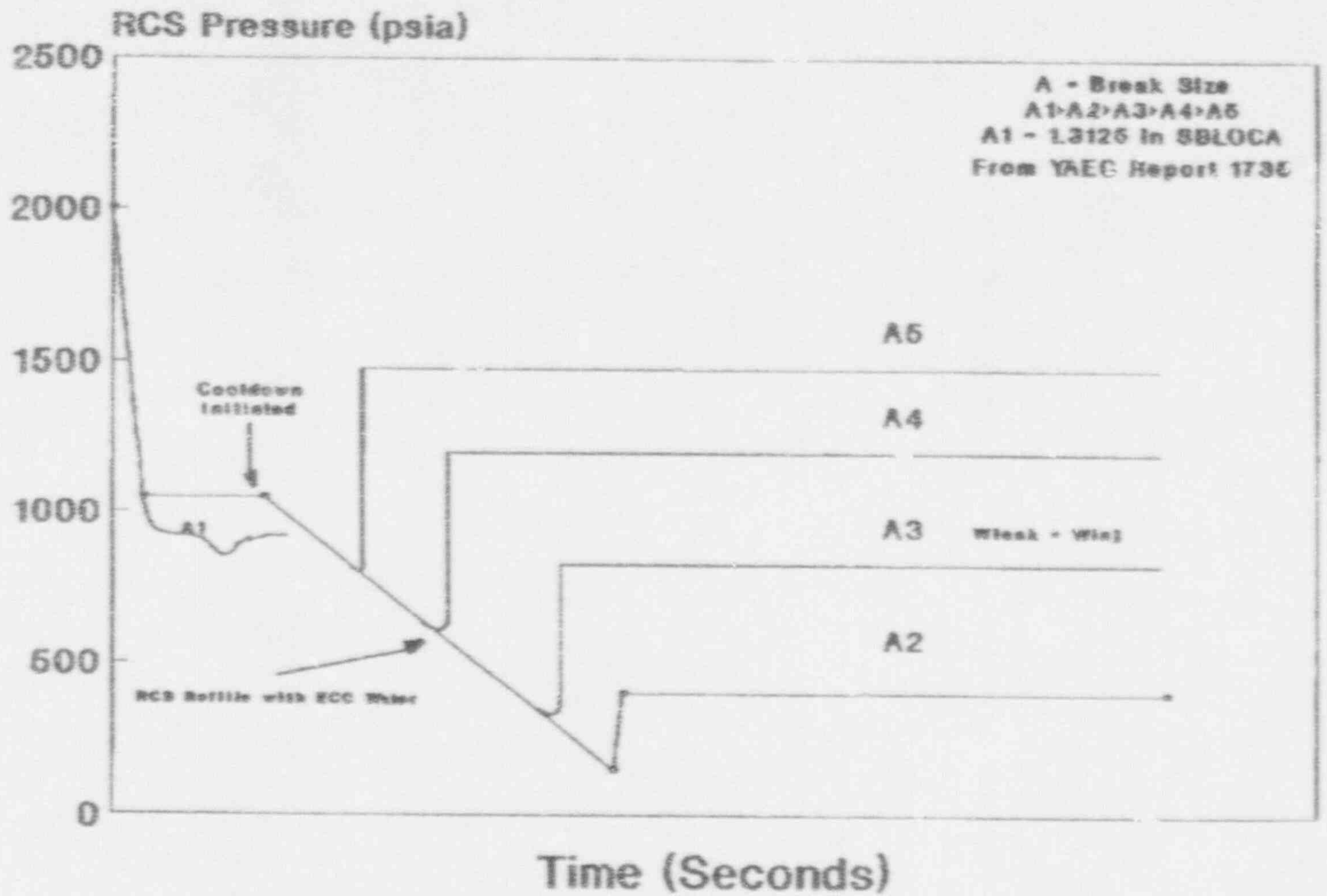

Fig. B.1. SBLOCA — Expected RCS pressure response.

Appendix C

ORNL Review of YAEC 1735 Radiation Effects on RT_{NDT} and Charpy Upper-Shelf Energy

Contents

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Appendix C

ORNL Review of YAEC 1735 Radiation Effects on RT_{NDT} and Charpy Upper-Shelf Energy

J. G. Merkle
R. K. Nanstad

C.1 Introduction

The Yankee Atomic Electric Co. (YAEC) report[1] includes detailed information regarding the materials, fluence estimates, surveillance data, and operating information as well as their analysis of the current and projected RT_{NDT} and Charpy upper-shelf energy for each material. Substantial differences existed between the original YAEC estimates of RT_{NDT} and those of the NRC staff and consultants. For the plates, the differences resulted primarily from the YAEC assertion that the A 302 grade B plates are coarse grained and, therefore, not sensitive to irradiation temperature in the range from 550 to 500°F, and that the coarse grain microstructure also mitigates the potentially embrittling effects of nickel on the lower plate. Regarding the welds, the chemical compositions are unknown and YAEC assumed that the copper content (0.18%) and nickel content (0.70%) are the same as those of similar welds in the Belgian BR3 reactor vessel fabricated by Babcock and Wilcox (B&W) in the same time frame as the Yankee vessel. Since Ref. 1 was issued, discussions between YAEC and NRC have led to convergence of the two organizations' estimates of irradiated RT_{NDT} values.

There are many factors contributing to the uncertainties regarding the fracture toughness of the Yankee reactor vessel. Among these are the relatively low operating temperature (~500°F), a slight amount of surveillance data, effects of grain size and nickel content, and lack of chemical composition data. Each of these will be discussed. The two toughness parameters of interest relative to the pressurized thermal shock (PTS) evaluation are the reference temperature (RT_{NDT}) and the Charpy upper-shelf energy. The relationship between low Charpy upper-shelf energy and fracture toughness is also discussed.

C.2 Composition of Linde 80 Welds

The compositions of the Linde 80 welds in the Yankee vessel are not known. It is known that they were fabricated with copper-coated wire and Linde 80 welding flux. The YAEC proposal is to assume a copper content of 0.18% and nickel content of 0.70%, the same as those reported for the BR3 reactor vessel. The justification is that the vessels were fabricated about the same time and would likely have similar chemistry. Although that is acceptable for nickel because of known specifications for nickel additions, that justification should be rejected for copper because the copper content in the welds is a somewhat uncontrolled combination of that from the steel used to draw the welding wire itself and that from the copper coating. It was not an element controlled by material specification. The copper content of Linde 80 welds can be quite variable as shown by a series of such welds fabricated by Babcock and Wilcox (B&W). The copper contents for the welds in that study varied from 0.15 to 0.49%, with an overall mean of 0.29% and a standard deviation of 0.07%).[2] Recent chemical analyses of samples from the Midland Unit 1 reactor vessel have revealed copper variations from 0.21 to 0.46%, with an overall mean of 0.29 wt% and a standard deviation of 0.07%, and all the welds were fabricated with the same heat of weld wire and lot of welding flux.[a] The variation in copper, then, can be very large even within one wire/flux combination. The following guidance is provided in *Regulatory Guide 1.99*, Rev. 2:

> In Tables 1 and 2 "weight-percent copper" and "weight-percent nickel" are the best estimate values for the material, which will normally be the mean of the measured values for a plate or forging or for weld samples made with the weld wire heat number that matches the critical vessel weld. If such values are not available, the upper limiting values given in the material specifications to which the vessel was built may be used. If not available, conservative estimates (mean plus one standard deviation) based on generic data may be used if justification is provided. If there is no information available, 0.35% copper and 1.0% nickel should be assumed.

The above guidance was the basis for using the generic data for Linde 80 welds, discussed above, to establish

[a]Nanstad, R. K., McCabe, D. E., and Swain, R. L., *Variations in RT_{NDT} and Chemical Composition for the Midland Unit 1 Reactor Vessel Low Upper-Shelf Welds*, draft NUREG/CR report in preparation.

0.35 wt% as the <u>best estimate</u> (=conservative estimate) of copper content in the Yankee vessel welds for Regulatory Guide applications (note that the calculated estimate would be 0.29 + 0.07 = 0.36%, but the Regulatory Guide uses 0.35% even when no information is available).

C.3 RT_{NDT} Considerations

C.3.1 Summary of RT_{NDT} Estimates

Fracture safety margin assessments for the Yankee Rowe reactor pressure vessel depend directly on estimates of RT_{NDT} shift for the different regions of the vessel. Referring to Fig. C.1, from the YAEC report, the important regions of the vessel, with regard to irradiation effects, are the upper plate, lower plate, upper axial weld, lower axial w[illegible] circum-ferential weld. The only surveilla[illegible] the [illegible]nkee vessel was material from [illegible] data from Yankee capsule[illegible] Research Laboratory (NRL) [illegible] Yankee surveillance specimens were [illegible]diated in the Belgian BR3 reactor and the data are re[illegible]rted in Table 5.7 and Figs. 5–6 and 5–10 of Ref. 1.

As stated in Refs. 3 and 4 and noted by Hiser,[6] the surveillance material was heat treated separately from the vessel itself. This apparently led to a difference between the B&W Charpy impact data at +10°F for the unirradiated vessel material and data obtained by Westinghouse and NRL for the unirradiated surveillance material, as illustrated in Fig. C.2 from Ref. 1. The Westinghouse data in Fig. C.2 are WAPD data from Fig. 2 of Ref. 5. The NRL data in Fig. C.2 can be found in Fig. 8.11 of Ref. 4 and Fig. 5 of Ref. 5. The utility's estimate of the initial RT_{NDT} for the upper plate is based on the average Westinghouse and individual B&W Charpy impact energy values at +10°F (see Table 5.4 of Ref. 1). Apparently referring to Para. B.1.1.4 of NRC Branch Technical Position[7] MTEB 5-2, which is not a conversion from longitudinal to transverse orientations as implied on p. 5–3 of Ref. 1, the utility added 20°F to +10°F to get RT_{NDT_0} = +30°F for the upper plate. This ignores individual Charpy values at +10°F which are less than 30 ft–lb (not specifically mentioned in MTEB 5-2) but it does assume, conservatively, that the surveillance and upper vessel plates are metallurgically identical.

It was noted by Serpan and Hawthorne[5] that NRL performed drop weight tests on the unirradiated Yankee surveillance material, providing an initial NDT temperature of +10°F. Referring to Fig. C.2, it appears that, ignoring material differences,[6] RT_{NDT} might be controlled by the temperature at which CVN = 50 ft–lb. Nevertheless, recognizing the possible initial metallurgical difference between the upper vessel and surveillance plates and then applying MTEB 5-2 to the B&W data at +10°F only, Hiser[6] determined that an initial RT_{NDT} of +10°F could be justified for the upper plate.

The initial RT_{NDT} for the lower plate was estimated by the utility[1] and by Hiser[6] as +30°F, based on applying MTEB 5-2, Sect. B.1.1.4 to the Charpy impact energy data at +10°F tabulated in Table 5.5 of Ref. 1.

The initial RT_{NDT} for the Linde 80 weld metal in the Yankee vessel was estimated by the utility as +10°F based on B&W test report sheets for material related to the Yankee vessel in an unspecified way (see pp. 5–5 and 5–28 of Ref. 1). Hiser[6] checked this estimate using generic data (see pp. 2 and 3 of the attachment to Ref. 5). Hiser[6] also applied Branch Technical Position MTEB 5-2 to Charpy data at +10°F for the upper and lower vessel welds, obtained from Ref. 8, again obtaining an initial RT_{NDT} of +10°F (see p. 11 of the attachment to Ref. 6). In the latter evaluation, the MTEB 5-2 lower limit of 45 ft–lb was changed to 30 ft–lb because the welds should be isotropic.[a] Note that the value of RT_{NDT_0} for the welds is 10°F higher than the value for Linde 80 welds specified in Para. 50.61(b) of 10CFR50.

Estimates of ΔRT_{NDT} for the Yankee vessel near-beltline materials have evolved since the submittal of Ref. 1. In Ref. 1, the utility developed and applied graphically a trend curve for the plate materials based on Yankee Rowe and BR3 surveillance data (see pp. 5–26 and Fig. 5–6 of Ref. 1). In Ref. 1, it was assumed that coarse-grained structure nullifies the effects of irradiation temperature for both plates and the effect of nickel for the lower plate. *Regulatory Guide 1.99* (Rev. 2) (all further reference to *Regulatory Guide 1.99* will mean to Rev. 2) was used for estimating ΔRT_{NDT} values for the welds, using an irradiation temperature adjustment obtained from a draft of ORNL/TM-10445 (see pp. 5–26 and 5–38 of Ref. 1). Note that the draft[b] of ORNL/TM-10445 is a difficult-to-read and out-of-date document. It was assumed by the utility, without complete documentation, that the copper and nickel concentrations in the Yankee welds were the same as those measured in BR3 welds fabricated by B&W at about the same time as the Yankee vessel. The NRC accepted the Yankee estimate of nickel concentration, because it was a controlled element in the weld wire, but not the copper concentration, because it was not a controlled element.[c]

After reviewing Ref. 1, NRC made independent preliminary estimates[d] of the ΔRT_{NDT} values for the Yankee

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[a] Hiser, Jr., A. L., NRC, personal communication to J. G. Merkle, ORNL, August 23, 1990.

[b] Fabry, A., et al., *Influence of Neutron Irradiation on the Notch Ductility of LWR Welds*, NUREG/CR-4940 (ORNL/TM-10445), Draft Manuscript, August 20, 1987.

[c] Hiser, Jr., A. L., NRC, personal communication to J. G. Merkle, ORNL, October 4, 1990.

[d] Hiser, Jr., A. L., draft of Ref. 6, undated.

Rowe vessel materials and also retained a consultant, G. R. Odette, to do the same thing. The preliminary NRC estimates were all based on *Regulatory Guide 1.99* procedures, with multiplicative adjustments for irradiation temperature and nickel. The estimates for the plate materials were of two types, the first incorporating chemistry factors calculated on the basis of known plate chemistries, and the second incorporating chemistry factors calculated by the method of least squares from the Yankee surveillance data following the procedure described in *Regulatory Guide 1.99*. The BR3 surveillance data were not considered in this calculation. The surveillance-based calculations used two sets of fluence values, different by a factor of two, because of a YAEC claim that errors had occurred in the original fluence calculations. The estimates for the welds were made on the basis of calculated chemistry factors for two chemistries, the *Regulatory Guide 1.99* default chemistry and the BR3 weld chemistry claimed by YAEC to represent the Yankee vessel welds. Upper-shelf-drop estimates were also made by the *Regulatory Guide 1.99* procedure with no adjustment for irradiation temperature, assuming that compensation is provided[6] by the use of J-R curves measured at 500°F. In contrast to the YAEC estimates in Ref. 1, the preliminary NRC estimates indicated that most if not all the near-beltline material RT_{NDT} values exceeded the 10CFR50 PTS screening criteria. The YAEC estimates of Charpy upper shelf energy values less than 50 ft–lb were also confirmed.

Odette's estimates[9] of ΔRT_{NDT} were based on a study of available data for irradiation temperatures near 500°F, nickel effects and a log–log plot of both the Yankee Rowe and the BR3 surveillance data, the latter adjusted for irradiation temperature effects. Odette's ΔRT_{NDT} estimate for the upper plate was based on a linear interpolation (on log–log paper) between the two YAEC surveillance points, using the originally reported fluences, according to

$$\Delta RT_{NDT} = 184.57 f^{0.3419}, \ {}^\circ F,^{a} \qquad (1)$$

where $f = \Phi \times 10^{-19}$ n/cm^2. Using f = 2.3,[b] $\Delta RT_{NDT} \approx 245$°F. The ΔRT_{NDT} estimate for the lower plate was obtained by adding a +80°F nickel adjustment to the value for the upper plate, ignoring the differences in fluence between the upper and lower plates, to obtain ΔRT_{NDT} = 325°F. The ΔRT_{NDT} estimates for the axial and circumferential welds were obtained from a generic upper-bound *Regulatory Guide 1.99*-type curve for the 500°F irradiation data examined, according to

$$\Delta RT_{NDT} = 300\, f^{(0.28 - 0.10 \log_{10} f)}, \ {}^\circ F . \qquad (2)$$

For the axial weld, f = 0.38[a] and ΔRT_{NDT} = 220°F (Odette reported 230°F), and for the circumferential weld, f = 2.05[a] and ΔRT_{NDT} = 359°F (Odette's value was rounded up to 360°F). Odette's ΔRT_{NDT} results[9] were generally less than the preliminary NRC values but still confirmed that at least the lower plate and the circumferential weld have exceeded the PTS screening criteria.

Following the receipt of Odette's estimates, Hiser's calculational procedures were revised so that the two sets of estimates were closer together. Hiser's[6] final ΔRT_{NDT} estimates for plate material were based on only the BR3 surveillance data to avoid the controversy about YAEC surveillance capsule fluence accuracy. The multiplicative adjustment for irradiation temperature was replaced with an additive adjustment based on 1°F/°F, and the high nickel content of the lower plate, relative to that for the upper plate surveillance specimens, was accounted for by adding 70°F to the upper-plate correlation for ΔRT_{NDT}. The reference irradiation temperature was lowered from 511°F to 500°F, somewhat arbitrarily, thus raising the irradiation temperature adjustment by 11°F. (Time and fluence-weighted average cold leg temperatures based on Tables 2.1 and 2.3 of Ref. 1 produce reference temperatures of 507.1 and 504.8°F, respectively, the combined average of which is 506°F.) Recognizing that a concave downward *Regulatory Guide 1.99* fluence function curve produces higher ΔRT_{NDT} estimates than a straight line on a log–log plot, for fluences in the range of interest (see Fig. C.3), Hiser made both types of estimates. The latter was based on a linear least squares fit on log–log paper to the five BR3 surveillance specimen results for fluences exceeding 10^{19} n/cm^2 (see Table 5.7 of Ref. 1), with an irradiation temperature adjustment to the data before fitting.[6] The resulting shift equation was

$$\Delta RT_{NDT} = 172.16 f^{0.3160}, \qquad (3)$$

the constants in which are close to those in Eq. (1). The revised ΔRT_{NDT} estimates for the welds were made by the *Regulatory Guide 1.99*, Revision 2, procedure for three chemistries, *Regulatory Guide 1.99* default (0.35% Cu, 1.0% Ni), BR3 (0.18% Cu, 0.7% Ni), and "best estimate" (0.35% Cu, 0.7% Ni), the latter chemistry corresponding to the 10CFR50.61 "best estimate" values of ΔRT_{NDT}. Chemistry and fluence factors were determined from *Regulatory Guide 1.99*. An irradiation-temperature adjustment of 50°F was added to the calculated shift. The values labeled "best estimate" could more accurately be termed a "prudent estimate," the conservatism in which provides

[a]This correlation is slightly different from that shown in Ref. 9 because the one shown in Ref. 9 was fitted to BR3 as well as the YAEC surveillance data. Although Eq. (1) does not actually appear in Ref. 9, it is consistent with the approach recommended by Odette.

[b]As mentioned later, a more accurate set of fluencies than these become availabel after these calculations were made; they are included elsewhere in this report.

[a]As mentioned later, a more accurate set of fluencies than these become availabel after these calculations were made; they are included elsewhere in this report.

an incentive for the utility to make copper-content measurements for the Yankee vessel welds.[a] No mention is made of the depth in the vessel wall at which the ΔRT_{NDT} values are being calculated, but presumably it is the inside surface.
Hiser's[6] and Odette's[9] ΔRT_{NDT} estimates were transmitted to NRC-NRR, which selected a combination of the two sets of estimates for transmittal to the utility as the staff estimates.[10] The original peak fluences and licensee estimates of RT_{NDT} as well as the NRC staff estimates of RT_{NDT} are shown in Table C.1. The unirradiated RT_{NDT} values are from Ref. 1 and Hiser.[b] The NRC ΔRT_{NDT} values for the plate are Odette's,[9] while those for the welds are Hiser's[6] "best estimate" values, with Odette's higher value for the circumferential weld included as a precaution. The large disparity between the NRC and YAEC estimates is evident.

Approximately a month after receiving the NRC staff estimate the utility transmitted back to NRC revised 1990 fluence values and RT_{NDT} estimates.[11] These revised estimates are shown in Table C.2, which also shows a comparison between ΔRT_{NDT} calculations performed at ORNL by the same methods chosen by the NRC staff for the preparation of Table C.1 and the revised YAEC submittal. Table C.2 demonstrates that the utility has accepted the NRC's basis and methods for calculating ΔRT_{NDT} values and, therefore, that there is no longer a controversy about surveillance specimen fluences, irradiation temperature effects, or nickel effects.

The RT_{NDT} values given in Tables C.1 and C.2 do not include the margin terms discussed in *Regulatory Guide 1.99* and 10CFR50, Para. 50.61. The utility applied a margin of 56°F to the RT_{NDT} estimate for weld metal (see Table 5.9, p. 5-28, of Ref. 1) but no margin was considered for plate. Hiser[6] used margins of 34°F for plate and 56°F for weld metal, apparently by doubling the values of σ_Δ in *Regulatory Guide 1.99*, but did not elaborate on the source of these numbers. The values transmitted by NRR[10] to the utility (see Table C.1) did not include margins.

C.3.2 Grain Size Effects

The YAEC report offered considerable discussion regarding the effects of microstructure on sensitivity to irradiation. Based on the relatively high austenitizing temperatures used for the Yankee plates (1750 to 1800°F), they assert that the plates have a relatively coarse austenite grain size and that their assertion is supported by BR3 microstructural analyses showing relatively coarse prior austenite grains. Their assertion of relatively coarse prior-austenite grains being present in the microstructure is likely correct. They further assert that a coarse-grain microstructure results in an increased sensitivity to neutron radiation.

One of the references they cite is that of Gordon and Klepfer,[12] which concluded that coarse ferrite grains in ferritic steels exhibit greater irradiation-induced shifts due to longer diffusion paths to defect sinks. Likewise, Nichols and Harries[13] showed a similar result. The Gordon and Klepfer work, however, was performed with almost pure ferrite grain steels and, as stated by Gordon and Klepfer, as substructure development occurs in the form of pearlite, bainite, martensite, etc., the assumptions used in their model become invalid because the damaging defects no longer have a relatively direct diffusion path to a ferrite-ferrite boundary. As shown in the Yankee report, the Yankee plate microstructure is largely bainitic; thus, the Gordon-Klepfer model, even if it is correct, may not be applicable to the Yankee case. On the other hand, Hawthorne[14] observed no effect of grain size on transition temperature shift for A 533 grade B class 1 steel. Likewise, Hosbons and Wotton[15] stated that there were no differences in quenched and tempered steels because of the finer carbide distribution inherent in the quenched structure. The Yankee plates are quenched and tempered. Recent work by Amayev[16] on chromium-molybdenum steels reported no differences between fine and coarse grains on the Charpy shift. Finally, Trudeau,[17] for a 3.25% Ni steel, showed less shift for the coarse grain than the fine grain steel.

There are other papers in the literature which attempt to examine the effects of grain size on embrittlement. The problem is that there are many confounding parameters involved other than the size of the prior austenite grains. The dislocation structure, precipitate structure, etc. all contribute to the mobility of defects in the microstructure, and these are affected by the fabrication process, heat treatment, and chemistry. The effects of grain size on embrittlement are, in other words, very uncertain and lacking consensus.

C.3.3 Temperature Effects

The effects of irradiation temperature on embrittlement have been extensively studied. In a general sense, it is agreed that for ferritic low-alloy steels hardening and embrittlement increase with decreasing irradiation temperature, at least within a certain temperature range. This effect has been shown for many steels including A 302 grade B.[18] In the range from about 400 to 600°F, there is considerable scatter even for a given material, indicating a high degree of sensitivity to irradiation temperature in that approximate temperature range. There are insufficient data for the Yankee plates, and none for the welds, with which to ascertain the effects of irradiation temperature on those specific materials.

There are many references which could be cited regarding irradiation temperature effects. Hiser discussed

[a]Hiser, Jr., A. L., NRC, personal communication to J. G. Merkle, ORNL, October 4, 1990.

[b]Hiser, Jr., A. L., draft of Ref. 6, undated.

some important ones in his memorandum: Stallman[19] on A 533 grade B class 1 (HSST Plate 02), Odette[20] on base and weld metals, Saulet (unreferenced), Fabry (unreferenced) on Linde 80 welds, and Lowe[21] on Linde 80 welds. Odette observed a range of irradiation temperature effects with different materials with 1° increase in transition temperature shift for each 1° decrease in irradiation temperature stated as a representative value. It should be noted, in fact, that observations were noted in which embrittlement increased with increasing temperature, and the authors emphasize the synergisms of other variables such as flux, fluence, and composition. Stallman also observed an average dependence of 1° shift increase per 1° decrease in irradiation temperature. Saulet's analysis expressed the effect as a ratio, such that a shift at 550°F would be multiplied by 1.45 to estimate the shift at 500°F. Using the Saulet method, a shift of 100°F at 550°F would be estimated as 145°F at 500°F. Using the representative value of 1° per degree of irradiation temperature simply adds 50°F to the shift at 550°F. For a fluence of 2.16×10^{19} neutrons/cm^2 (>1 MeV), the YAEC estimated shift of 180°F for the upper plate would become 260°F using the ratio method and 230°F using the additive method.

For the Linde 80 weld case, Fabry obtained a ratio of 1.40 for Linde 80 welds irradiated in BR3, while Lowe's analysis of the HSST Linde 80 welds determined an increase of about 0.7° in the shift for 1° decrease in irradiation temperature. Analyzing the same HSST data, Nanstad and Berggren[22] obtained an average value of about 0.5°F. For a fluence of 1.93×10^{19} neutrons/cm^2 (>1 MeV), the YAEC estimated shift of 203°F for the beltline welds would be increased by values ranging from 25 to 84°F using the various methods described above.

A couple of other pertinent studies are those of Williams et al.[23] and Ahlf et al.[24] For relatively high fluences, the Williams study showed temperature dependencies, in the manner discussed above, of 0.5 and 1.0°F/°F for two different materials. The Ahlf study reported dependencies of 0.5, 0.9, and 2.15°F/°F, for an average of about 1.2°F/°F, for three different materials.

In summary, the effects of irradiation temperature are dependent on many variables and, although there are specific instances of contradiction, the bulk of the studies reported in the literature indicate higher embrittlement with lower irradiation temperature in the temperature and fluence ranges applicable to the Yankee situation. All the above referenced studies involved radiation exposures in the range of 10^{19} n/cm^2 (>1 MeV). The use of an empirical correlation such as one degree increase in shift for one degree decrease in irradiation temperature is certainly not a scientifically satisfying approach, but it is a prudent approach which is substantiated with a body of research. Based on the information cited, use of that value to make a best estimate of the RT_{NDT} for the Yankee vessel seems reasonable and not overly conservative.

C.3.4 Nickel Effects

Nickel has long been identified as a potential "bad actor" in irradiation embrittlement of various steels. Based on the analyses of surveillance data from commercial light-water reactors, nickel plays a prominent role in the estimates of embrittlement in *Regulatory Guide 1.99* (Rev. 2). Odette and Lucas[20,25] observed that nickel can have a strong effect on the transition temperature shift in steels with copper, and that some data suggest an independent effect of nickel at high fluences. They also observed contradictory results, but the predominant observations led them to conclude that, for pressure vessel steels in general, nickel enhances embrittlement. As discussed in Hiser's memorandum, Hawthorne[26,27] reported significant effects of nickel on two pairs of plates (copper content was 0.16% in one pair and 0.28% in the other) from split melts where copper and all other elements were kept constant, while nickel was increased from 0.27 to 0.67% for each pair. At 2.5×10^{19} n/cm^2 (>1 MeV), the higher-nickel-content plates exhibited temperature shifts of 23% (0.16% Cu) and 44% (0.28% Cu) greater than those for the low nickel plates.

In other studies, Williams et al.[28] observed that nickel tended to mitigate the temperature dependence, but the studies were conducted with welds having nickel contents of about 0.3% or less and about 1.6%. Studies reported by Maricchiolo, Milella, and Pini[29] also indicate a mitigating effect of increased nickel, although the preponderance of their data were for nickel-to-copper ratios from about 5 to 25; while Fisher and Buswell[30] see enhanced sensitivity with increased nickel dependent on the copper and nickel contents.

Both Odette and Lucas, and Williams et al. emphasize that the effects of nickel are not very well understood. The often-mentioned synergism of copper and nickel is confounded by effects of other elements and heat treatments which may affect the precipitation kinetics of the copper as well as the matrix-damage component of embrittlement. Although there are observations to the contrary, the evidence to support the YAEC claim of no nickel effect for the lower plate is minimal. Furthermore, observations of significant enhancement of embrittlement from increased nickel make consideration of a nickel adjustment the prudent choice. Using different methods, Hiser and Odette recommended the addition of 70 and 80°F, respectively, to the upper plate shift to account for the higher nickel in the lower plate.

C.3.5 Summary of Metallurgical and Temperature Effects on RT_{NDT}

The YAEC report on the Yankee reactor vessel embrittlement presents extensive discussions regarding the effects of irradiation temperature, nickel content, and grain size on neutron embrittlement of the vessel plates. Their claim that the probable coarse grain size of the plates mitigates the effects of lower irradiation temperature and higher nickel content is not substantiated with sufficient evidence. The confounding effects of so many variables demands prudent choices in cases like this where information is so sparse. The YAEC claims may turn out to be correct, but the information available at this time is inadequate to allow their use. The bases used by the NRC staff for shift estimates are reasonable under the circumstances and not overly conservative.

C.4 Charpy Upper-Shelf Energy Considerations

C.4.1 Summary of Upper-Shelf Energy Estimates

The utility's estimates of Charpy V-notch upper-shelf impact energy at the end of plant life are given on pp. 5–26 of Ref. 1 and then repeated in less detail on pp. 3–4, 3–5, and 3–7 of the same reference. These estimates are stated as follows: (pp. 5–26) "The predictions for plate longitudinal Charpy V-notch upper shelf energy are based on data from the current BR3/ YAEC test program on surveillance capsule specimens at BR3. These data are shown in Fig. 5–10." (pp. 3–5) "The measured upper shelf energy of the Yankee plate material (L–T) at a fluence associated with the year 2020 is 5' ft–lb. Therefore, using SRP 5.3.2 to obtain the transverse (T-L) direction, results in an upper shelf energy of 35 ft–lb." [MTEB 5-2, attached to SRP 5.3.2, prescribes a multiplying factor of 0.65 for estimating transverse direction upper shelf values from longitudinal direction upper shelf values.] (pp. 5–26) "The predicted upper shelf energy for weld metal is 40 ft–lb in the year 2020. It is based on an initial upper shelf energy of 70 ft–lb and use of *Reg. Guide 1.99*, Rev. 2, and BR3 chemistry to predict the drop in upper shelf energy. The validity of 40 ft–lb is also corroborated by data from the B&W Owners' Group presented at the May 24, 1990, ACRS meeting in West Palm Beach, Florida, which showed that upper shelf energy for their Linde-80 welds were above 40 ft-lb for fluences out to and beyond 2×10^{19} n/cm^2."

The NRC[6] made calculations for the individual reactor vessel near-beltline materials using Fig. 2 of *Regulatory Guide 1.99* and the same fluence values used to estimate ΔRT_{NDT}. The NRC results are summarized in Table C.3. It can be seen that the NRC 1990 estimates for plate are less than the utility's EOL estimate. The NRC 1990 estimates for weld metal, assuming 0.35% copper, are close to the utility's EOL estimate. None of the foregoing upper shelf CVN estimates considered through-wall fluence attenuation, although it is permitted by 10CFR50, Appendix G, Sect. V, to do so.

Since both the utility and the NRC utilized Fig. 2 of *Regulatory Guide 1.99* for estimating upper shelf drops, and the irradiation temperature for the data base of that figure is 550°F, it is advisable to consider the effect of irradiation temperature on this estimate. Hiser[6] noted that, "Lower irradiation temperature tends to result in greater radiation sensitivity (i.e., greater shifts and shelf drops)" but also that, "the Regulatory Guide is thought to be conservative for irradiation at 550°F; the degree of conservatism is probably sufficient to account for the Yankee Rowe operating temperature of 500°F." Information regarding the effect of irradiation temperature on the Charpy upper-shelf energy is sparse. Nanstad and Berggren[22] analyzed the HSST low upper-shelf welds and determined an effect of about –0.022 ft–lb/°F, meaning that the upper-shelf energy decreases 0.022 ft–lb for each one degree Fahrenheit decrease in irradiation temperature at a fluence of about 8×10^{18} n/cm^2 (>1 MeV). For a 50°F decrease in temperature, the decrease in upper-shelf energy is about 1.1 ft–lb. For an upper-shelf energy of about 40 ft–lb, that amount of change is certainly not substantial.

The Yankee Rowe surveillance program produced upper-shelf-drop data as well as transition temperature shift data.[5] It should be noted[a] that of the five Yankee steel upper shelf values listed in Table 2 of Ref. 4, only two are measurements. The others, denoted by the approximation symbol (~), are estimates. (These data were listed in NUREG-0569 without distinguishing between experimental data and estimates.)[31] Data for two of these specimens were used by Steele and Serpan[32,33] to develop a graphical correlation between percent upper-shelf drop and increase in Charpy V-notch 30-ft-lb temperature. This plot, with the remaining data and estimates from Table 2 of Ref. 5 added, is shown in Fig. C.4. Also shown in Fig. C.4 is Hiser's[6] estimate of percent shelf drop and ΔRT_{NDT} for the upper plate. The upper-shelf drop, from Table C.3, is 32.8%, and the ΔRT_{NDT} value, from Eq. (4), for f = 2.3, is 224°F. Hiser's estimates are consistent with the two Yankee surveillance data points and the two additional estimates for Yankee material. The data for the ASTM correlation monitor material all plot above the Yankee surveillance data. Odette's estimate of ΔRT_{NDT} for the upper plate was 245°F, which would shift the estimating point in Fig. C.4 21°F to the right, still preserving a consistent trend with the other Yankee data.

Additional upper shelf drop data for ASTM correlation monitor material specimens were compiled by NRL.[34,35] Unirradiated upper shelf values ranged from 71 to 86 ft–lb in the longitudinal direction, and 45 to 46 ft–lb in the transverse direction. Irradiated upper shelf values seemed to approach lower limits depending on irradiation temperature and specimen

[a]Hiser, Jr., A. L., NRC, personal communication to J. G. Merkle, ORNL, October 11, 1990.

orientation. For irradiation at 550°F, the lower limits appeared to be 63 ft–lb for the longitudinal direction and about 41 ft–lb for the transverse direction. For irradiation at temperatures less than 300°F, the corresponding lower limits were 44 ft–lb and 18 ft–lb. Clearly, irradiation temperature and orientation are important variables. The estimated 1990 values for Yankee plate in Table C.3 are all between the lower limits for the corresponding orientations given in Ref. 35. Thus the estimating procedures in *Regulatory Guide 1.99* apparently do contain enough conservatism to justify application to a vessel operating at temperatures between 500 and 550°F.

The revision of the fluences for the Yankee vessel given in Ref. 11 required a recalculation of the upper shelf drops. The procedure for estimating upper shelf drops requires reading and interpolating values from Fig. 2 of *Regulatory Guide 1.99*, which is a log log plot of percent shelf drop vs fluence, with copper content as a parameter. This procedure is somewhat tedious, but since all the curves in the figure are straight lines, the procedure is easily reduced to the application of simple equations. The equation of the upper-bound curve is

$$\Delta CVN(\%) = 42.39f^{0.1502}, \quad (4)$$

The equation of the lower curves for base metal is

$$\Delta CVN(\%) = (100\ Cu + 9)f^{0.2368}, \quad (5)$$

and the equation of the lower curves for weld metal is

$$\Delta CVN(\%) = (100\ Cu + 14)f^{0.2368}, \quad (6)$$

The intersection of the lower curves with the upper curve occurs at

$$f = [42.39/A]^{11.55}, \quad (7)$$

where A is the multiplying factor in Eqs. (5) and (6). The recalculated 1990 Charpy V-notch upper shelf impact energies are shown in Table C.4. All changes are reductions, but the only significant change from Table C.3 is for the upper axial weld, because of the large change in fluence.

C.4.2 Low Upper-Shelf Energy Effects on Fracture Toughness

Low upper-shelf impact energy in reactor pressure vessel steels and welds has the effect of lowering the margin between strength in the presence of flaws and applied loads. In Chap. 3 of Ref. 1, the utility performed low upper-shelf analyses for Levels A, B, and C loading conditions according to procedures proposed by the ASME Section XI Working Group on Flaw Evaluation. Because the ratio of inside radius to wall thickness (R/w) for the Yankee vessel is 6.83, including the thickness of the cladding, the stresses due to pressure are roughly seventy percent of what they would be for a vessel with a R/w ratio of 10. Thus, the utility calculated adequate margins on the upper shelf even though upper-shelf energies were estimated to be as low as 40 ft–lb. This result was anticipated in a previous NRC analysis.[31] The NRC did not review the utility's upper-shelf analysis in detail. In this evaluation, the utility's calculations of applied K_I due to pressure and thermal loading have not been checked, but the choices of representative J–R curves for base metal and weld metal have been reviewed. Additionally, the choices of upper-shelf toughness values appropriate for use in PTS analyses have been examined. This subject was not discussed by the utility in Ref. 1. Apparently, YAEC used the ASME maximum value of $K_C = 200\ \text{ksi}\sqrt{\text{in.}}$ as an upper-shelf toughness, without questioning whether or not this value actually corresponds to the Charpy upper-shelf energies estimated.

In Ref. 1 it was noted that size effects have been observed in J-R curves measured by Hiser and Terrell[36] for transversely oriented (T-L) specimens of unirradiated A 302 grade B steel. Additionally, as shown in Fig. C.5, such J-R curves can lose all slope, approaching constant values of J. Consequently, a procedure was developed in Ref. 1 for estimating the J-R curves for irradiated low upper-shelf A 302 grade B plate. The procedure consists of developing mean and mean −2σ correlations between Charpy upper-shelf impact energy and J_{Ic}, as shown in Fig. C.6, and then, based on Fig. C.5, assuming that the upper-bound constant level of J for any base-metal J-R curve is 1.3 times J_{Ic} (see pp. 3-6 and 3-7 of Ref.1). In Ref. 6, Hiser developed mean and mean −2σ correlations between $J_{0.1}$, corresponding to $\Delta a = 0.10$ in., and CVN, and these correlations are shown in Fig. C.6. The convergence of correlation curves for $J_{0.1}$ and J_{Ic} for CVN approaching 15 ft–lb in Fig. C.6 is further indication of the flattening out of low-upper-shelf J-R curves for A 302 grade B base metal.

The correlations in Fig. C.6 have the following equations:

$$J_{Ic}\ (\text{mean}) = 160 + 4.20\ CVN, \quad (8)$$

$$J_{Ic}\ (-2\sigma) = 4.20\ CVN, \quad (9)$$

$$J_{0.1}\ (\text{mean}) = 108 + 11.75\ CVN, \quad (10)$$

and

$$J_{0.1}\ (-2\sigma) = -162 + 11.75\ CVN, \quad (11)$$

where J is in in.–lb/in.^{2} and CVN is in ft–lb.

For estimating the J-R curves for Linde 80 weld metal, the utility used a correlation, developed by Hiser,[37] between the parameters of a power law representation of a J-R curve,

$$J = C[\Delta a/k]^n, \quad (12)$$

and the Charpy upper-shelf impact energy. The coefficients in the correlation used in Ref. 1 are given in Table C.2 of Ref. 37. Because Eq. (12) is a power law, the estimated J-R curve will not level off as did the base metal J-R curve shown in Fig. C.5. Nevertheless, there are J-R curves for Linde 80 weld metal that display the tendency to flatten out. Such an example, corresponding to CVN = 39 ft-lb, is shown in Fig. C.7, which is from Fig. C-50 in Ref. 37. The asymptotic upper level of J_{max} for specimen W8A-121, from Fig. C.7, is about 600 in.-lb/in.^{2}

The Yankee Rowe estimate of J_{max} for A 302-B plate is

$$J_{max} = 1.3\, J_{Ic}\,. \quad (13)$$

For purposes of estimating the upper-shelf toughness appropriate for a PTS analysis, values of J_{max} (mean) can be converted to K_c by the equation

$$K_c = [EJ_{max}(mean)/(1-\nu^2)]^{1/2}. \quad (14)$$

Applying Eqs. (8) through (14) to the upper-shelf Charpy impact energies estimated by the utility[1] and by NRC[6] (prior to the fluence revision) gives the values of J and K shown in Table C.5.

Figure C.8 shows the J-R curve for irradiated Linde 80 weld specimen W8A-121 from Fig. C.7 compared to the J-R curve for the unirradiated 6T A 302 grade B specimen from Fig. C.5, plus the J_{max} values from Table C.5 for A 302 grade B plate, based on the NRC 1990 estimates of CVN. From Eq. (15), the value of J_{max} corresponding to $K_C = 200$ ksi$\sqrt{in.}$ is 1213 in.-lb/in.^{2} Clearly, $K_C = 200$ ksi$\sqrt{in.}$ is not an appropriate upper-shelf toughness value for PTS analysis for the near beltline materials in the Yankee Rowe vessel. As indicated in Table C.5, values of 141, 126, and 113 ksi$\sqrt{in.}$ are more appropriate for the welds, upper plate, and lower plate, respectively. The sensitivity of P(FIE) to inclusion of lower values is discussed in Sect. D.4.2.

C.5 Summary of Radiation Effects

There are many factors contributing to the uncertainties regarding the fracture toughness of the Yankee reactor vessel. Among these are the relatively low operating temperature (~500°F), only a small amount of surveillance data, effects of grain size and nickel content, and lack of chemical composition data.

The copper content of welds fabricated with copper-coated wire can be quite variable, as shown by B&W and HSSI Program studies. *Regulatory Guide 1.99*, Rev. 2 allows the use of conservative estimates based on generic data (mean + standard deviation). A copper content of 0.35 wt% (mean of 0.29% plus standard deviation) was determined for the Yankee welds, based on the B&W generic data.

The YAEC report asserted that the plates have a relatively coarse austenite grain size, which is likely, with a resultant increased sensitivity to neutron radiation and which mitigates the effects of the lower irradiation temperature and nickel content. In summary, references were cited which showed there are many confounding parameters involved other than the size of the prior austenite grains. The dislocation structure, precipitate structure, etc., all contribute to the mobility of defects in the microstructure and these are affected by the fabrication process, heat treatment, and chemistry. The effects of grain size on embrittlement are, in other words, very uncertain and lacking consensus.

The effects of irradiation temperature are dependent on many variables and, although there are specific instances of contradiction, the bulk of the studies reported in the literature indicate higher embrittlement with lower irradiation temperature in the temperature and fluence ranges applicable to the Yankee situation. This effect has been shown for many steels including A 302 grade B and for Linde 80 welds. The use of an empirical correlation such as one degree increase in shift for one degree decrease in irradiation temperature is certainly not a scientifically satisfying approach, but it is a prudent approach which is substantiated with a body of research. Based on the information cited, use of that value seems reasonable and not overly conservative for the exposure conditions of the Yankee vessel.

Although there are observations to the contrary, the evidence to support the YAEC claim of no nickel effect for the lower plate is minimal. Based on the analyses of surveillance data from commercial light-water reactors, nickel plays a prominent role in the estimates of embrittlement in *Regulatory Guide 1.99* (Rev. 2). Further-more, the cited observations of significant enhancement of embrittlement from increased nickel make consideration of a nickel adjustment the prudent choice. Using different methods, Hiser and Odette recommended the addition of 70 and 80°F, respectively, to the upper plate shift to account for the higher nickel in the lower plate.

The YAEC claim that the probable coarse grain size of the plates mitigates the effects of lower irradiation temperature and higher nickel content is not substantiated with sufficient evidence. The confounding effects of so many variables demands prudent choices in cases like this where information is so sparse. The YAEC claims may turn out to be correct, but the information available at this time is inadequate to allow their use. The bases used by the NRC staff for shift estimates are reasonable under the circumstances and not overly conservative.

Using available drop-weight and Charpy impact data on Yankee surveillance material and with the application of NRC Branch Technical Position MTEB 5-2, the initial RT_{NDT} values for the Yankee plates were estimated by the NRC and accepted by YAEC. The NRC and YAEC estimates for the welds were identical. Although vast differences initially existed between the YAEC and NRC staff estimates of the RT_{NDT} shifts for all the vessel materials, discussions between YAEC and NRC have led to convergence of the two organizations' estimates, and indicate that the PTS screening criteria have been exceeded.

The NRC estimates for upper-shelf energies were somewhat lower than those of YAEC and are based on those in *Regulatory Guide 1.99*, Rev. 2, with no consideration for the lower irradiation temperature of the Yankee vessel because it was concluded by NRC that the Guide contains sufficient conservatisms with respect to the specific conditions of Yankee. For reasons cited in this report, however, the utility calculated adequate margins of stress on the upper shelf to compensate for those differences. The analyses at ORNL, however, regarding fracture toughness and J-R curves, indicates the utility's use of the ASME maximum value of $K_c = 200\ \mathrm{ksi}\sqrt{\mathrm{in.}}$ as an upper-shelf fracture toughness is too high for the low upper-shelf materials in the Yankee vessel.

C.6 References

1. *Reactor Pressure Vessel Evaluation Report*, YAEC No. 1735, Yankee Atomic Electric Company, Bolton, Massachusetts, July 1990.[a]

2. K. E. Moore and A. S. Heller, *B&W 177-FA Reactor Vessel Beltline Weld Chemistry Study*, BAW-1799, Babcock & Wilcox, Lynchburg, Virginia, July 1983.[a]

3. C. Z. Serpan, Jr., H. E. Watson, J. R. Hawthorne, and L. E. Steele, *Yankee Reactor Pressure Vessel Surveillance: Evaluation of Specimens Exposed During the Second Core*, NRL Report 6179, U.S. Naval Research Laboratory, Washington, D.C., November 24, 1964.[b]

4. L. E. Steele et al., *Irradiation Effects on Reactor Structural Materials, Quarterly Progress Report — 1 May–31 July 1966*, NRL Memorandum Report 1719, U.S. Naval Research Laboratory, Washington, D.C., August 15, 1966.[b]

5. C. Z. Serpan, Jr., and J. R. Hawthorne, *Yankee Reactor Pressure Vessel Surveillance: Notch Ductility Performance of Vessel Steel and Maximum Service Fluence Determined from Exposure During Cores II, III, and IV*, NRL Report 6616, U.S. Naval Research Laboratory, Washington, D.C., September 29, 1967.[b]

6. A. L. Hiser, Jr., NRC, "Summary of Fracture Toughness Estimates for Irradiated Yankee Rowe Vessel Materials," letter to C. Y. Cheng, NRC, with attachment, August 30, 1990.[a]

7. Branch Technical Position — MTEB 5-2, Fracture Toughness Requirements, pp. 5.3.2-13 to 5.3.2-18 in U.S. Nuclear Regulatory Commission Standard Review Plan, NUREG-0800, Rev. 1, U.S. Nuclear Regulatory Commission, Washington, D. C., July 1981.[b]

8. G. Papanic, Yankee Atomic Electric Company, letter to E. McKenna, U.S. Nuclear Regulatory Commission, February 4, 1987.[a]

9. G. R. Odette, University of California at Santa Barbara, letter to A. Taboada, NRC, July 30, 1990, with attachment entitled, "1990 Shift Estimates for the Yankee Rowe Vessel."[a]

10. T. E. Murley, NRC-NRR, letter to A. C. Kadak, Yankee Atomic Electric Company, Docket No. 50-029, August 31, 1990, with attachment entitled, "Safety Assessment of Yankee Rowe Vessel."[a]

11. T. E. Murley, NRC-NRR, letter to R. F. Fraley, ACRS, October 9, 1990, with attachment: J. D. Haseltine, Yankee Atomic Electric Company, letter to W. Russell, NRC-NRR, Docket No. 59-29, September 28, 1990.[a]

12. G. M. Gordon and H. H. Klepfer, "The Engineering Significance of Ferrite Grain Size on the Radiation Sensitivity of Pressure Vessel Steels," pp. 48–66 in *Effects of Radiation on Structural Metals*, ASTM STP 426, American Society for Testing and Materials, 1967.[c]

13. R. W. Nichols and D. R. Harries, "Brittle Fracture and Irradiation Effects in Ferritic Pressure Vessel Steels," p. 162 in *Radiation Effects on Metals and Neutron Dosimetry*, ASTM STP 341, American Society for Testing and Materials, January 1963.[c]

14. J. R. Hawthorne, "Demonstration of Improved Radiation Embrittlement Resistance of A533-B Steel Through Control of Selected Residual Elements," p. 96 in *ASTM-STP 484*, American Society for Testing and Materials, 1971.[c]

[a]Available from NRC Public Document Room for a fee.
[b]Available from National Technical Information Service.

[a]Available from NRC Public Document Room for a fee.
[b]Available from National Technical Information Service.
[c]Available from public and special technical libraries.

15. R. R. Hosbons and B. L. Wotton, "The Effect of Fast Neutron Irradiation on the Mechanical Properties of Some Quenched and Tempered Steels, p. 142 in *ASTM-STP 484*, American Society for Testing and Materials, 1971.[a]

16. A. D. Amayev et al., "Radiation Stability of VVER-1000 Reactor Vessel Materials," Report of Working Group 3 of the U.S.S.R.-U.S. Joint Coordinating Committee on Civilian Nuclear Reactor Safety (JCCCNRS), Moscow, U.S.S.R., pp. 25-29 June 1990.[b]

17. L. P. Trudeau, *Radiation Effects on Reactor Structural Materials*, AEC Monograph Series, American Society for Metals, Rowman and Littlefield, New York, 1964.[a]

18. L. E. Steele, *Neutron Irradiation Embrittlement of Reactor Pressure Vessel Steels*, p. 123, Technical Reports Series No. 163, International Atomic Energy Agency, Vienna, 1975.[a]

19. F. W. Stallman, *Curve Fitting and Uncertainty Analysis of Charpy Impact Data*, NUREG/CR-2408, Oak Ridge National Laboratory, January 1982.[c]

20. G. R. Odette and G. E. Lucas, *Irradiation Embrittlement of LWR Pressure Vessel Steels*, NP-6114, Electric Power Research Institute, January 1989.[d]

21. A. L. Lowe, "An Evaluation of Linde 80 Submerged-Arc Weld Metal Charpy Data Irradiated in the HSST Program," *ASTM STP 1046*, Vol. 2, American Society for Testing and Materials, 1990.[a]

22. R. K. Nanstad and R. G. Berggren, *Effects of Irradiation on Low Upper-Shelf Welds, Heavy-Section Steel Irradiation Program, Irradiation Series-2 and -3*, NUREG/CR-5696 (ORNL/TM-11804), July 1991.[c]

23. T. J. Williams et al., "The Influence of Copper, Nickel and Irradiation Temperature on the Irradiation Shift of Low Alloy Steels," pp. 393–99 in *Proceedings of the Second International Symposium on Environmental Degradation of Materials in Nuclear Power Systems — Water Reactors*, TMS AIME/ANS/NACE, Monterey, Calif., September 1985.[a]

24. J. Ahlf, D. Bellmann, F. J. Schmitt, and W. Spalthoff, "Investigation on the Dependence of RPV Steel Embrittlement on Irradiation Temperature and Neutron Exposure," in *Proceedings of the 4th ASTM-EURATOM Symposium on Reactor Dosimetry*, Gaithersburg, Md., 1982.[a]

25. G. R. Odette and G. E. Lucas, "Irradiation Embrittlement of Reactor Pressure Vessel Steels: Mechanisms, Models, and Data Correlations," pp. 206–41 in *Radiation Embrittlement of Nuclear Reactor Pressure Vessel Steels: An International Review (Second Volume)*, ASTM-STP 909, L. E. Steele, Ed., American Society for Testing and Materials, Philadelphia, 1986.[a]

26. J. R. Hawthorne, *Steel Impurity Element Effects on Postirradiation Properties Recovery by Annealing*, NUREG/CR-5388, August 1989.[a]

27. J. R. Hawthorne, "Status of Knowledge of Radiation Embrittlement in U.S. Reactor Pressure Vessel Steels," in pp. 100–15 in *Radiation Embrittlement and Surveillance of Nuclear Reactor Pressure Vessels: An International Study*, ASTM-STP 819, L. E. Steele, Ed., American Society for Testing and Materials, 1983.[a]

28. T. J. Williams, P. R. Burch, C. A. English, and P. H. N. de la cour Ray, "The Effect of Irradiation Dose Rate and Temperature, and Copper and Nickel Content, on the Irradiation Shift of Low Alloy Steel Submerged Arc Welds," in *Proceedings of the Third International Symposium on the Environmental Degradation of Materials in Nuclear Power Systems — Water Reactors*, G. J. Theus and J. R. Weeks, Eds., TMS AIME/ANS/NACE, 1988.[a]

29. C. Maricchiolo, P. P. Milella, and A. Pini, "Prediction of Reference Transition Temperature Increase Due to Neutron Irradiation Exposure," pp. 96–105 in *Radiation Embrittlement of Nuclear Reactor Pressure Vessel Steels: An International Review (Second Volume)*, ASTM-STP909, L. E. Steele, Ed., American Society for Testing and Materials, Philadelphia, 1986.[a]

30. S. B. Fisher and J. T. Buswell, "A Model for PWR Pressure Vessel Embrittlement," *Int. J. Pressure Vessels and Piping*, 27, pp. 91–135 (1987).[a]

31. K. G. Hoge, *Evaluation of the Integrity of SEP Reactor Vessels*, NUREG-0569, U.S. Nuclear Regulatory Commission, Washington, D.C., December 1979.[b]

[a] Available from NRC Public Document Room for a fee.
[b] Available from National Technical Information Service.
[c] Available from public and special technical libraries.
[d] Available for purchace from organization sponsoring publication cited, and/or from authors and/or recipients (documented letters).

[a] Available from NRC Public Document Room for a fee.
[b] Available from National Technical Information Service.

32. L. E. Steele and C. Z. Serpan, *Procedures for Interpreting the Structural Implications of Radiation-Damage Surveillance Results on Nuclear Pressure Vessels*, NRL Report 7358, U.S. Naval Research Laboratory, Washington, D.C., December 30, 1971.[a]

33. L. E. Steele, *Neutron Irradiation Embrittlement of Reactor Pressure Vessel Steels*, International Atomic Energy Agency, Technical Report Series No. 163, Vienna, 1975.[b]

34. J. R. Hawthorne, *Trends in Charpy-V Shelf Energy Degradation and Yield Strength Increase of Neutron-Embrittled Pressure Vessel Steels*, NRL Report 7011, U.S. Naval Research Laboratory, Washington, D.C., December 22, 1969.[a]

35. J. R. Hawthorne, "Trends in Charpy-V Shelf Energy Degradation and Yield Strength Increase of Neutron-Embrittled Pressure Vessel Steels," *Nuclear Engineering and Design, 11*, pp. 427-446 (1970).[b]

36. A. L. Hiser and J. B. Terrell, *Size Effects on J-R Curves for A 302-B Plate*, NUREG/CR-5265 (MEA-2320), Materials Engineering Associates, Lanham, Md., January 1989.[c]

37. R. M. Gamble, A. Zahoor, A. Hiser, H. A. Ernst, and E. T. Pollitz, *Evaluation of Upper-Shelf Toughness Requirements for Reactor Pressure Vessels*, EPRI NP-6790-SL, Vol. 2, Draft, Electric Power Research Institute, Palo Alto, Calif., October 1989.[d]

[a]Available from NRC Public Document Room for a fee.
[b]Available from National Technical Information Service.

[b]Available from National Technical Information Service.
[c]Available from public and special technical libraries.
[d]Available for purchase from organization sponsoring publication cited, and/or from authors and/or recipients (documented letters).

Table C.1 Licensee and staff estimates of RT_{NDT} for the YNPS beltline materials in 1990, prior to September 1990

YNPS beltline material	1990 original peak fluences ($\times 10^{19}$ n/cm^2)	Unirradiated reference temperature (°F)		Increase in reference temperature resulting from irradiation* (°F)		Reference temperature RT_{NDT} in 1990* (°F)	
		Staff estimate	Licensee estimate	Staff estimate	Licensee estimate	Staff estimate	Licensee estimate
Upper plate	2.3	30	10	245	180	275	190
Lower plate	2.05	30	10	325†	173	355†	183
Axial welds**	0.38	10	10	216	131	226	141
Circumferential weld**	20.5	10	10	320-360	219	330-370	229

*Does not include "margin" term.

**NRC used Cu=35%, Ni=0.7%. YAEC used Cu=0.18%, Ni=0.7%.

†Based on a fluence of 2.3×10^{19} n/cm^2 rather than the correct value of 2.05×10^{19}.

Table C.2 Licensee and ORNL estimates of RT_{NDT} for the YNPS beltline materials in 1990, based on September 1990 revisions from Licensee

YNPS beltline material	1990 revised peak fluences ($\times 10^{19}$ n/cm^2)	Unirradiated reference temperature (°F)		Increase in reference temperature resulting from irradiation* (°F)		Reference temperature RT_{NDT} in 1990* (°F)	
		ORNL estimate**	Licensee estimate	ORNL estimate**	Licensee estimate	ORNL estimate**	Licensee estimate
Upper plate	2.6	10	30	256	255	266	285
Lower plate	2.31	30	30	326	325	356	355
Upper axial welds†	1.24	10	10	290	290	300	300
Lower axial weld†	1.2	10	10	288	288	298	298
Circumferential weld†	2.31	10	10	328	328	338-378	338

*Does not include "margin" term.

**ORNL estimates based on concurrence with estimates by NRC staff and consultant.

†Based on Cu = 0.35%, Ni = 0.7%.

Table C.3 NRC estimates of Charpy upper-shelf energies for the YNPS beltline materials in 1990, prior to September 1990

Material	Original fluence ($\times 10^{19}$ n/cm^2)	Initial energy (ft-lb)	Drop (%)	Original 1990 energy (ft-lb)
Upper plate				
L	2.3	76	32.8	51.1
T	2.3	49.4	32.8	33.2
Lower plate				
L	2.05	76	34.0	50.2
T	2.05	49.4	34.0	32.6
Upper axial weld				
0.35 Cu	0.38	70.2	37.0	44.2
0.18 Cu	0.38	70.2	25.5	52.3
Circumferential weld				
0.35 Cu	2.05	70.2	47.0	37.2
0.18 Cu	2.05	70.2	37.6	43.8

Table C.4 ORNL estimates of Charpy upper-shelf energies for the YNPS beltline materials in 1990, based on September 1990 revisions from Licensee

Material	Revised fluence ($\times 10^{19}$ n/cm^2)	Initial energy (ft-lb)	Drop (%)	Revised 1990 energy (ft-lb)
Upper plate				
L	2.6	76	33.9	50.2
T	2.6	49.4	33.9	32.7
Lower plate				
L	2.31	76	35.3	49.2
T	2.31	49.4	35.3	32.0
Upper axial weld				
0.35 Cu	1.24	70.2	43.8	39.5
0.18 Cu	1.24	70.2	33.7	46.3
Lower axial weld				
0.35 Cu	1.20	70.2	43.6	39.6
0.18 Cu	1.20	70.2	33.4	46.8
Circumferential weld				
0.35 Cu	2.31	70.2	48.1	36.5
0.18 Cu	2.31	70.2	39.1	42.8

Table C.5 Summary of Charpy upper-shelf energies and fracture toughnesses for the YNPS beltline materials in 1990, prior to September 1990.

Charpy V-notch (ft-lb)		-2σ R curve			Mean upper-shelf K_{Ic} for PTS analysis		
YR (2020)	NRC (1990)	YR (2020)	NRC (1990)	ORNL	YR (2020)	NRC	ORNL (1990)
			Linde 80 weld				
40	44-52 (Axial)	MEA correlation for CVN = 40 ft-lb		Specimen W9A-121, CVN = 39			J_{max} = 600 in.-lb/in.^{2}
	37-44 (Circumferential)				K_{max} = 200 ksi $\sqrt{in.}$		K_{max} = 141 ksi $\sqrt{in.}$
			A 302 plate (longitudinal)				
57	51 (Upper)	J_{Ic} = 245 in.-lb/in.^{2}	$J_{0.1}$ = 289 in.-lb/in.^{2}				J_{max} = 481 in.-lb/in.^{2}
	50 (Lower)	J_{mad} = 320 in.-lb/in.^{2}			K_{max} = 200 ksi $\sqrt{in.}$		K_{max} = 126 ksi $\sqrt{in.}$
			A 302 plate (transverse)				
35	33 (Upper)	J_{Ic} = 150 in.-lb/in.^{2}	$J_{0.1}$ = 149 in.-lb/in.^{2}				J_{max} = 388 in.-lb/in.^{2}
	33 (Lower)	J_{mad} = 195 in.-lb/in.^{2}			K_{max}= 200 ksi $\sqrt{in.}$		K_{max} = 113 ksi $\sqrt{in.}$

RPV WELD & PLATE LOCATIONS

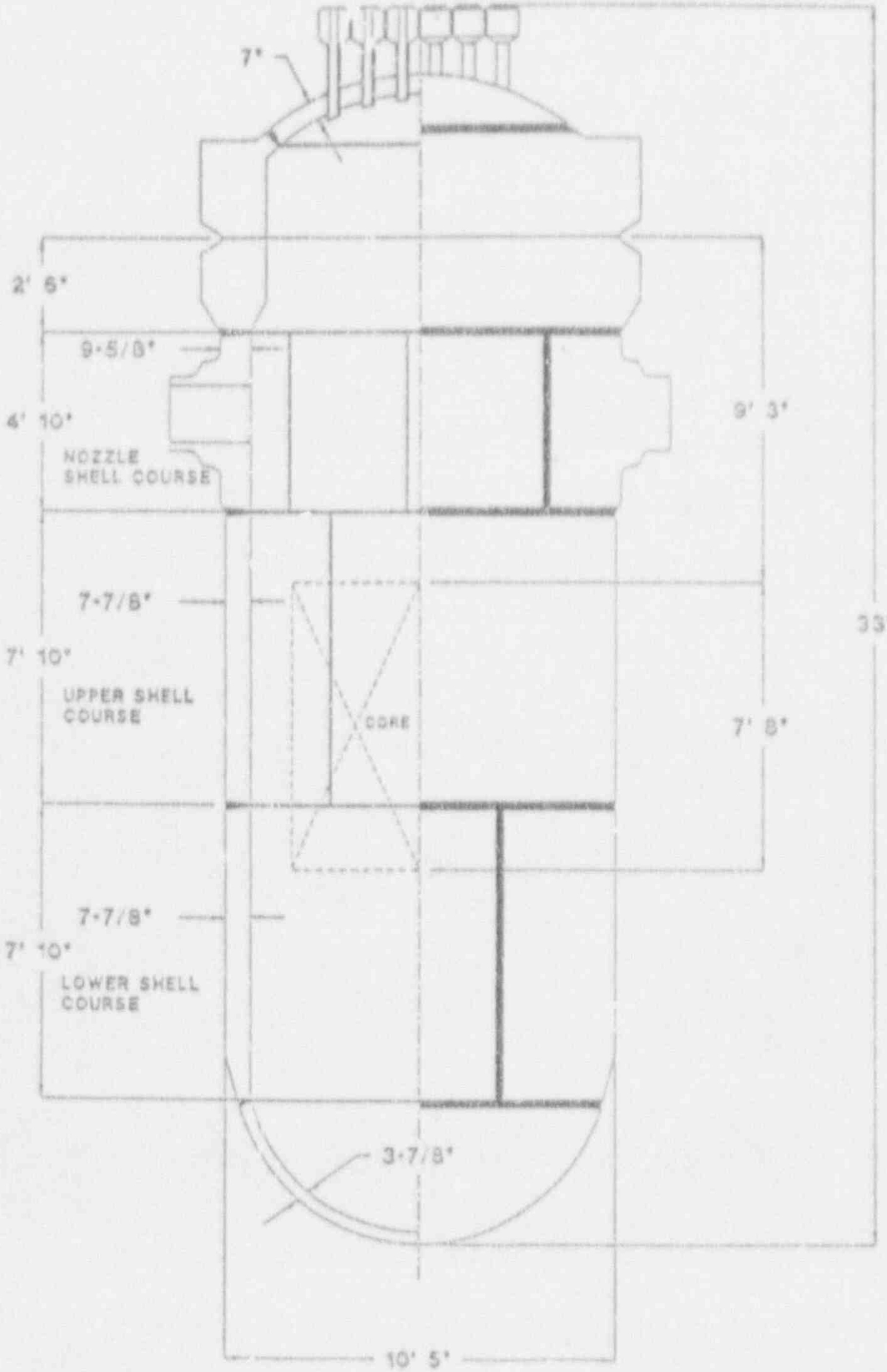

Fig. C.1. Schematic drawing showing locations of plates and welds in the Yankee reactor vessel. *Source:* *Reactor Pressure Vessel Evaluation Report*, YAEC No. 1735, Yankee Atomic Electric Company, Bolton, Massachusetts, July 1990.

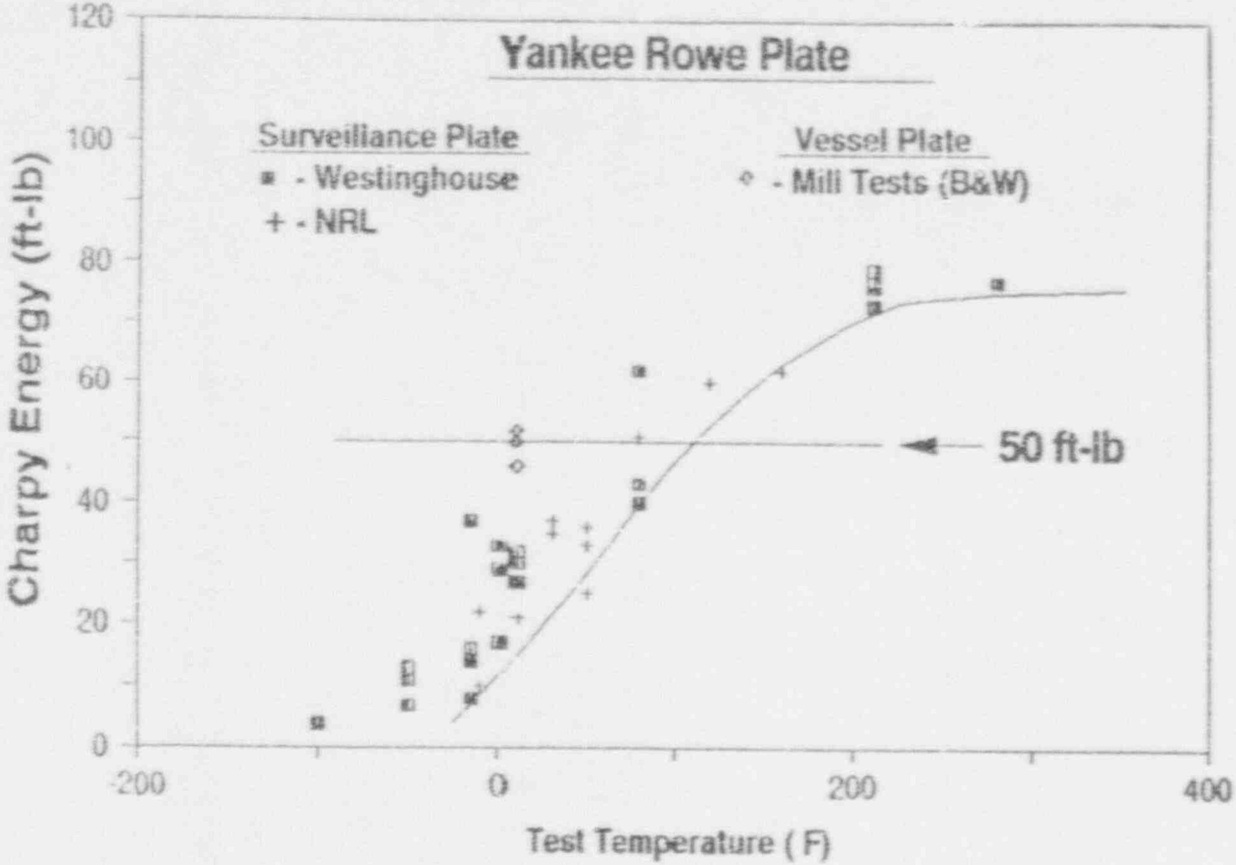

Fig. C.2. Charpy-V impact data for the Yankee Rowe upper plate, from the actual vessel plate and from the surveillance plate. The illustrated curve is an approximate lower bound to the surveillance plate data. *Source:* Hiser, Jr., A. L., NRC, "Summary of Fracture Toughness Estimates for Irradiated Yankee Rowe Vessel Materials," letter to C. Y. Cheng, NRC, with attachment, August 30, 1990.

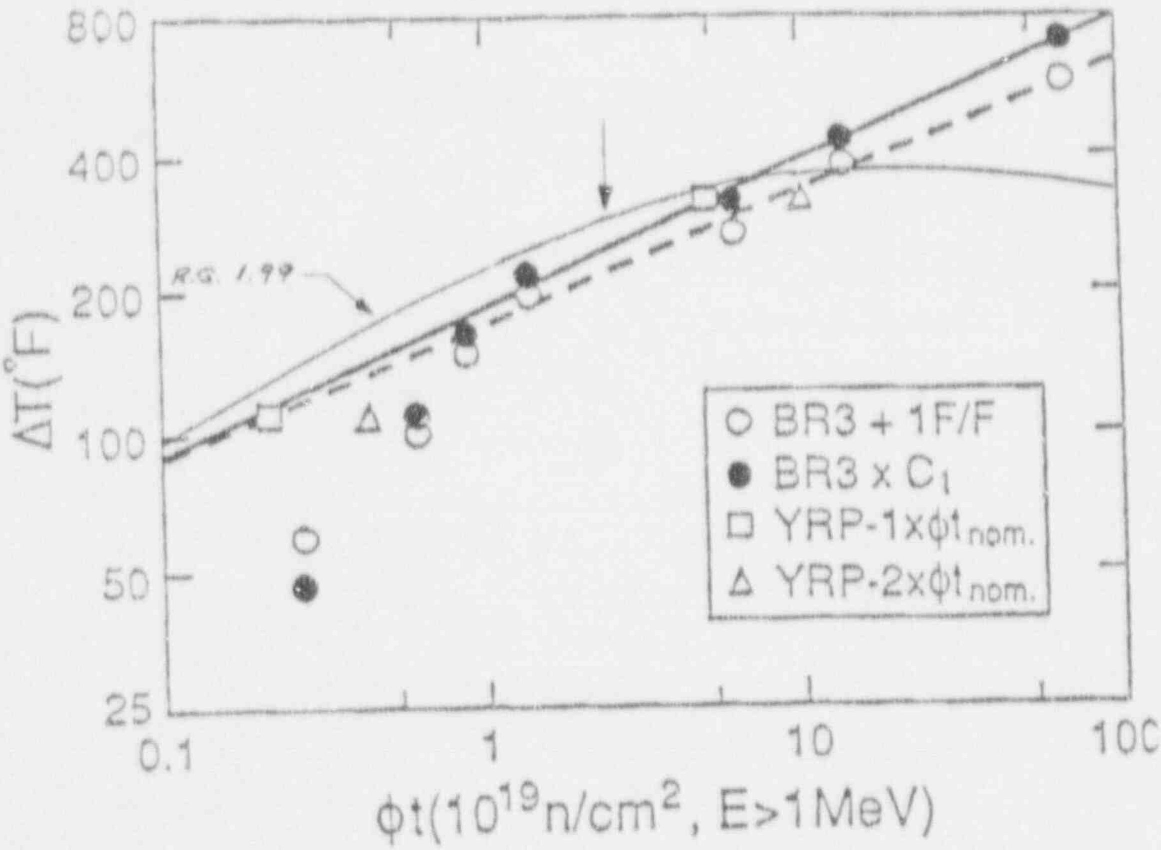

Fig. C.3. Yankee Rowe Upper Plate (YRP) and adjusted BR3 shift data vs fluence. *Source:* Hiser, Jr., A. L., NRC, "Summary of Fracture Toughness Estimates for Irradiated Yankee Rowe Vessel Materials," letter to C. Y. Cheng, NRC, with attachment, August 30, 1990.

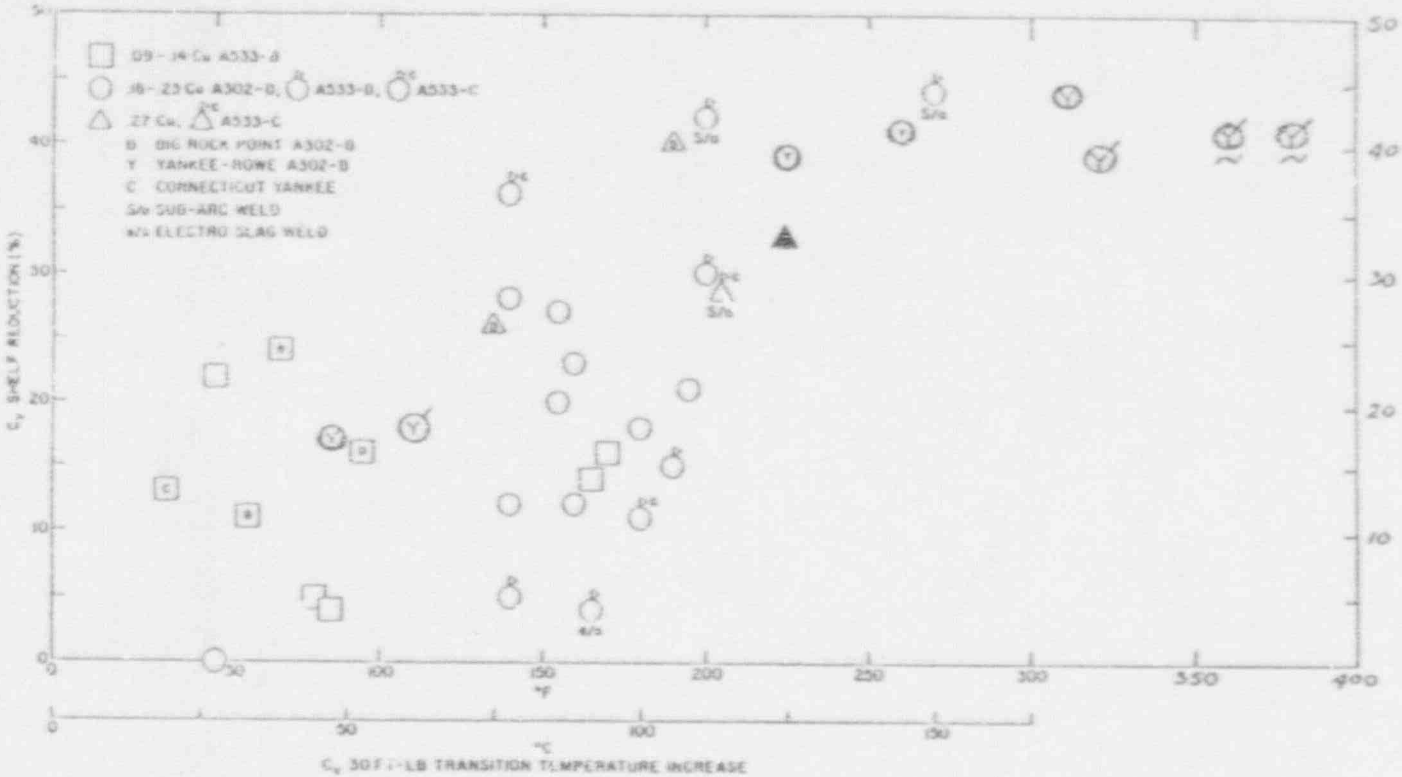

Fig. C.4. Charpy V-notch upper-shelf energy levels vs transition temperature for representative A 533 grade B and A 302 grade B steels irradiated in test reactors at 550°F (288°C) and for surveillance specimens (many from accelerated or near-core positions) irradiated at 500 to 585°F (260 to 307°C) in several commercial nuclear power plants. *Source*: Steele, L. E., *Neutron Irradiation Embrittlement of Reactor Pressure Vessel Steels*, International Atomic Energy Agency, Technical Report Series No. 163, Vienna, 1975.

~ = ESTIMATE
= YANKEE
= ASTM
▲ = HISER, R.G
1.99 ESTIMATE

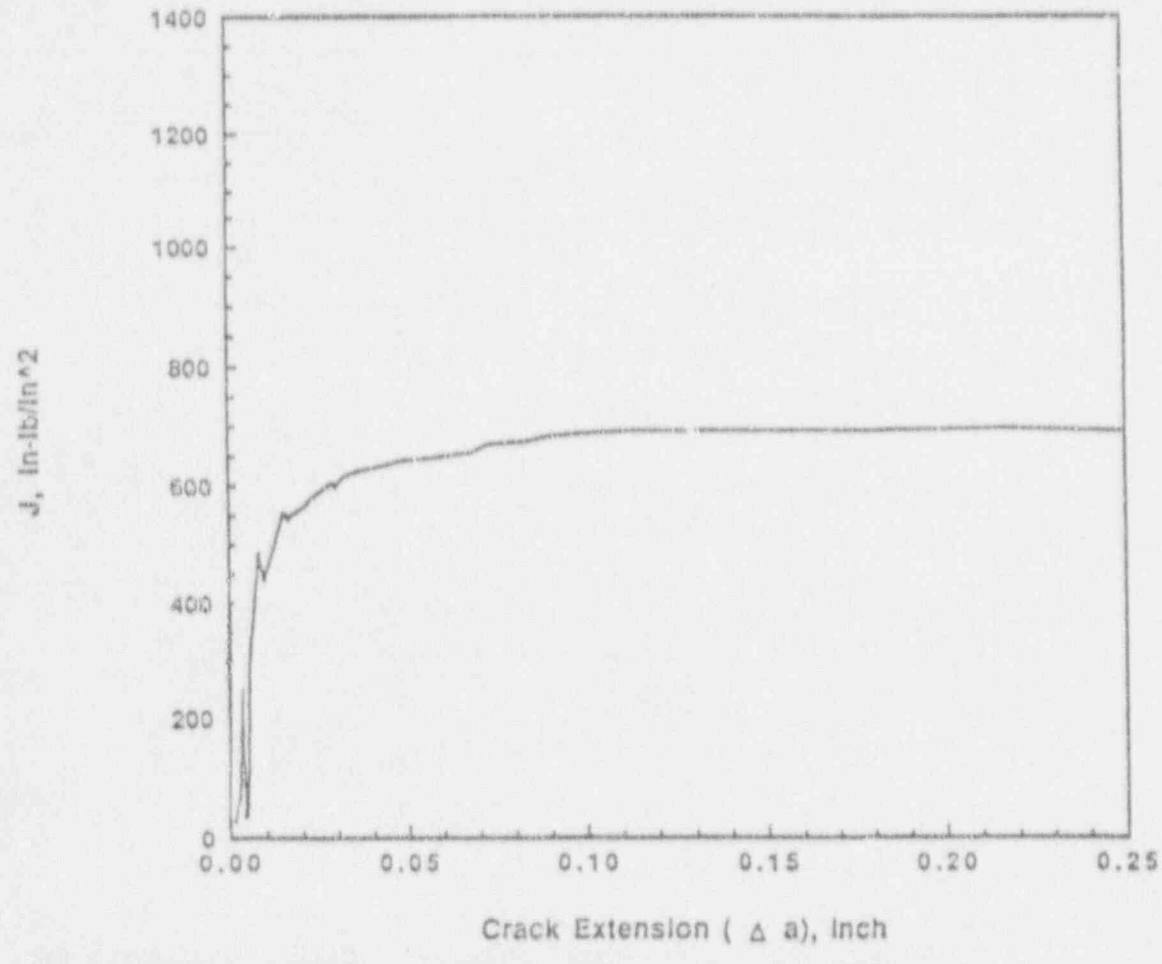

Fig. C.5. J-integral tearing resistance curve for a TL-oriented 6T-CT specimen of unirradiated A302-B steel (Refs. 1 and 36).

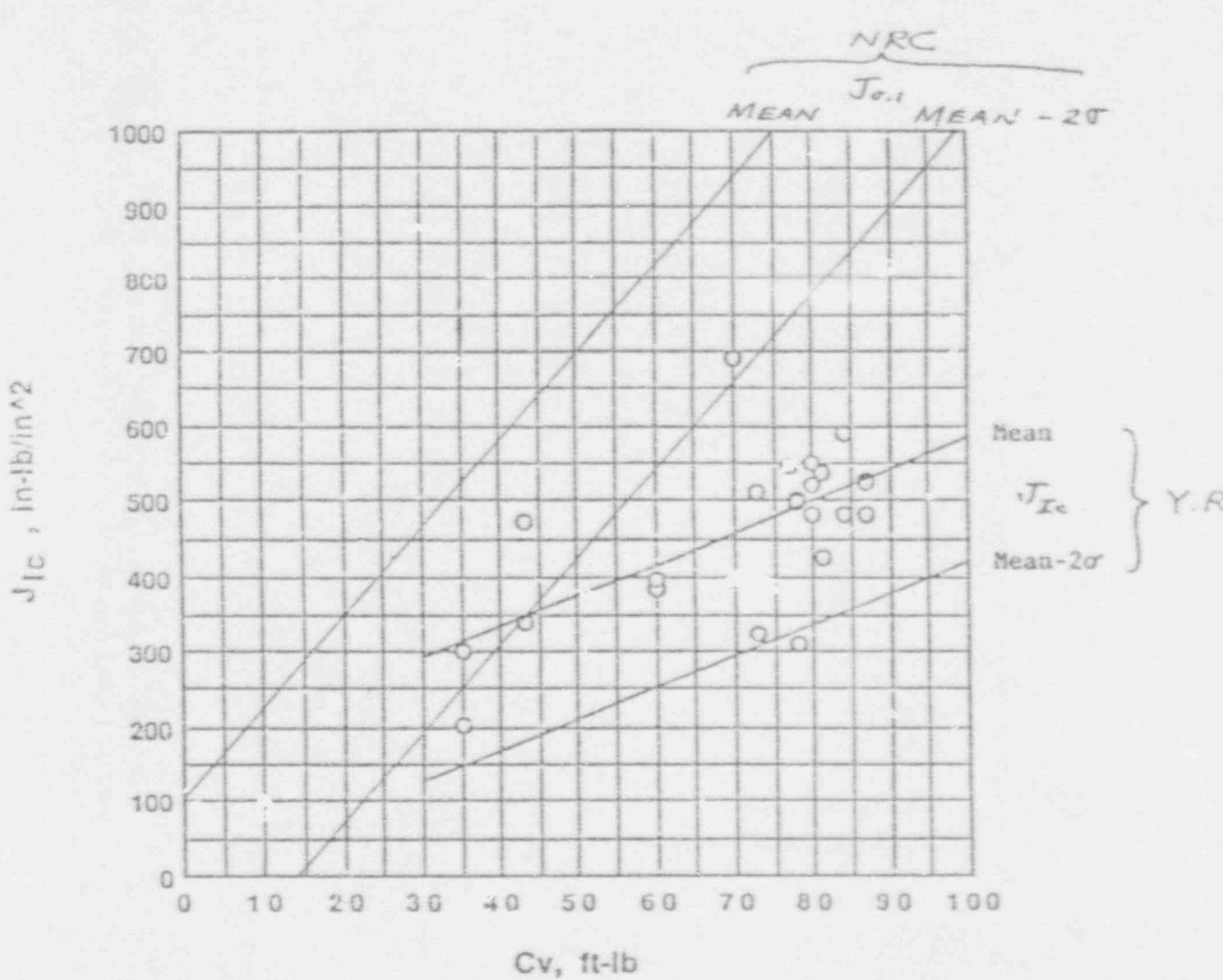

Fig. C6. Least-square fit mean and mean minus 2 sigma lines, A 302 grade B plate, LT and TL directions, 400 to 550°F. *Source: Reactor Pressure Vessel Evaluation Report*, YAEC No. 1735, Yankee Atomic Electric Company, Bolton, Massachusetts, July 1990.

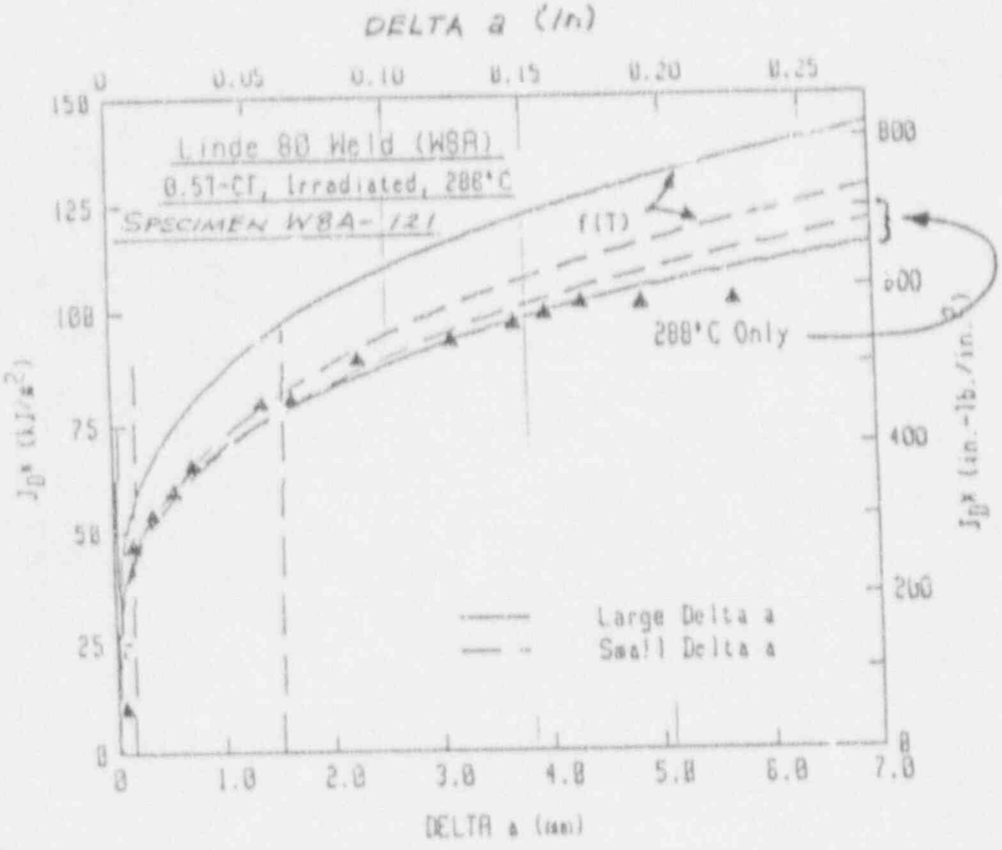

Fig. C.7. J-integral tearing resistance data for a 0.5T-CT specimen of irradiated Linde 80 submerged arc weld metal (Ref. 37).

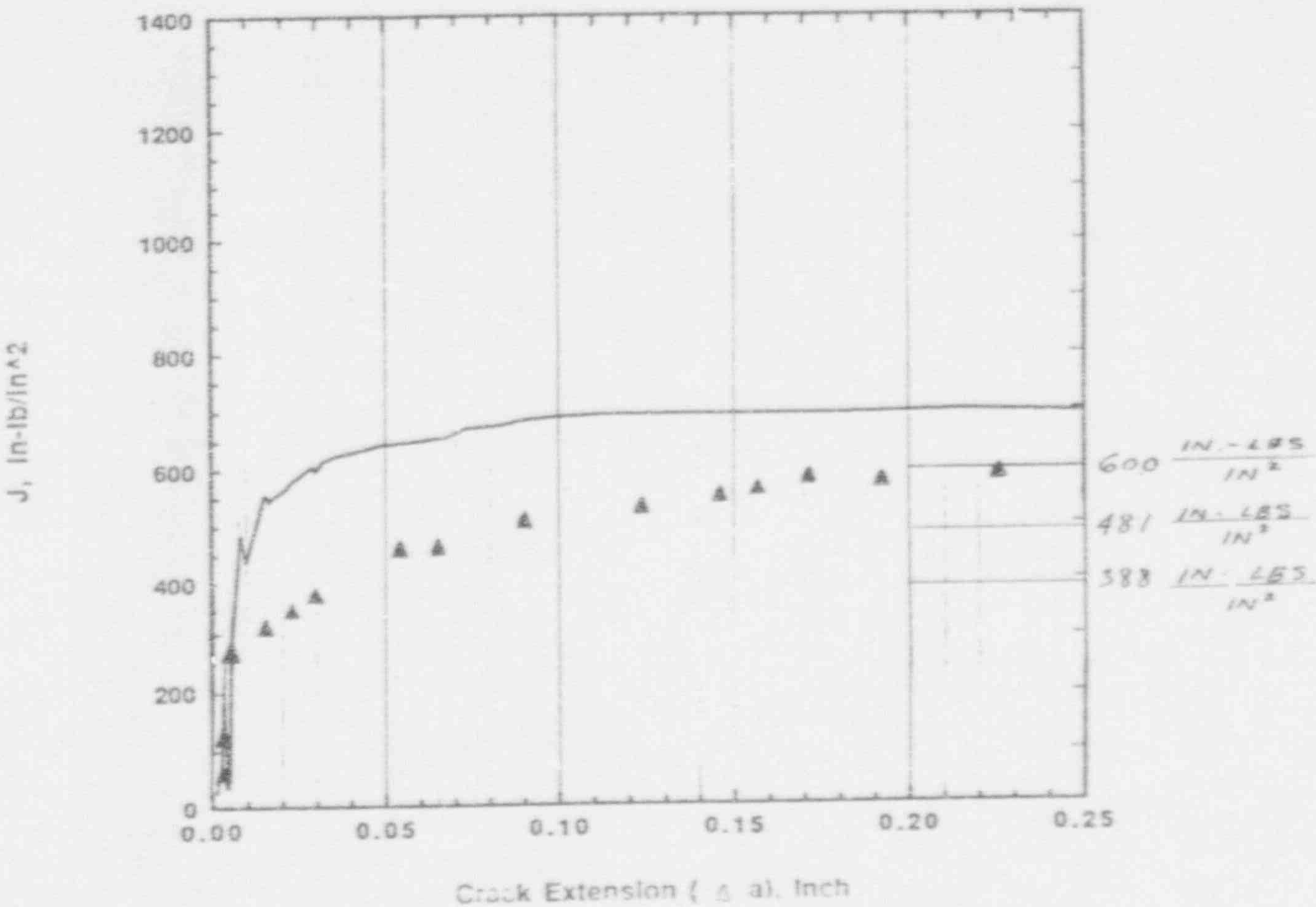

Fig. C.8. Comparison of J_{max} values calculated from Eqs. (8) and (13) with the J-R curves from Figs. C.5 and C.7.

Appendix D

ORNL Review of YAEC No. 1735 Probabilistic Fracture Mechanics

Contents

Appendix D

ORNL Review of YAEC No. 1735 Probabilistic Fracture Mechanics

T. L. Dickson
R. D. Cheverton

D.1 Introduction

Nuclear Regulatory Guide 1.154 (Ref. 1) specifies that OCA-P[2] (developed at Oak Ridge National Laboratory) and VISA-II[3] (developed by the U.S. NRC and Pacific Northwest Laboratories) are acceptable codes for performing the probabilistic fracture-mechanics analysis portion of the plant-specific safety analysis that may be performed for any nuclear plant that desires to operate beyond the pressurized-thermal-shock (PTS) screening criteria.[4] Yankee Atomic Electric Company (YAEC) has performed such an analysis for Yankee Rowe using a modified version of VISA-II. This report reviews the YAEC analysis and includes an "independent" ORNL analysis. The review is supplemented by an additional study by Simonen (Ref. 5 and Appendix E).

D.2 Scope

The original scope of work for the ORNL review of the probabilistic fracture-mechanics analyses of Yankee Rowe (as defined in the August 9, 1990, initial Yankee review meeting) was to perform a comprehensive comparison of the "baseline" VISA-II and the OCA-P probabilistic fracture-mechanics codes. The original scope was later expanded to include an independent probabilistic fracture mechanics analysis of the Yankee Rowe vessel when subjected to the YAEC-defined small-break loss-of-coolant PTS transient (SBLOCA-7), using the OCA-P code. The results of these efforts are discussed in Sect. 3 and 4, respectively.

The scope also included a discussion of the flaw-density treatment in the YAEC and ORNL analyses (Sect. D.5) and a brief discussion of some other specific features in the YAEC analysis (Sect. D.6).

D.3 Comparison of VISA-II and OCA-P

D.3.1 Comparison of Deterministic Methodologies

VISA-II and OCA-P are capable of performing a deterministic fracture-mechanics analysis of a reactor pressure vessel subjected to pressurized-thermal-shock (PTS) loading. Both codes perform a thermal analysis, linear-elastic stress analysis, and a linear-elastic fracture-mechanics (LEFM) analysis; however, the two codes use different analytical methods. Known fundamental differences utilized in the deterministic aspects of the two codes are as follows:[2,3,6]

1) Thermal analysis:
   OCA-P uses a general one-dimensional finite-element method. VISA-II uses a closed-form solution based on a slab-geometry formulation.

   OCA-P allows a point-by-point description of the thermal-hydraulic boundary conditions, i.e., the downcomer coolant temperature–time history, which is input into the thermal analysis. VISA-II fits a polynomial or an exponential (user selected) to five user-input data (time, temperature) points used to describe the downcomer coolant temperature–time history. As a result, VISA-II has a more limited, though usually adequate, capability for accurately modeling the thermal-hydraulic boundary conditions.

   OCA-P allows for accurate time-dependent modeling of the convective heat transfer coefficient. VISA-II is limited to a single value for a given analysis.

2) Stress analysis:
   OCA-P uses a general one-dimensional finite-element method. VISA-II uses a closed-form, one-dimensional solution technique.

   OCA-P allows a point-by-point description of the pressure-time loading history, which is input to the stress analysis. VISA-II fits a polynomial or an exponential (user selected) to five user-input data (time, pressure) points used to describe the pressure-time history. As a result, VISA-II has a more limited capability for accurately modeling pressure-time histories. This could be significant in cases involving complex pressure-time histories such as those corresponding to transients involving repressurization.

3) Fracture-mechanics analysis:
   Both codes perform a linear-elastic fracture-mechanics (LEFM) analysis using stress-intensity factor (K_I) influence coefficients and superposition techniques to calculate K_I values. However,

VISA-II uses a 4th-order polynomial to fit the stress distribution, and K_I influence coefficients are calculated for each of the terms. OCA-P uses a relatively large number of influence coefficients to obtain a more accurate value of K_I. Even so, for most cases, the difference in K_I is small.

It should be noted that the influence coefficients in the baseline version of VISA-II apply specifically to a vessel that has a ratio of vessel radius to wall thickness (R/w ratio) of 10. The R/w ratio for Yankee is ~7. In applying OCA-P to the Yankee Rowe vessel, influence coefficients were derived (using a finite-element technique) for the specific reactor vessel geometry.

D.3.2 Comparison of Probabilistic Methodologies

Estimation of the risk of vessel failure is carried out by means of probabilistic methods to account for the uncertainties in a number of critical parameters. The basic philosophical approaches used in VISA-II and OCA-P are essentially identical. The models are based on Monte Carlo techniques; that is, many vessels are simulated, and each is subjected to a deterministic fracture-mechanics analysis to determine whether the vessel will fail.

Each vessel is defined by randomly selected values of several parameters that are judged to have significant uncertainties associated with them, and a deterministic analysis is performed for each vessel to determine if it will fail when subjected to a specific PTS transient. In each deterministic analysis, it is assumed that each region of the vessel being analyzed contains one flaw. The calculated probability of failure for a specific vessel region, based on one flaw in the region and referred to as the unadjusted value, is equal to the number of vessels that fail divided by the total number of vessels simulated. The probability of failure based on the "actual" number of flaws in the region and referred to as the adjusted valve, is obtained by multiplying the unadjusted probability of failure by the number of flaws that are assumed to exist in that region. The total probability is obtained by adding the adjusted probabilities for each of the regions. If the total number of flaws in critical regions of the vessel is not too much greater than unity (limiting value depends on the value of the probability), double counting is not a problem; otherwise, a correction must be made for double counting (more than one flaw resulting in failure of the vessel).

These failure probabilities are referred to as conditional probabilities of failure [P(F|E)] because the PTS transient (event) is assumed to occur; the term "failure" refers to full penetration of the vessel wall by the propagating flaw.

VISA-II and OCA-P both stochastically simulate the same parameters: fast neutron fluence at the inner surface of the vessel, RT_{NDT0}, ΔRT_{NDT}, K_{Ic}, K_{Ia}, the concentrations of copper and nickel, and the size of the assumed flaw.

VISA-II uses NRC-derived mean fracture toughness curves, whereas OCA-P allows the user the option of using the NRC-derived curves or a set derived by ORNL. The latter set was utilized in the Integrated-Pressurized-Thermal-Shock (IPTS) studies.[7]

To our knowledge, no extensive comparison of the details of the probabilistic methodologies utilized by OCA-P and VISA-II had been performed prior to this effort. Personnel at Pacific Northwest Laboratories performed a comparison of the conditional probabilities of failure calculated by VISA-II and OCA-P in 1984.[8] The conclusion at that time was that VISA-II appeared to calculate conditional probabilities of failure lower than those calculated by OCA-P by approximately a factor of 6. It was concluded at that time that this difference was due to the fact that OCA-P included the stresses in the cladding whereas VISA-II did not (the present version of VISA-II does have the capability to include stresses in the cladding).

D.3.3 Comparison of VISA-II (Baseline Version) and OCA-P

The purpose of comparing OCA-P and the baseline version of VISA-II was to examine their validity, and to facilitate this effort, the VISA-II code was installed at ORNL. The Rancho-Seco PTS transient (Fig. D.1) and a vessel radius-to-wall-thickness ratio (R/w) of 10 were chosen for the comparison (this value is consistent with the stress-intensity-factor influence coefficients utilized by the baseline version of VISA-II); the initial downcomer-water and vessel temperatures were inadvertently assumed to be 590 instead of 550°F (for the purpose of comparing the solutions, this is of no significance); the potential benefits of warm-pretressing were not included in the analyses; and the preservice-inspection option in the flaw-size distribution function was not included.

D.3.3.1 Deterministic solutions

OCA-P thermal-response and stress-analysis solutions were previously successfully validated against the general-purpose, finite-element thermal and stress analysis codes ADINA-T and ADINA, respectively. VISA-II thermal-response and stress analysis solutions were previously successfully validated against the general-purpose finite-element ANSYS code. Figures D.2, D.3, and D.4 show the comparisons of the thermal-response solutions, the hoop stress solutions, and the stress-intensity-factor solutions (for longitudinal infinite-length flaws), respectively, for the Rancho-Seco transient. In each of the three cases, the solutions of VISA-II and OCA-P agree reasonably well, although the VISA-II K_I values do not reflect the expected decrease in K_I for deep flaws[9] under the specific pressure and thermal loading conditions (Fig. D.4) (this latter discrepancy is not a factor for most cases analyzed).

D.3.3.2 Probabilistic solutions

After demonstrating that the basic engineering mechanics (heat transfer, stress analysis, and fracture-mechanics analysis) solutions of VISA-II and OCA-P appeared to be in reasonably good agreement, the probabilistic solutions of VISA-II and OCA-P were compared. Initial attempts to achieve reasonable agreement were not successful. OCA-P was predicting values of P(F|E) higher than those for VISA-II by a factor of ~8. This is consistent with the results observed in the 1984 comparison of the VISA-II and OCA-P probabilistic solutions.[8]

OCA-P was enhanced to print out a more detailed event summary (number of initiations, reinitiations, crack arrests, and stable terminating crack arrests) to facilitate a more rigorous comparison of the probabilistic solutions. An examination of the event summaries indicated that the two codes were predicting the probability of crack initiation to be approximately equal; however, VISA-II was predicting lower values of P(F|E) as a result of predicting significantly more stable crack arrests than OCA-P.

An examination of OCA-P and VISA-II by flow charting down to a fairly fine level of detail was performed at ORNL. This examination revealed three areas in the VISA-II code that were thought to be the cause of the discrepancy between the two probabilistic solutions. Corrections to VISA-II appeared to be in order and were discussed and coordinated with Fred Simonen at Pacific Northwest Laboratories. After ORNL made these corrections to VISA-II, the probabilistic solutions of VISA-II and OCA-P agreed considerably better. The following tabulation illustrates the probabilistic solutions of OCA-P, baseline VISA-II, and VISA-II (with the three ORNL suggested corrections) for 100,000 trials for the Rancho-Seco PTS tranient:

	OCA-P	VISA-II	ORNL modified VISA-II
Number of Initiations	3926	3711	3422
Number of Stable Arrests	488	3275	144
Number of Failures	3438	436	3278
Probability of Initiation P(I|E)	0.039	0.037	0.034
Probability of Failure P(F|E)	0.034	0.0043	0.033

As can be concluded from the above tabulation, the ORNL specified VISA-II code modifications dramatically decreased the number of stable crack arrests predicted by VISA-II, and this significantly increased the number of failures and thus P(F|E). P(F|E) calculated by OCA-P and the modified VISA-II are nearly identical; however, the modified VISA-II predicted a smaller number of initiations and arrests than OCA-P, which indicates there is still some difference in the OCA-P and VISA-II methodologies. It is suspected that a contributing factor to the difference is the method used to implement the flaw-size distribution function in the two codes. The VISA-II and OCA-P analyses define nine possible initial flaw depths distributed according to the Marshall distribution function,[10] which is used for the YAEC and ORNL analyses. The nine depths utilized by OCA-P ranged from 0.08 to 2.08 in., whereas the nine depths utilized by VISA-II ranged from 0.125 to 3.5 in. Therefore, the initial crack depth mesh used by VISA-II is more heavily biased toward deeper flaws. It is expected that this would result in fewer initiations because of the lower values of ΔRT_{NDT} and lower thermal stresses associated with deeper flaws. It is necessary to determine the proper number and size of initial flaw depths by means of convergence studies, and this was done for the OCA-P analyses.

D.3.3.3 Summary of comparison of VISA-II/OCA-P solutions

OCA-P and the baseline version of VISA-II produce nearly the same deterministic solution for the Rancho Seco PTS event.

Three errors were discovered in the probabilistic portion of the VISA-II code, one of which results in significantly lower values of P(F|E). Upon correcting these errors, VISA-II and OCA-P produced similar probabilistic solutions, although as noted above, there is still some difference in the OCA-P and VISA-II probabilistic methodologies.

D.3.4 Details of ORNL Suggested Corrections to VISA-II Probabilistic Code

1) The flags for flaw initiation (INITIA) and arrest (IARRST) initialization were moved inside the loop for simulating a new flaw (statement 80 in the main program). This modification corrects the results of the accumulators that track the number of initiations and arrests.
2) Calculation of the nominal stress in the remaining ligament to check for plastic instability was modified (the sixth line below statement 500 in the main program) to include the crack depth (a), i.e., stress = P * (R + a)/w where:

P = pressure at time, t,
a = simulated crack depth,
R = inner vessel radius, and
w = vessel wall thickness.

Inclusion of crack depth (a) in the calculation of pressure stresses results in a larger stress in the remaining ligament and thus in more failures caused by plastic instability.

3) The call to subroutine ΔRTNDT (the thirteenth line below statement 500 in the main program) was deleted. This modification reconciled a subtle yet fundamental difference in the VISA-II and OCA-P probabilistic methodologies and dramatically reduced the number of stable crack arrests predicted by VISA-II. The significance of this code modification is evident with an under-standing of how the value of RT_{NDT} is calcula-ted. It is calculated by both VISA-II and OCA-P as follows:

$$RT_{NDT} = RT_{NDTO} + \Delta RT_{NDT} + (\sigma^2_{RTNDTo} + \sigma^2_{\Delta RTNDT})^{1/2} * ERRTN ,$$

where

RT_{NDT} = Value of RT_{NDT} adjusted for radiation, imbrittlement,

RT_{NDT0} = Initial (unirradiated) value of RT_{NDT} (User specified in input data)

ΔRT_{NDT} = increase in RT_{NDT} due to radiation (is a function of fluence attenuated to the particular crack depth; copper; and nickel),

σ_{RTNDT0} = 1σ uncertainty for the specified value of RT_{NDT0},

$\sigma_{\Delta RTNDT}$ = 1σ uncertainty in the correlation used to calculate ΔRT_{NDT}.

ERRTN is a number between −3 and +3 that is obtained from a normal distribution having a mean of zero and a 1σ of 1. The product of ERRTN and $(\sigma^2_{RTNDT0} + \sigma^2_{\Delta RTNDT})^{1/2}$ is the uncertainty in $(RT_{NDT0} + \Delta RT_{NDT})$.

A fundamental difference in the baseline VISA-II and the OCA-P probabilistic methodologies is that OCA-P calculates a value of ERRTN once for each simulated vessel and uses this value throughout the wall thickness when checking for either crack initiation or arrest. VISA-II calculates a value of ERRTN when checking for crack initiation and then recalculates ERRTN for each crack-depth increment when advancing the crack tip through the thickness of the vessel wall (in 0.25-in. increments) checking for crack arrest. This randomness enhances the probability (relative to the methodology utilized in OCA-P) of crack arrest because it increases the chances of a very high crack-arrest fracture-toughness value at at least one of the 0.25-in. increment check points for arrest. This is reflected in the considerably higher number of stable crack arrests predicted by VISA-II.

In reality there is variability of copper and nickel concentrations through the wall; however, the approach adopted by both VISA-II and OCA-P assumes that copper and nickel, for a specific vessel, have no variability through the wall. Therefore, to be consistent with this assumption, $(\sigma^2 RT_{NDT0} + \sigma^2 \Delta RT_{NDT})^{1/2}$* ERRTN should also be assumed to be constant through the wall for a specific vessel. The approach utilized by VISA-II is equivalent to depending on inhomogenities in the wall to enhance the probability of crack arrest. This is a nonconservative approach.

Deleting the specified call to subroutine ΔRTNDT in VISA-II described above results in VISA-II calculating a value of ERRTN once per simulated vessel. This is consistent with the methodology utilized in OCA-P.

The impact of the first two code changes on the probabilistic solution of VISA-II was detectable but was not significant with regard to the calculated value of P(F|E). The result of the third modification dramatically decreased the number of stable crack arrests and thus increased the number of failures and P(F|E).

D.4 OCA-P Applied to Yankee Rowe

D.4.1 Deterministic Analysis

The OCA-P code was used to perform an independent analysis of the Yankee Rowe vessel with the plant subjected to the YAEC-specified SBLOCA7 PTS event (Fig. A-1). Based on recent data from YAEC and the ORNL evaluation of RT_{NDT} addressed elsewhere in this document (Appendix C), the upper axial weld was selected for a detailed analysis of the conditional probability of failure. Other transients and vessel regions also contribute to the overall frequency of failure; however, considering the preliminary nature of this study and the limited time for its completion, it was sufficient to conduct a detailed analysis of what were believed to be the dominant transient and region.

Input data used in the ORNL OCA-P and the YAEC VISA-II heat transfer and stress analyses for Yankee Rowe are specified in Table D.1 (note that the ORNL analysis included cladding as a discrete region but the YAEC analysis did not).

The thermal-response solutions predicted by VISA-II and 1R (the finite-element thermal code used in conjunction with OCA-P) are illustrated in Fig. D.5. As indicated, the VISA-II temperatures are a little lower in the base material. This is because the VISA-II analysis did not include the cladding, and because it was based on slab geometry.

Figure D.6 illustrates the SBLOCA 7 pressure transient. VISA-II uses a polynomial to fit five points in time, whereas OCA-P allows a more accurate point-by-point description of the transient. This accounts for the difference indicated. Also illustrated in Fig. D.6 is the SBLOCA7 transient with repres-surization to 1.55 ksi (maximum head of safety injection system)[11] at an assumed time of 20 min.

Figure D.7 shows the hoop-stress solutions predicted by VISA-II and OCA-P for the Yankee vessel when subjected to the SBLOCA7 transient. Both of the OCA-P hoop-stress solutions illustrated in Fig. D.7 included the 0.109-in. cladding; the VISA-II solutions did not. The higher of the two OCA-P solutions includes a weld residual tensile stress of ~6 ksi.

ORNL also compared baseline VISA-II and OCA-P K_I values, even though the VISA-II values correspond to K_I influence coefficients for R/w = 10, and those for OCA-P correspond to the actual Yankee geometry (R/w ~ 7). As indicated in Fig. D.8, for a/w < 0.5, the agreement is very good. Since most initial crack initiations correspond to a/w < 0.5, a comparison of VISA-II/Yankee and OCA-P/Yankee is meaningful.

D.4.2 Probabilistic Analysis

Input data and correlations used in the OCA-P probabilistic fracture-mechanics analyses for Yankee Rowe are presented in Table D.2, while Table D.3 indicates differences between the input and correlations used in the ORNL OCA-P analysis and the YAEC VISA-II analysis.

The increase in RT_{NDT} is a function of fluence (attenuated to the specific wall-depth location) and the concentrations of copper and nickel. OCA-P was modified to exactly reproduce ΔRT_{NDT} predicted in the "weld table" of Regulatory Guide 1.99, Rev. 2, plus a low-temperature-operation correction factor of 44°F, which is based on an irradiation-temperature correction factor of 1°F additional increase per 1°F irradiation temperature below 550°F and operating data included in Ref. 11.

The ORNL mean K_{Ia} and K_{Ic} fracture toughness curves utilized in the IPTS studies[7] were used in OCA-P for the Yankee analysis. The maximum value of toughness at which crack arrest could occur was specified to be 200 ksi$\sqrt{\text{in.}}$, in accordance with Ref. 7. Additional sensitivity analyses were performed using a value of 140 ksi$\sqrt{\text{in.}}$, but P(F|E) was not significantly impacted by this lower value. Also, in accordance with Regulatory Guide 1.154, the potential benefits of warm-prestressing were not included in the analysis. The analysis did assume a preservice inspection as formulated by the Marshall flaw nondetection function.

Analyses were performed for the SBLOCA7 transient described in the YAEC report[12] (no repressurization) and also for a case involving repressurization to 1.55 ksi at a time of 20 min, considering only the upper axial weld in detail. The results of these analyses are presented in Fig. D.9 as a function of the mean copper concentration, considering two values of the copper standard deviation ($1\sigma = 0.025$ and 0.07). The actual weld chemistry is not known for the Yan-kee Rowe vessel, but a best estimate of 0.29 weight percent copper (mean with $1\sigma = 0.07$) and 0.7 weight percent nickel ($1\sigma = 0.0$) was deduced from Ref. 12.

The corresponding "best-estimate" value of P(F|E) with repressurization and residual stresses is 2×10^{-3}. A rough estimate of the corresponding mean value of P(F|E) is 9×10^{-2}, which was obtained by multiplying the "best-estimate" value by 45, the ratio of mean flaw density to best-estimate flaw density derived in the IPTS study for H.B Robinson[7] (the corresponding value in the YAEC Yankee Rowe analysis was 55 flaws/m^3 for the upper weld).

The other regions of the vessel (plate and other welds) will contribute to P(F|E); however, a more sophisticated analysis is required because of double-counting problems (more than one flaw per vessel) introduced by the specific OCA-P methodology. An appropriate analysis to account for all regions has not been performed yet.

D.5 Flaw-Density Considerations

The conditional probability of vessel failure is directly proportional to the number of flaws in critical regions of the vessel, provided that the total number of flaws in critical regions is one or less. With more than one flaw, a direct proportionality may fail because only one flaw can result in failure. However, with more than one flaw the chances of failure tend to increase because the chances of having a critical flaw size are increased, but the increase is not proportional to the number of flaws.

In the IPTS study, the "best estimate" of the flaw density for surface flaws normal to the surface was 1 flaw/m^3. This flaw density was assumed appropriate for all regions (weld and plate) because it is believed that the existence of shallow surface flaws is most likely associated with the cladding process and attack of the cladding. There is, of course, a large uncertainty with regard to the surface density of shallow flaws, one reason being that they are extremely difficult to detect. Because of the very large shallow-flaw surface densities "known" to exist in the Sequoyah and Loviisa II vessels and the large uncertainties, a log normal distribution was assumed for the IPTS studies. The most probable value was 1 flaw/m^3, the 84th percentile (+1σ) was 100 flaws/m^3, and the distribu-tion was truncated at the 94th percentile (500 flaws/m^3). The corresponding mean value was 45 flaws/m^3. (It is of interest to note that YAEC assumed essentially the same flaw density for the upper weld but much lower densities for the plate regions.)

More recently, flaw-density data have been obtained from sections of the Hope Creek and Midland vessels. The corresponding surface densities were 6 and 7 flaws/m^2 (Ref. 13), while the surface density corresponding to 45 flaws/m^3 is 11 flaws/m.^{2} If it is assumed that the Hope Creek and Midland values are the most probable, and that a log-normal distribution with a substantial standard deviation is reasonable, the mean values are substantially greater than 11 flaws/m^2. Thus, it appears that 45 flaws/m^3 is not necessarily a conservative mean value.

Considering the volume of the Yankee Rowe upper axial weld and a flaw density of 45 flaws/m^3, the number of flaws per weld is ~1, in which case there are no problems with double counting, if only that weld contributes significantly to P(F|E).

It appears that the upper axial weld is not the only significant contributor to P(F|E). As shown in Table 3 of Ref. 14, the value of RT_{NDT} for the upper plate is about the same as that for the upper axial weld (~300°F). Assuming the high-fluence region of the upper plate to be substantially broader (azimuthally) than the weld region and assuming the flaw densities for the two regions to be the same (for reasons mentioned above) the contribution of the plate region would be substantially greater than that of the weld. Under these conditions there is more than one flaw total in all regions of concern, and, thus, P(F|E) is no longer directly proportional to the number of flaws. P(F|E) will, however, be substantially greater than P(F|E) for the weld alone.

D.6 Discussion of Specific Features in the YAEC Analysis

D.6.1 Number of Subregions Considered in Beltline Region

The YAEC approach was to divide the beltline region into five subregions (upper and lower plate, upper and lower axial welds, and circumferential weld). The plate and axial-weld regions were further subdivided longitudinally to take advantage of the decrease in fluence toward the ends of the core. Assuming that the initial axially oriented flaws are short enough to fall within the height of a subregion, this procedure provides an accurate account of the potential for initial initiation of axially oriented flaws. However, once initiated, the flaw extends in surface length beyond the borders of the specific subregion, and thus a higher fluence must be used for arrest and reinitiation. YAEC did not incorporate the latter feature, and thus initial initiations tend to be treated accurately, but arrest and reinitiation tend to be treated nonconservatively. The degree of nonconservatism is negligible for initial flaws near midheight of the core, where the neutron flux is a maximum and flat. For flaws near the end of the core, the error can be substantial.

Division of the plate regions azimuthally to take advantage of the azimuthal variation in flux could also be considered but was not. Instead, the flaw in the plate was always assumed to be at peak flux in the azimuthal direction. This is a conservative approach.

D.6.2 Flaw Density

The flaw density assumed by Yankee for the upper axial weld was 55 flaws/m^3 and for the plate about a factor of 200 less. The value of 55 flaws/m^3 is nearly the same as the mean value used in the ORNL IPTS studies[7] for all regions. ORNL believes, as mentioned in Sect. D.5, that surface flaws are most likely the result of the cladding process and/or some type of attack, such as stress corrosion cracking, in which case surface flaws are probably just as likely over base-metal as over welds. Thus, ORNL believes that higher flaw densities should be considered for the plate regions.

D.6.3 Flaw Configuration

Reference 11 states that infinite-length flaws were used for the initial initiating events and for subsequent events in the welds and upper plate, while a 47 in.-long semielliptical flaw was used for subsequent events in the lower plate. The YAEC VISA-II input data sets indicate that 6/1 semielliptial flaws were used for initial initiation events, and for subsequent events (arrest and reinitiation) 47-in.-long semielliptical flaws were used for the lower plate and 94-in. flaws for the upper plate. The ORNL IPTS studies[7] considered both infinite-length and finite-length flaws for subsequent events, and the results indicated little difference in the calculated value of P(F|E) for the document transients, which were high pressure. For low-pressure transients the effect was much larger; however, ORNL has not conducted a similar comparison for Yankee Rowe.

D.6.4 VISA-II Code Errors

It is assumed that the version of VISA-II used by YAEC to perform the Yankee Rowe analysis contained the three coding errors discussed in Sect. D.3.4. Therefore, it is suspected that the results of the YAEC analysis would under predict P(FIE) because of the tendency to over predict the number of stable crack arrests.

References

1. U.S. Nuclear Regulatory Commission, Regulatory Guide 1.154, "Format and Content of Plant-Specific Pressurized Thermal Shock Safety Analysis Reports for Pressurized Water Reactors".[a]

2. R. D. Cheverton and D. G. Ball, *OCA-P, A Deterministic and Probabilistic Fracture Mechanics Code for Application to Pressure Vessels*, NUREG/CR-3618 (ORNL-5991), Union Carbide Corp., Nuclear Division, Oak Ridge National Lab., Oak Ridge, TN, May 1984.[a]

3. F. A. Simonen et al., *VISA II – A Computer Code for Predicting the Probability of Reactor Pressure Vessel Failure*, NUREG/CR-4486 (PNL-5775), Pacific Northwest Lab., Richland, WA (March 1986).[a]

4. Code of Federal Regulations, Title 10, Part 50, Section 50.61 and Appendix G.[a]

5. Letter from F. A. Simonen, Pacific Northwesst Laboratories, to R. D. Cheverton, Oak Ridge National Laboratory, "Review of Yankee Atomic PTS Report," October 29, 1990.[b]

6. Manfred Geib, *Verification of OCA-P and VISA II on Behalf of Strains and Stresses Induced During HDR–TEMB Thermal Mixing Tests*, Battelle-Institute, Frankfort, Germany.[b]

7. D. L. Selby et al., *Pressurized Thermal Shock Evaluation of the H.B. Robinson Nuclear Power Plant* NUREG/CR-4183 (ORNL/TM-9567), Martin Marietta Energy Systems, Inc., Oak Ridge National Lab., Oak Ridge, TN, September 1985.[a]

8. Personal written communication from F. A. Simonen of Pacific Northwest Laboratories to D. G. Ball of Oak Ridge National Laboratory, March 29, 1984.[b]

9. J. G. Merkle, *An Approximate Stress Intensity Factor Solution for a Deep Inside-Surface Longitudinal Crack in a Cylinder Under Thermal Loading*, ORNL/NUREG/TM-3, April, 1976.[a]

10. W. Marshall, "An Assessment of the Integrity of PWR Pressure Vessels," October 1976.[c]

11. Yankee Atomic Electric Company, *Reactor Pressure Vessel Evaluation Report for Yankee Nuclear Power Station*, YAEC-1735, July 9,1990.[c]

12. K. E. Moore and A. S. Heller, *B&W 177-FA Reactor Vessel Beltline Weld Chemistry Study*, BAW-1799, B & W Owner's Group Materials Committee, July 1983.[c]

13. W. E. Pennell, "Heavy-Section Steel Technology Program Overview," presented at the 18th Water Reactor Safety Information Meeting, Rockville, MD, October 22-24, 1990.[b]

14. Written communication from Yankee Atomic Electric Company to Mr. William Russell of United States Nuclear Regulatory Commission, September 28, 1990.[c]

[a]Available for purchase from G PO Sales Program.
[b]Available from National Technical Information Service.

[a]Available for purchase from G PO Sales Program.
[b]Available from National Technical Information Service.
[c]Available from NRC Public Document Room for a fee.

Table D.1. Input data used in the ORNL OCA-P and the YAEC VISA-II thermal and stress analyses of Yankee Rowe

Vessel dimensions:

Vessel Inner Radius = 54.5 in.

Wall thickness = 7.875 in.

Cladding thickness = 0.019 in.

Cladding properties[a,b]:

Modulus of Elasticity (E) = 27,000 ksi

Poisson's ratio (v) = 0.3

Thermal expansion coefficient (α_{clad}) = 9.9E-6/F

Thermal Conductivity (k) = 10 BTU/hr-ft-F

Specific Heat (cp) = 0.12 BTU/lb-F

Density (ρ) = 488 lb/ft^3

Base-metal properties[a,c,d]

Modulus of Elasticity (E) = 28000 ksi

Poisson's ratio (v) = 0.3

Thermal expansion coefficient (α_{base}) = 7.85E-6/F

Thermal Conductivity (k) = 24 BTU/hr-ft-F

Specific Heat (cp) = 0.12 BTU/lb-F

Density (ρ) = 488 lb/ft^3

Temperature

Vessel initial temperature = 515°F

Water initial temperature = 515°F

Coefficient of convective heat transfer=504 BTU/hr°ft²°F

[a]No temperature dependence of material properties included in analyses.

[b]The YAEC analysis did not include cladding in either the thermal or stress analysis.

[c]VISA-II requires an input value for $E*\alpha_{base}/(1-v)$ rather than input for each of the individual parameters. The YAEC analysis used $E*\alpha_{base}/(1-v) = 0.312$. Using the OCA-P input values for E,α_{base}, and v yields a value for $E*\alpha_{base}/(1-v)$ of 0.314. This difference is not significant.

[d]The Thermal Diffusivity $\frac{k}{\rho c_p}$ of the base metal used by YAEC was 0.953 in.^{2}/min. For OCA-P it was 0.982 in.^{2}/min. This difference is not significant.

Table D.2. Correlations and values of parameters used in OCA-P probabilistic fracture-mechanic analysis of Yankee Rowe

Volume of weld = 0.63 ft^3

Flow stress = 80.0 ksi

Flaw Data:

Flaw density = 1 flaw/m^3 (0.03 flaws/ft^3)

Number of crack increments to be used for initial crack depth = 9

Size of first crack depth increment = 0.169 in.

Extreme dimension of deepest crack depth increment = 2.25 in.

Marshall flaw size distribution function used

Marshall flaw nondetection function used (simulates preservice inspection and repair)

Flaws were assumed to be axially oriented and infinitly long

Fracture-Toughness Data:

K_{Ic} and K_{Ia} mean curves same as those used in the original IPTS studies, i.e.,:

K_{Ia} mean = 1.25* ASME lower bound K_{Ia} curve

K_{Ic} mean = 1.43* ASME lower bound K_{Ic} curve

Maximum K_{Ia} = 200 Ksi $\sqrt{in.}$, 140 ksi $\sqrt{in.}$ [a]

K_{Ia} standard deviation = 0.15 $\bar{K}_{Ia}$

K_{Ic} standard deviation = 0.10 $\bar{K}_{Ic}$

K_{Ic} truncation = $\pm 3\sigma$

K_{Ia} truncation = $\pm 3\sigma$

RT_{NDT} Data:

RT_{NDT_0} = +0°F

RT_{NDT_0} standard deviation = 17°F

ΔRT_{NDT_0} calculated by Regulatory Guide 1.99, Rev. 2 (Welds) with an additional 44°F added as a correction factor for the low temperature operation of the Yankee plant (44°F = 550 - 506°F)

ΔRT_{NDT} truncation = $\pm$ 3σ

Fluence at inner vessel wall = 1.24E+19 n/cm^2

Fluence standard deviation (fraction of mean) = 0.3

Fluence variability truncation = $\pm 3\sigma$

Mean copper content = various values

Mean nickel = 0.7 wt%

Copper standard deviation = 0.025 and 0.07 wt%

Nickel standard deviation = 0.0%

[a]Used 140 ksi $\sqrt{in.}$ for sensitivity study; however, this did not significantly impact the calculated conditional probabilities of failure.

Table D.3. VISA-II/YAEC probabilistic fracture-mechanics analysis input data that were different from those used in the ORNL OCA-P analysis

The YAEC Analysis:

1) used the NRC mean K_{Ic} and K_{Ia} fracture toughness curves;
2) did not simulate a preservice inspection;
3) used ΔRT_{NDT} values specified by NRC;
4) used Regulatory Guide 1.99, Rev. 2, to calculate ΔRT_{NDT}; however, no correction factor for low-temperature operation was included;
5) assumed zero variability for RT_{NDT_0} ($1\sigma = 0$);
6) assumed $1\sigma = 28°F$ for ΔRT_{NDT};
7) used a flow stress of 75.6 ksi;
8) used $1\sigma = 10\%$ of mean for inner surface fluence;
9) truncated variability of fluence at 1σ;
10) assumed flaws were axially oriented, and were semielliptical with aspect ratio equal 6/1 for initial initiation and 47-in. long for arrest and reinitiation (lower plate and axial weld);
11) did not treat cladding as a discrete region; and
12) used flaw-depth increments greater than those used by ORNL (may not be converged).

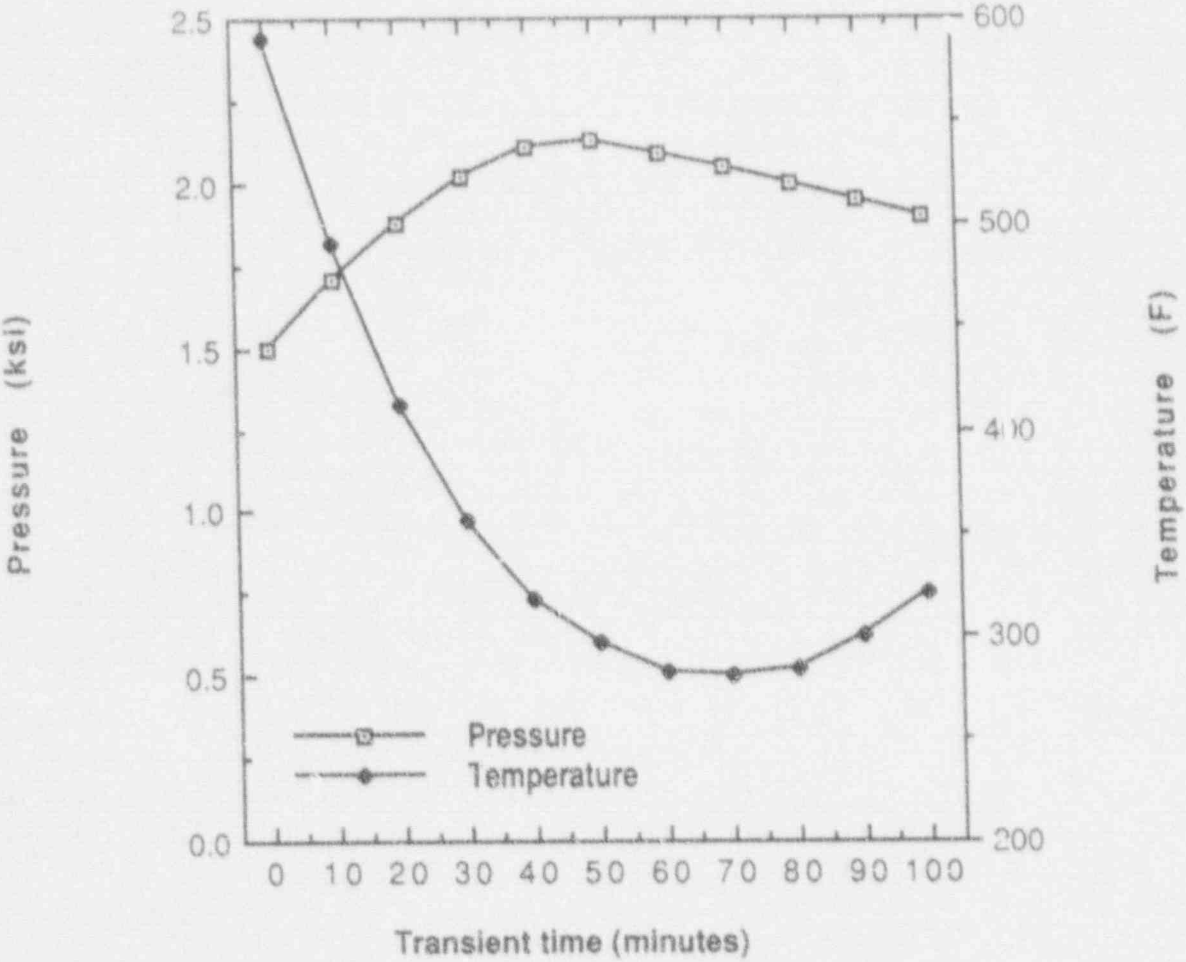

Figure D.1. Approximate Rancho-Seco PTS event.

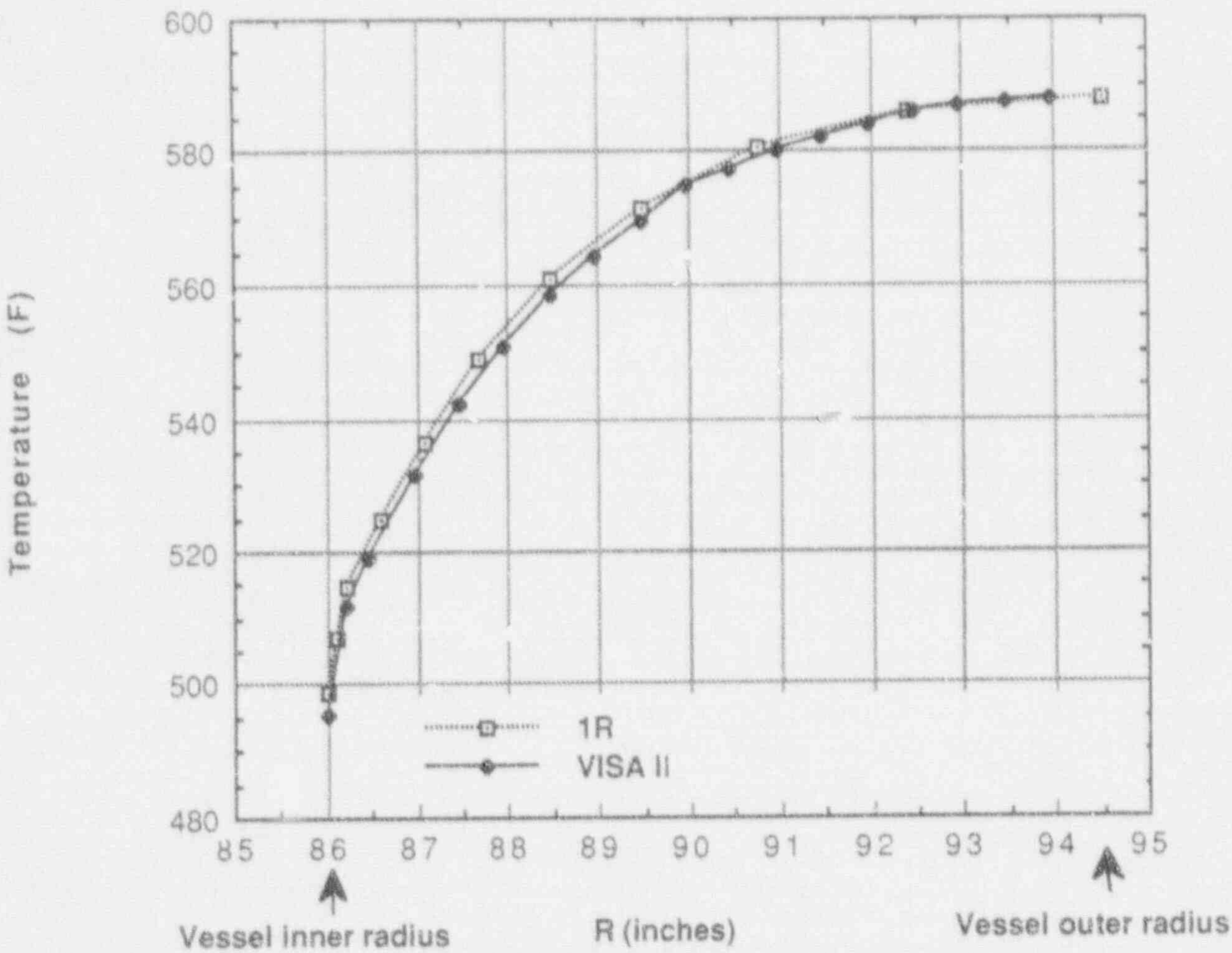

Figure D.2. VISA-II and 1R predicted temperature distributions at time = 10 min. for Rancho-Seco thermal transient (R/w ~10).

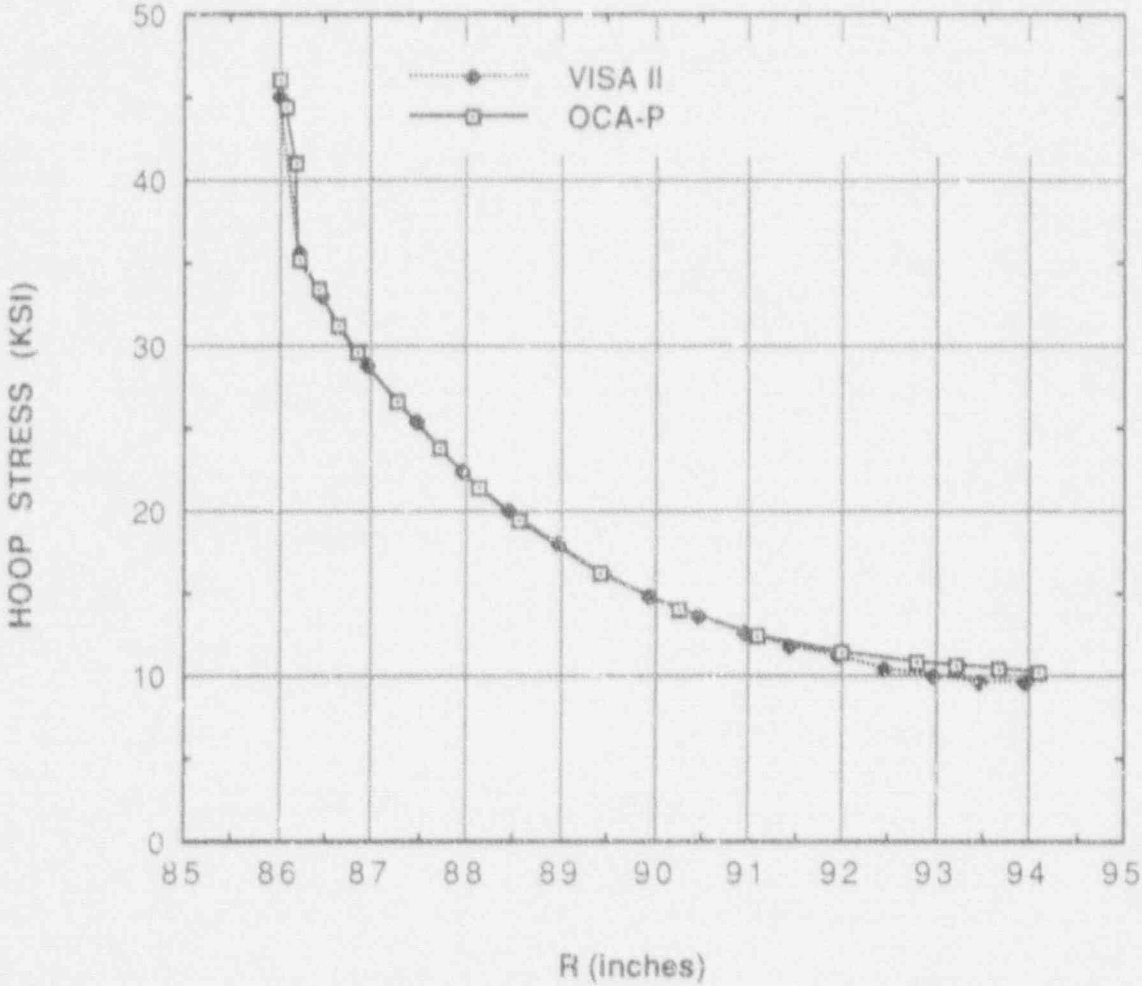

Figure D.3. VISA-II and OCA-P predicted hoop stress distributions at time = 10 min. for Rancho-Seco PTS event (R/w ~10).

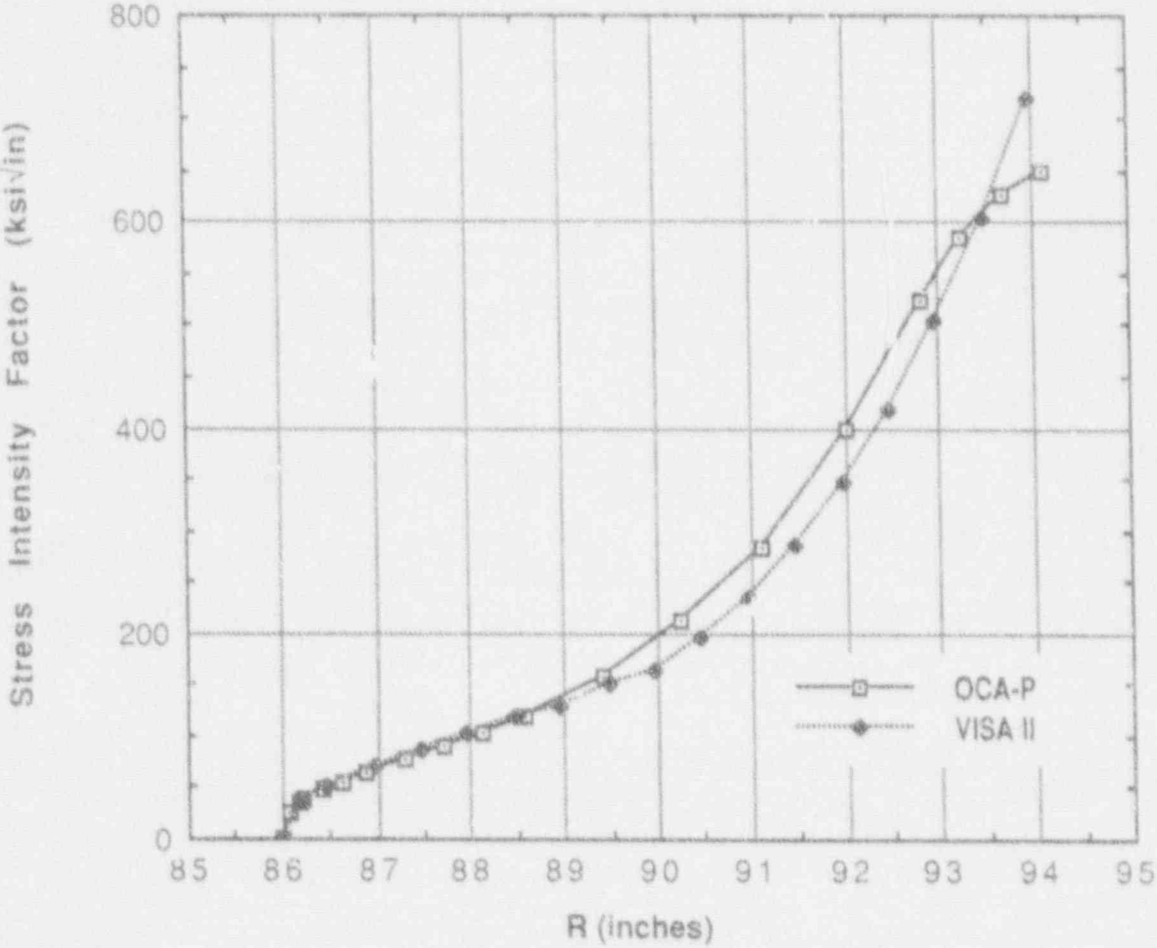

Figure D.4. VISA-II and OCA-P predicted stress intensity factors at time = 10 min. for Rancho-Seco PTS event (R/w ~10).

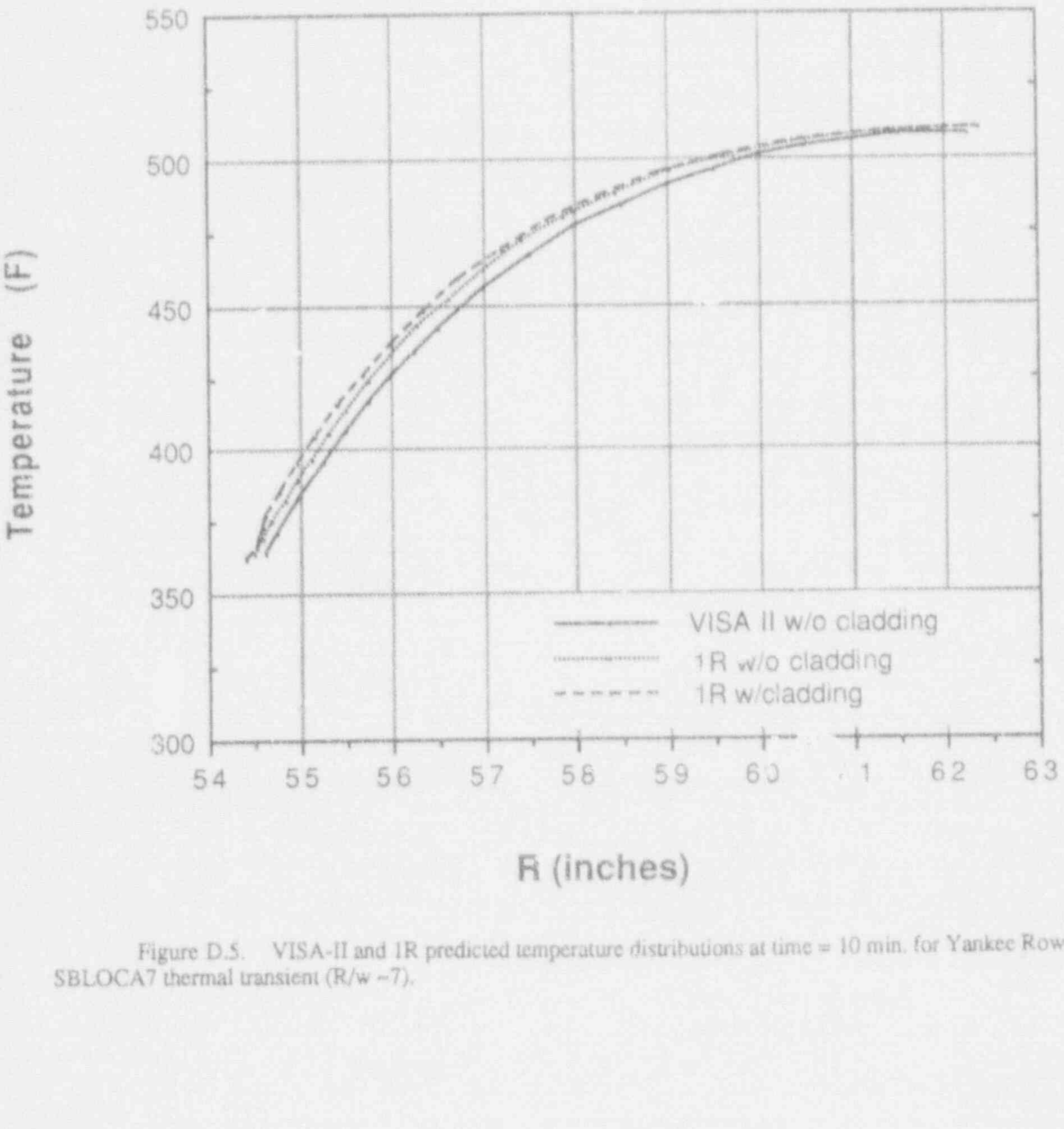

Figure D.5. VISA-II and 1R predicted temperature distributions at time = 10 min. for Yankee Rowe SBLOCA7 thermal transient (R/w ~7).

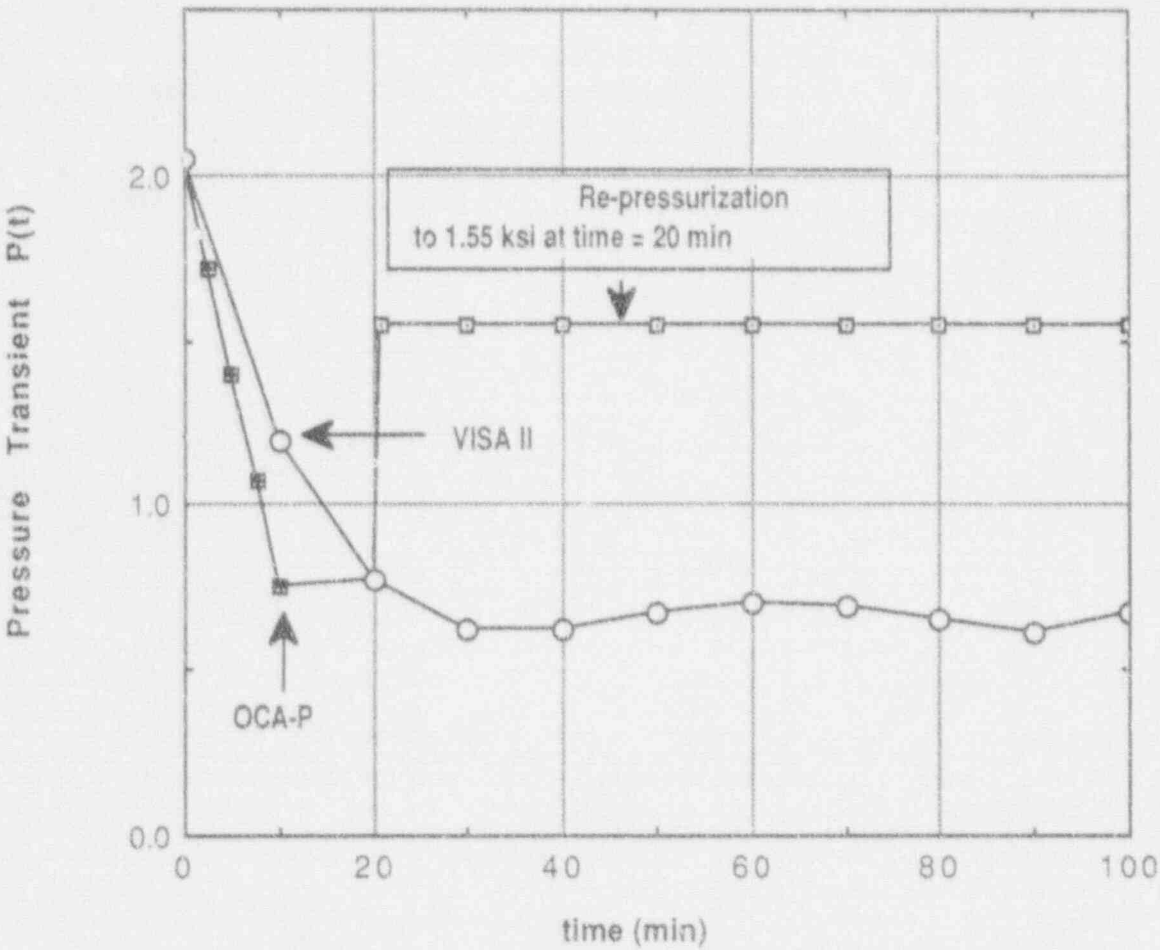

Figure D.6. SBLOCA7 pressure transients.

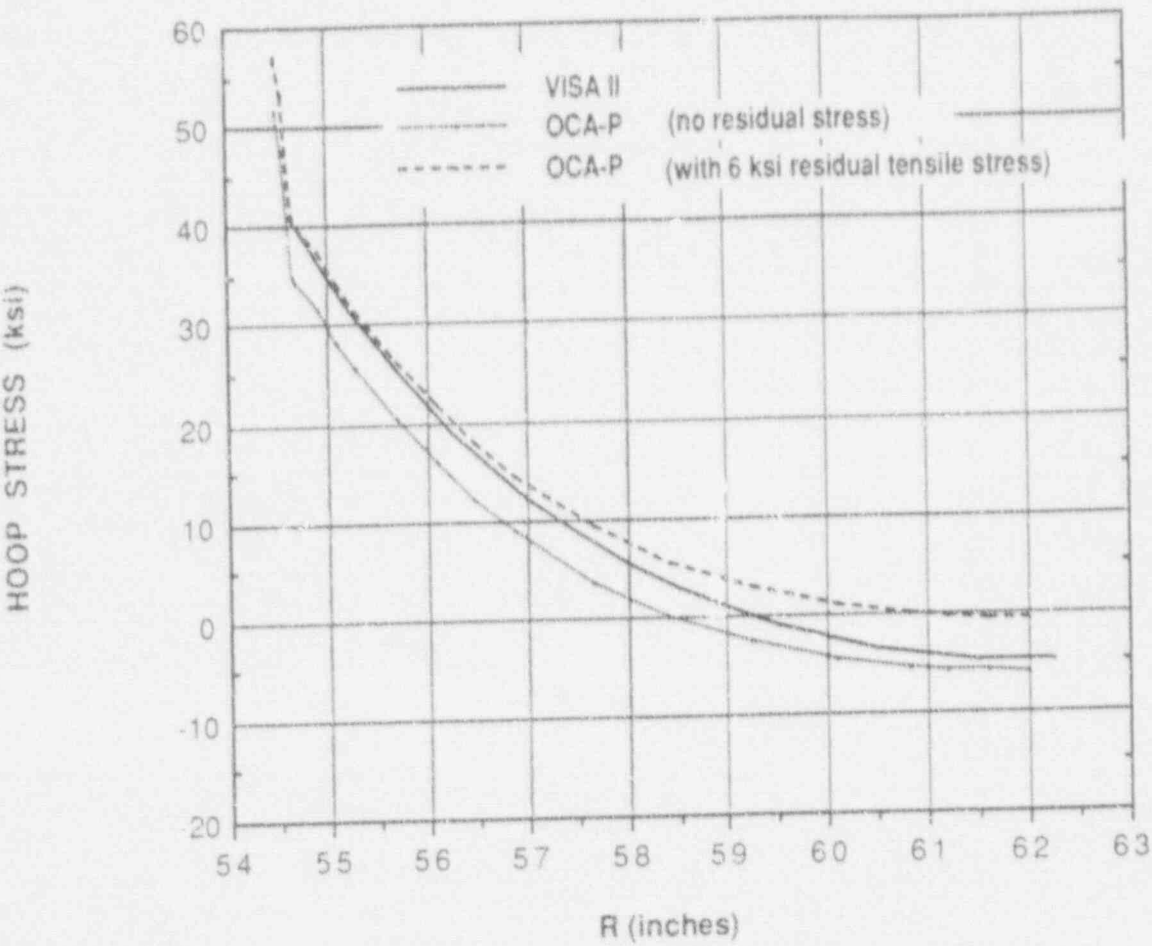

Figure D.7. VISA-II and OCA-P pedicted hoop stress distributions at time = 10 min. for Yankee Rowe SBLOCA7 PTS event (R/w ~7).

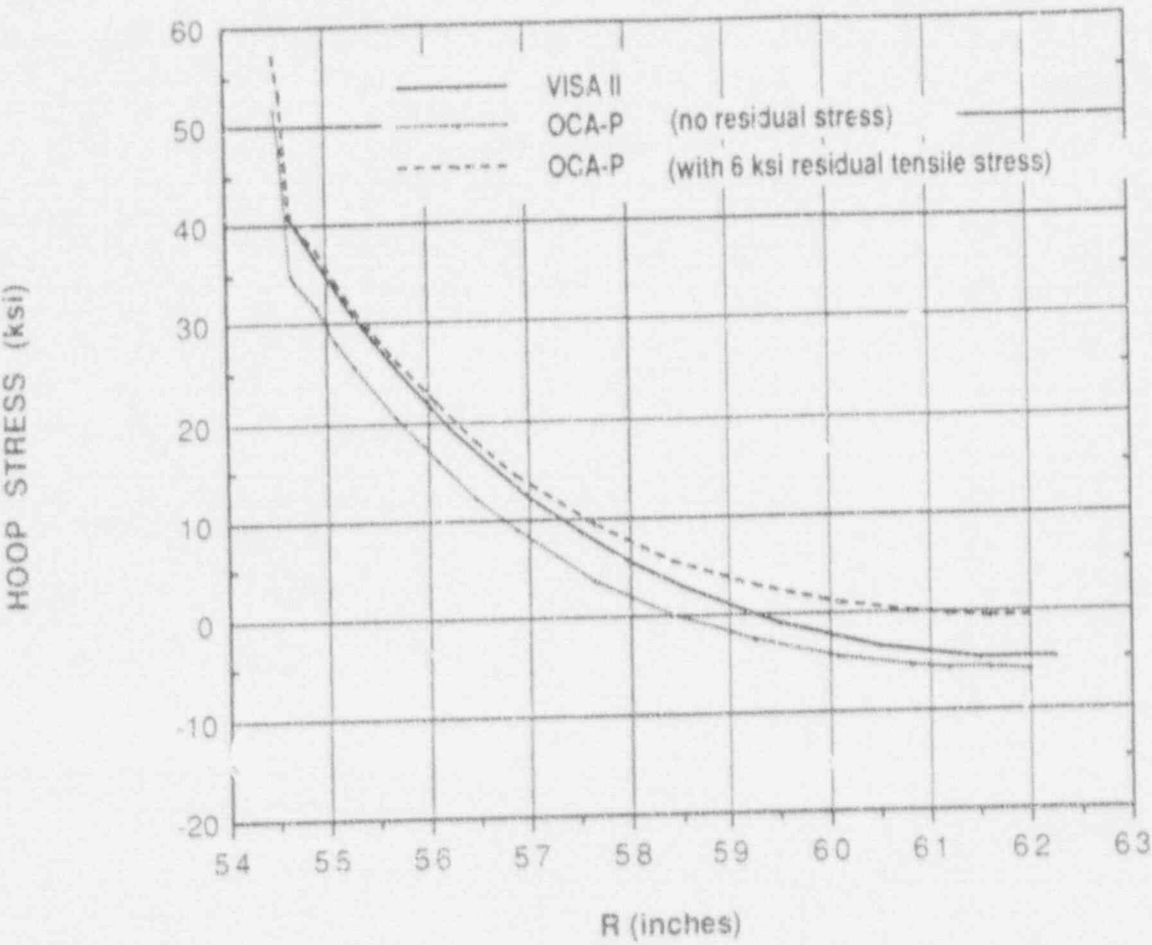

Figure D.7. VISA-II and OCA-P pedicted hoop stress distributions at time = 10 min. for Yankee Rowe SBLOCA7 PTS event (R/w ~7).

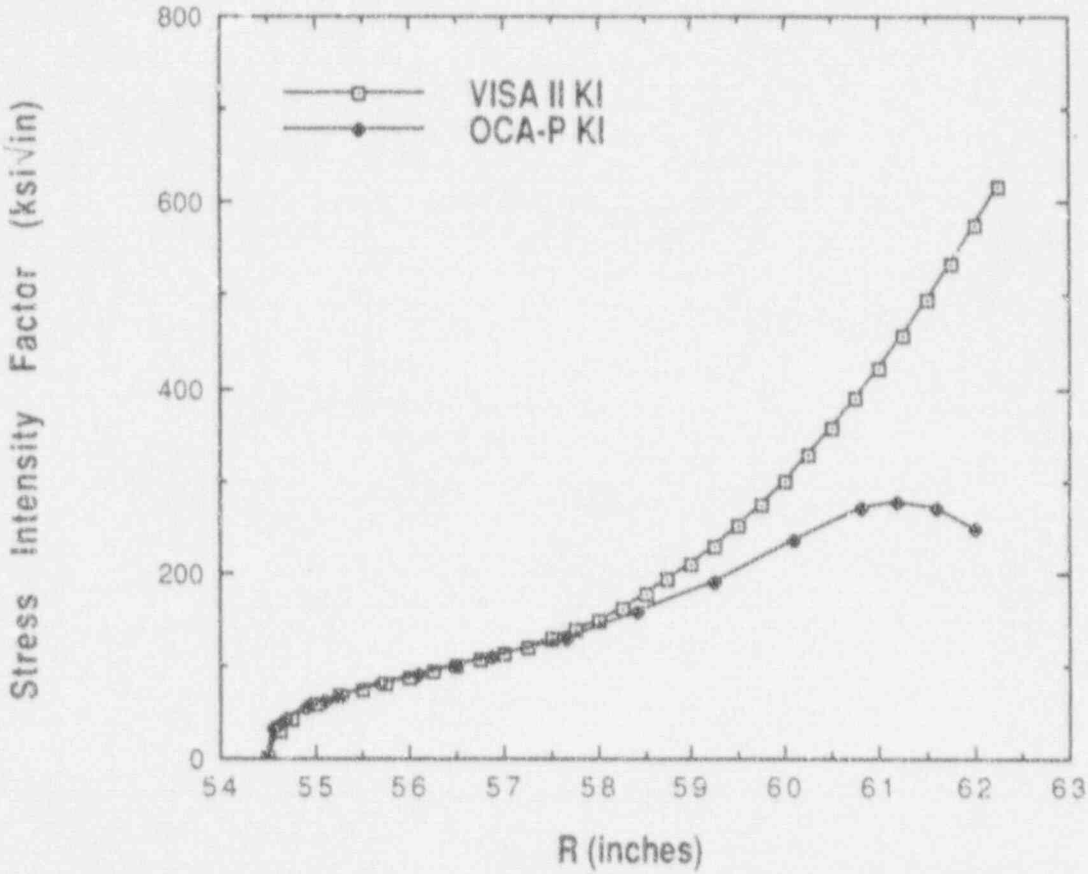

Figure D.8. VISA-II and OCA-P predicted stress intensity factors at time = 10 min. for Yankee Rowe SBLOCA7 PTS event (R/w ~7).

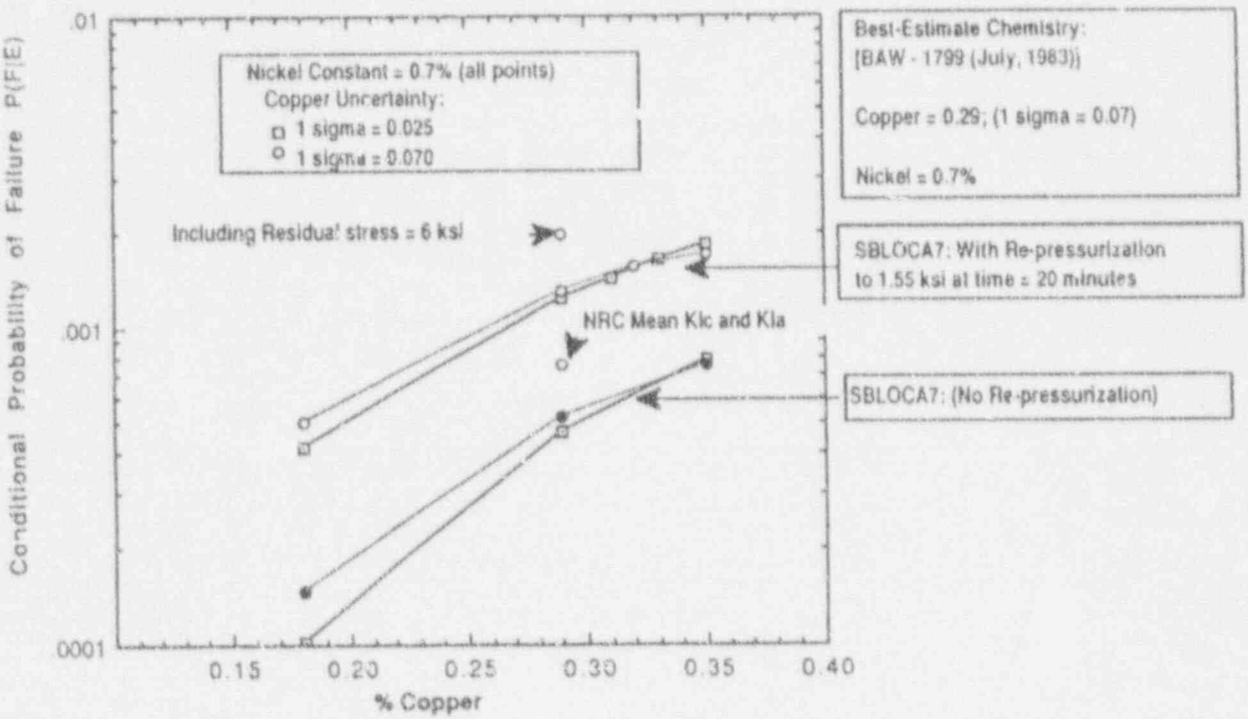

Figure D.9. Results of OCA-P probabilistic fracture mechanics analyses for Yankee Rowe upper axial weld subjected to PTS event SBLOCA7.

Appendix E

PNL Review of YAEC No. 1735

F. A. Simonen

Contents

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October 29, 1990

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Dear Dick:

REVIEW OF YANKEE ATOMIC PTS REPORT

This letter is my input for your review of the document "Reactor Pressure Vessel Evaluation Report for Yankee Nuclear Power Station", YAEC No. 1735, July 9, 1990. My comments cover the following areas as described in your letter dated August 16, 1990, to Mr. M. E. Mayfield at NRC:

1. Comparison of Yankee Rowe's (Ron Gamble's) version of VISA-II and the PNL version.

2. Check input to the fracture-mechanics analyzes.

3. Participation in the comparison of OCA-P and VISA-II.

4. Evaluation of the vessel inspection program.

My review of the Yankee report was performed from the standpoint of compliance with Regulatory Guide 1.154 "Format and Content of Plant Specific Pressurized Thermal Shock Safety Analysis Reports for Pressurized Water Reactors".

PNL CONCLUSIONS

Some details of the PNL and Yankee versions of the VISA-II code were found to be somewhat different. However, the two codes are expected to give similar predictions of vessel failure probabilities except with respect to residual stresses. The Yankee version takes a more conservative approach to residual stresses than required by Reg. Guide 1.154, and therefore was found to predict slightly higher values for vessel failure probabilities.

The input parameters for the Yankee calculations were reviewed item by item for consistency with Reg. Guide 1.154 and PNL's recommendations (NUREG/CR-4486) for application of VISA-II. While several details of the Yankee inputs differed from those used in prior NRC studies, sensitivity calculations indicate that these differences should not have a major impact on calculated failure probabilities. Inputs for pressures, temperatures and irradiation induced embrittlement do have very significant impacts on calculated failure probabilities, but these parameters were outside the scope of PNL's review.

PNL participated with ORNL in efforts to compare the VISA-II and OCA-P codes. The codes were found to give similar predictions, except in a detailed aspect of simulating the shift in RTndt for purposes of predicting the arrest of a growing crack. Both codes make reasonable assumptions for the arrest simulations, and give approximately the same numerical results if input parameters are assigned in a manner consistent with assumptions stated in the user's manual for the respective codes.

The chapter in the Yankee report on NDE plans was reviewed by PNL. It is noted that the Yankee report does not take credit for NDE as a factor in calculating vessel failure probabilities, and therefore Reg. Guide 1.154 does not call for discussion of inspection programs. Nevertheless, this chapter does provide interesting and useful information of preliminary plans by Yankee for future inspection of the reactor pressure vessel.

COMPARISON OF YANKEE ROWE AND PNL VERSIONS OF VISA-II

Section 5 of Reg. Guide 1.154 states that calculations should be performed with a probabilistic fracture mechanics code such as OCA-P or VISA-II. The Yankee Rowe evaluation was performed with a modified version of the VISA-II code, and therefore one part of PNL's review was to compare the Yankee Rowe version and PNL versions of VISA-II. PNL's objective was to assure that the modified code still complied with the requirements of Reg. Guide 1.154, and to assure that any code changes did not introduce unacceptable unconservatisms into calculated failure probabilities.

Basis for Comparison - Formal documentation of the Yankee Rowe version of the code was not available for PNL's review. Computer outputs from the Yankee Rowe version of the code did permit PNL to make some limited benchmark numerical comparisons. The review was based on 1) numerical results from an example output file and as cited in rather limited detail in the Yankee Rowe report, and 2) informal "word of mouth" reports of the types of changes that were made in the Yankee Rowe version of the code.

Numerical Comparisons - PNL was provided with a copy of the input data used in the Yankee Rowe calculations for the case "SBLOCA 7, LOWER PLATE, MAX MEAN DRTNDT=315". Calculations were performed with PNL's version of VISA-II for this set of data. There were no predicted failures for 500,000 simulated vessels, and this result agreed with the results presented in the Yankee Rowe report. However, for this case the Yankee report gave no more details or discussion of actual numerical results. Rather significantly, PNL's calculations gave nearly 1,000 flaw initiations for the 500,000 simulations, all of which became arrested cracks without any through wall penetrations. Such a trend is not mentioned in the Yankee Rowe report.

During October 1990, PNL received input and output files for a computer run made with the Novetech version of the code. Attachment #1 compares crack initiation and vessel failure probabilities for the Novetech and PNL versions of VISA-II. The Novetech version predicts high probabilities, with the higher probabilities attributed to the inclusion of residual stresses. Further comparisons of the two versions beyond the results of Attachment #1 could not be made, since PNL was not provided the deterministic output of the Novetech version. A full comparison would require that PNL have a copy of the Novetech code, so that more extensive benchmark calculations can be performed.

Differences in Codes - Prior to the Yankee Rowe review Dr. Fred Simonen of PNL (one of the code developers) and Mr. Ron Gamble of Novetech Corporation (a user of the code) had engaged in phone discussions of detailed aspects of the VISA-II code. These discussions had occurred on several occasions over a time period of a year or more. During a discussion on August 6, 1990, Mr. Gamble described a number of changes to VISA-II. It has since become known that Novetech performed the probabilistic fracture mechanics calculations on behalf of Yankee Rowe. A subsequent phone discussion between Simonen and Gamble at Novetech on October 18, 1990 further clarified aspects of Novetech's version of Visa-II.

Notes from the August 6th phone discussion indicate the following modifications:

1. Inclusion of a welding residual stress of 8 ksi tension at the inner vessel surface and becoming compressive at the mid wall of the vessel. The distribution was said to be consistent with data published by Paris.

2. The shift in RTndt was revised to exactly reproduce the numbers in the tables of Reg, Guide 1.99 Rev. 2. The final version of this guide was published after the VISA-II code was issued, and the numbers in the final version of 1.99 differ sightly from the numbers upon which VISA-II were based.

3. The crack tip stress intensity factor solution for internal pressure loading was replaced with a recent solution due to Dr. Zahoor of Novetech.

This list of changes is generally consistent with subsequent statements made by NRC staff during the course of this review effort.

Lacking full details of the Yankee Rowe version of VISA-II, we can only offer some qualified comments. In particular it will be assumed that all the modifications as described by Novetech were correctly implemented into VISA-II with no coding errors.

Residual Stresses - The inclusion of residual stresses was not stated as a requirement in Reg. Guide 1.154. During the development of VISA-II the possible presence of residual stresses was recognized. However, there is generally little information regarding the levels and distributions of such stresses for a given vessel, although levels of residual stresses in the welds of reactor vessels are believed to be small relative to the thermal stresses during PTS events. In the overall balance between conservative and unconservative assumptions a decision was made to neglect residual stresses in the VISA-II code. Inclusion of a modest level of residual stresses as in the Yankee Rowe calculations would increase the number of initiation events, but should contributed little to the noted tendency for the these initiated cracks to arrest before becoming through wall cracks.

The residual stresses were assumed to be approximated by a cosine function. PNL was referred to a solution in the "Tada Fracture Handbook" for details of the crack tip stress intensity factor solution by Novetech. This handbook gave a polynomial function for the solution, which differed from the trigonometric type of function described by Novetech. Nevertheless, the two functions give the same general trend for stress intensity factors. They agree for small ID surface flaws, and both predict small values of stress intensity factor for deep flaws that extend to the mid-wall of the vessel.

For the Yankee evaluation, we suggest that residual stresses be neglected because: 1) the calculations would then be fully consistent with Reg. Guide 1.154, and 2) this would avoid concerns that inclusion of residual stresses in the Yankee calculations contributed to the large number of predicted crack arrest events.

Reg. Guide 1.99 Rev. 2 Shift - The recoding of the RTndt shift equation was not considered to be an important consideration in the review of the Yankee Rowe calculations. PNL has found its calculated shift values to adequately reproduce numbers tabulated in Reg. Guide 1.19 Rev. 2. Nevertheless, further precision in the calculation as done for the Yankee Rowe evaluation is certainly an acceptable change to the code.

K-Solution for Pressure - The stress intensity factor solution for pressure loading in PNL's version of VISA-II has been checked for accuracy, and has been found to give acceptable results for the probabilistic fracture mechanics calculations. Nevertheless, further precision in this part of the calculation is certainly an acceptable code revision. It is understandable that Novetech would make use of equations from their own recent research.

CHECK OF FRACTURE MECHANICS INPUT

PNL was provided with computer files of the input data for certain of the calculations described in the Yankee Rowe report. Each item of this input for VISA-II was reviewed for consistency with the guidelines given in Reg. Guide 1.154 and in NUREG/4486 (the user document for VISA-II). The main focus of this review was the case titled "YANKEE, SBLOCA 7, LOWER PLATE, MAX MEAN DRTNDT-315".

Part of PNL's review consisted of performing calculations with PNL's version of VISA-II using the Yankee input. Some 38 variations of the baseline case "YANKEE, SBLOCA 7, LOWER PLATE, MAX MEAN DRTNDT-315" were evaluated to establish if Yankee's values of vessel failure probabilities were particularly sensitive to choices of input parameters. These calculations are listed in Attachment #2 along with the calculated values of crack initiation and vessel failure probabilities.

It should be noted that the levels of RTndt estimated for the Yankee vessel are quite high. For this reason we have concluded that predicted crack initiation and arrest events are to a large extent governed by the materials fracture toughness in the lower shelf regime. As such, the failure probabilities appear to be rather insensitive to parameters that govern the shift in RTndt (i.e. fluence, chemistry, errors in shift predictions, etc.). On the other hand, parameters that govern the applied level of stress intensity factor (pressure level, crack length, etc.) have more significant effects on failure probabilities.

Our comments on fracture mechanics inputs will compare the Yankee inputs to those that have been recommended and used by PNL for the VISA-II computer code (NUREG/CR-4486). It is assumed that ORNL will in a similar manner compare Yankee inputs with those used for OCA-P in the IPTS study.

Observations on Crack Arrest Behavior - A very striking trend was seen in the calculation for the baseline case (case 1 of Attachment #2). While the calculated failure probability (less than 1.0E-05) agreed with the result reported by Yankee, the output from VISA-II showed a rather high probability of crack initiation (1.05E-03). However, crack arrest was predicted to occur for all the initiated cracks, and hence no vessel failures were predicted. Reasons for this unusual trend were sought.

The 38 VISA-II calculations of Attachment #2 point to certain factors that contribute strongly to the very large number of arrest events:

1. The pressure during the critical parts of the small break LOCA transient for Yankee Rowe remains at a relatively low level of 670 psi. Evidently repressurization behavior as predicted in other PTS studies is not predicted to occur for the Yankee Rowe plant. Cases 3-5 of Attachment #2 show that an repressurization to only 1000 psi results in a noticeable increase in the calculated vessel failure probability. A substantial (but typical) repressurization to 2000 psi gives a relatively high failure probability of 7.99E-03. It is recommended that the Yankee accident scenarios be closely examined, to determine if possibilities for repressurization have been overlooked.

2. The Yankee calculations assume that lengthwise growth of flaws in the lower plate will not exceed the 47 inch dimension of this plate. Case 11 of Attachment #2 shows that cracks no longer tend to arrest, if the initiated flaws are permitted to grow to an essentially infinite length. Cases 16-18 address flaws of various lengths, and shows an progressive increase in failure probability as the initiated flaws are permitted to become longer. Case 16 (failure probability of 3.80E-05) is of particular interest, since the final length of the flaw is the height of the beltline region of the vessel.

3. The Yankee calculations assumed an upper shelf fracture toughness (for both initiation and arrest) equal to 200 ksi$\sqrt{in}$. Cases 12,13,30,31 and 32 address lower values of this upper shelf toughness. It is seen that the Yankee value of 200 must be reduced to 70 ksi$\sqrt{in}$ before the tendency for cracks to arrest is reduced. A toughness of such a low level is not considered to be a credible assumption.

4. Many other factors are addressed in Attachment #2, and none of these were found to reduce the strong trend for crack arrest. Some impact on initiation probabilities can be noted. However, we did not determine if any of these factors in combination could collectively give a substantial impact on crack arrest behavior.

Input for Clad Stress - The Yankee calculations neglected clad stresses as a factor that can promote crack growth. Evidently the clad effects were considered to be insignificant because the clad is relatively thin, and because mechanical interactions were minimal due to the "stitch" process used to bond the clad to the basemetal.

Information available to PNL indicated that the "stitch" attachment actually bonds clad to a rather significant fraction of the inner surface of the vessel. Also the weld areas are clad in a conventional manner with a weld deposit. Therefore interaction of clad with the basemetal is probably substantial. In Case #8 clad stresses were modeled as part of the VISA-II calculation. However, the results show little change in the calculated probability of crack initiation, and it is concluded that the Yankee calculations were reasonable in neglecting clad stresses.

Modeling of Fluence Gradient - The Yankee calculations made use of a feature in VISA-II that permits simulation of the spacial variations in neutron fluence over the inner surface of a vessel. Reg. Guide 1.154 would permit this approach, which accounts for the fact that the peak surface fluence may exist only over small fraction of the overall surface area of a given plate or weld.

In Case 6 of Attachment #2, the baseline calculation was performed but with the conservative assumption that the peak fluence existed over the entire surface of the lower plate. The resulting probability of crack initiation increased by a factor of about 10. This was about the expected change based on the fraction of the plate exposed to the peak levels of fluence.

Residual Stresses - The Yankee calculations included a contribution of welding residual stresses to the crack tip stress intensity factor. A cosine function approximated the distribution of residual stress through the vessel wall. There was a tensile stress of 8.0 ksi at both the inner and outer surfaces of the vessel, and a compressive stress at the mid wall location.

The tensile residual stress at the inner surface increases the probability of initiating flaws. It is estimated that the residual stress was roughly equivalent to an increased pressure of about 1.0 ksi. Case 4 of Attachment #2 suggests an increase in the crack initiation probability by a factor of about 3.0. Given crack initiation, the residual stress should have little effect on crack arrest events.

Input for Standard Deviation on Fluence - The Yankee calculations assume a standard deviation on fluence of 0.1 of the mean fluence value, as compared to the 0.3 value suggested in NUREG/CR-4486. The results for Case 14 indicates that 0.1 versus 0.3 has only a small effect on calculated failure probabilities (factor of about 10 percent).

It is believed that the Yankee Report bases a lower value of 0.1 on the fact that the fluence levels are rather well established for the Yankee vessel. However it should be noted that VISA-II uses the uncertainty in fluence levels in large measure to represent the uncertainty in predictions of the shift equation. In this regard, improved knowledge of fluences for the Yankee vessel is not relevant to the uncertainties in the fluence levels for the surveillance specimens which formed the basis of the RTndt shift correlation. Nevertheless, the Yankee calculations are perhaps consistent since they apply an error term to the shift equation as an alternative to the 0.3 sigma value for fluence uncertainty.

Input for Standard Deviation on RTndt - The Yankee calculations use inputs that differ somewhat from those suggested in the VISA-II user document (NUREG/CR-4486):

	Standard Deviation, degree F	
	Initial Value of RTndt	Shift
Yankee Calculations	10.0	17.0
NUREG/CR-4486	17.0 (for Plate)	0.0

Case 1 versus Case 33 of Attachment #2 compares calculated failure probabilities for the two approaches. The difference is only about 15 percent for initiation probability. This trend is consistent with our belief that failures are governed by fracture toughness on the lower shelf, and are therefore insensitive to calculated levels of shift.

Evidently the Yankee report follows certain recommendations of Reg. Guide 1.99 Rev. 2. These recommendations address the calculation of shift for purposes of deterministic fracture mechanics calculations, without regard to the probabilistic approach used in the VISA-II code. We note here that VISA-II add[illegible] variability in shift through the imposed uncertainties in [illegible], copper content, etc., and therefore adding an error term to the shift equation introduces excessive "noise" into the calculations. An input of zero error in the shift equation would be more consistent with the recommendations of NUREG/CR-4486.

<u>Standard Deviations on Copper and Nickel</u> - The Yankee inputs differ somewhat from the values suggested in NUREG/CR-4486 as follows:

	Standard Deviation, wht %	
	Copper	Nickel
Yankee Calculations	0.017	0.05
NUREG/CR-4486	0.025	0.00

Case 1 versus Case 34 of Attachment #2 compares calculated failure probabilities for the two approaches. The difference is only about 10 percent for initiation probability. This trend is again consistent with the fact that failures are governed by lower shelf fracture toughness, and are therefore insensitive to calculated levels of shift.

<u>Input for Upper Shelf Fracture Toughness</u> - The Yankee calculations assumed an upper shelf value of 200 ksi$\sqrt{in}$ for both the initiation and arrest toughnesses. These values are consistent with prior applications of the VISA-II code.

Cases 12,13,23-32 address the sensitivity of the calculated failure probabilities to decreases in upper shelf toughness values (as low as 50 ksi$\sqrt{in}$). Unless the toughness is decreased to a value less than 100 there is little impact on the failure probability. Only at a toughness of 70 ksi$\sqrt{in}$ (Case 31) did we predict that a significant fraction of the initiated cracks penetrate the vessel wall without arrest.

<u>Inputs for Deterministic Parameters</u> - The table below compares inputs used in the Yankee calculations with the corresponding inputs recommended in NUREG/CR-4486. There are no significant differences between the two sets of parameters.

	NUREG/CR-4486	Yankee
Thermal Diffusivity	0.982	0.983
Fluid/Vessel Film Coefficient	400	504
Constant for Fluence Attenuation	0.24	0.24
E alpha(1 - nu)	0.320	0.312
Warm Prestress	no	no
ISI	no	no
Flaw Length Before Initiation	infinite	6:1
Flaw Length After Initiation	infinite	length of weld
Location of Flaws	ID Surface	ID Surface

Input for Flaw Length Before Initiation - The Yankee input specifies that the flaw aspect ratio before initiation is 6:1, but also provides a tabular description of the (Marshall) flaw size distribution which gives an infinite length to the flaws. These are conflicting inputs. However, examination of the output of VISA-II shows that the later specification of infinite flaw length governed in the calculations.

Input for Flaw Length after Initiation - The Yankee calculations specifies that the flaw extends in length only to the entire height of the plate or weld of concern. Cases 16-18 show that failure probabilities will increase if the flaw is permitted to grow beyond the confines of the plate or weld. The assumption of finite flaw length is a possible unconservative feature of the Yankee calculations.

Input for Flaw Size Distribution - VISA-II uses the Octavia flaw size distribution as the default selection, but recognizes uncertainties in this aspect of the probabilistic model by suggesting that the Marshall distribution is also a suitable selection. The PTS screening limit was based in part on VISA calculations which used the Octavia distribution, whereas the subsequent IPTS calculations used the Marshall distribution.

The use of the Marshall distribution in the Yankee calculations is consistent with Reg. Guide 1.154. Case 9 of Attachment #2 shows that the Octavia distribution will give a similar but somewhat lower probability of crack initiation (by a factor of about 2) than the Marshall distribution.

Input for Flaw Density - The Yankee report follows the IPTS assumption of one flaw per cubic meter of vessel material to estimate a total of five flaws for the beltline region of the vessel. It then assumes that five flaws are approximately equivalent to assuming one flaw in each of the plates and welds of the vessel beltline. An implication of this assumption is that flaws are more likely (on a per unit volume basis) in welds than in basemetal. That is, the Yankee calculations imply that there is far more than one flaw per cubic meter of weld metal. We believe that this assumption is plausible, and does not conflict with Reg. Guide 1.154.

The documentation for the VISA-II code does not make specific reference to a given number of flaws per cubic meter. The original Octavia distribution is believed to have assumed a total of one flaw for the six axial welds in the beltline region of a reactor pressure vessel. For consistency, it would reasonable to assume there is also one flaw for the circumferential welds of the vessel beltline. The discussion of NUREG/CR-4486 suggests that flaws are less frequent in basemetal (i.e. flaws per unit volume of metal). Hence, a total of one or two flaws in the basemetal of the beltline is a logical extrapolation of the VISA-II approach. In conclusion, the VISA-II documents would suggest an assumption of some 3-4 flaws in the beltline region of the Yankee vessel.

In summary, the Yankee calculations assume somewhat more flaws than the prior studies referenced in Reg. Guide 1.154. Thus the Yankee predictions of vessel failure probabilities are conservative in this regard.

Polynomial Approximation of Transient - The VISA-II code approximates pressure and temperature transients with polynomials that are fit through five points of the transient. For the small break LOCA both the temperature and pressure have rapid changes during the first five minutes of the 100 minute transient. Therefore the polynomials give a relatively poor approximation during the critical early part of the transient.

For Case 15 (Attachment #2) the calculations focused only on the early part of the transient, and as a result the polynomial approximation was much improved for this more limited time period. This improved calculation gave a slightly lower probability of crack initiation (1.28E-03) than for the baseline calculation (1.85E-03). Thus this aspect of the Yankee calculations is somewhat conservative.

COMPARISON OF THE OCA-P AND VISA-II COMPUTER CODES

A major part of PNL's review consisted of interactions between Fred Simonen at PNL and Terry Dickson at ORNL in a cooperative effort to compare the OCA-P and VISA-II codes. PNL's latest version of VISA-II (i.e. the version that was placed in the Argonne code center) was sent to ORNL on August 1?th along with other data and documentation. Similarly ORNL sent a copy of the OCA-P code to PNL along with documentation. The VISA-II code was installed and extensively exercised at ORNL. Installation of OCA-P at PNL was found to be a more involved effort, which was beyond the scope of the short term review project

Both VISA-II and OCA-P are specifically mentioned in Reg. Guide 1.154 as examples of probabilistic fracture mechanics codes that are considered suitable for use in PTS evaluations. During the development of VISA-II there were some limited benchmark calculations that compared results from the codes (letter from F. A. Simonen of PNL to D. G. Ball at ORNL dated March 29,1984). The two codes were found to give generally the same results for vessel failure probabilities. Numerical differences in the 1984 study were attributed to different assumptions regarding flaw size distributions, simulation of fracture toughness, etc. Both codes have since undergone further development, and have been extensively modified. The benchmark comparisons as discussed here are for the latest versions of the code, and address a wider range of input variables than considered in the 1984 calculations.

Programming Errors - Some minor programming type errors in VISA-II were found and corrected as part of the benchmarking activity. These are described in Attachments 2 and 4 to this letter. The recommended corrections of Attachment #3 prevents the generation of an excessively long output summary table, which is some cases caused the calculations to abort due to a stack overflow condition. Attachment #4 addresses a concern (noted previously by some users of the code) where the tabulation of initiation and arrest events appeared to give inconsistent totals, with occasional cases where the calculated crack depth for an arrested crack was smaller than the initial depth of the crack (physically impossible). Our recent review shows that VISA-II was double counting the number of initiation events for certain unusual combinations of simulation parameters, and giving other associated inconsistencies in the output table.

Plastic Instability Calculation - To be more correct and to be consistent with OCA-P we have made a change in VISA-II to account for the pressure acting on the crack faces during the prediction of plastic instability. This change has little effect on calculated failure probabilities.

<u>Simulation of Shift</u> - After correcting the "minor" programming errors in VISA-II, there were still rather significant and unexplainable differences in calculated failure probabilities when comparing OCA-P and VISA-II. These particular numerical comparisons were for the Rancho Seco transient. Results of the comparisons are summarized in Attachment #5.

- Case #1 versus Case #2 indicates the apparent lack of agreement between OCA-P and VISA-II that was first noted by ORNL.

- Case #1 versus Case #3 indicates the very good agreement that was eventually achieved once a basic difference in the probabilistic assumptions and logic of the two codes was identified.

- Case #1 versus Case #4 indicates the rather reasonable agreement (within a factor of about 2) when OCA-P and VISA-II are each applied using assumptions and consistent inputs as recommended in their respective users manuals.

For Cases #1 and #2 the two codes actually agree quite well in their predictions of the probability of initiating a crack. However, VISA-II appears to predict a much greater trend for cracks to arrest, once they do initiate. After a careful look at each code it was determined that VISA-II resimulates the random error in RTndt for each small increment of crack depth during the simulation of crack growth and arrest events. In contrast OCA-P simulates the error in RTndt only once for each crack and uses this same error term for each advance of the crack as it predicts if the growing crack will arrest. Once VISA-II was reprogrammed to match the assumption used in OCA-P the very good agreement of Case #1 versus Case #3 resulted.

The crack arrest calculations of the VISA-II and OCA-P codes follow somewhat different philosophical approaches for simulating the variability of RTndt. We believe that one approach is not inherently more correct than the other. Both approaches appear to be reasonable. Furthermore the predicted failure probabilities from the two codes agree within a factor of about two. The Yankee Rowe calculations have been reviewed from the standpoint of simulating variability in shift. We found that Yankee's application of the VISA-II code and the selection of input parameters were not entirely consistent with recommendations given in the user document for the code.

However, sensitivity calculations showed that the Yankee selection of inputs had only a modest impact on predicted failure probabilities.

Temperatures and Thermal Stresses - Another part of the benchmark effort addressed the deterministic calculations of temperatures and thermal stresses. Solutions were compared for the Yankee Transient "SBLOCA 7, LOWER PLATE, MAX MEAN DRTNDT=315". ORNL generated results using the OCA-P and the ADINA general purpose finite element code. PNL generated solutions using VISA-II and the ANSYS general purpose finite element code.

Attachment #6 gives PNL's results that show rather good agreement between the temperature and stress calculations of the VISA-II and ANSYS codes. With the ANSYS code it was possible to model the cylindrical geometry of a vessel, and the calculations were performed for R/t = 6.916, 10 and 1000. VISA-II uses flat plate solutions to approximate the temperatures and thermal stresses in a vessel, and the numerical results of Attachment #6 are in fact identical for the cases of R/t = 6.916 and R/t = 100. The ANSYS results show a relatively small effect of R/t on the temperature and thermal stress solutions. As expected, the VISA-II flat plate solutions agree best with the ANSYS solutions for the largest value of R/t (=1000).

In conclusion, we have further validated the temperature and thermal stress calculations in the VISA-II code. For this purpose we used water temperatures for one of the critical transients from the Yankee Rowe report. We believe that VISA-II as applied in the Yankee Rowe evaluations provides an accurate method for calculating temperatures and thermal stresses.

EVALUATION OF YANKEE INSPECTION PROGRAM

In Chapter 7 (titled "Reactor Vessel Inspection") first briefly describes the fabrication history, preservice inspections, and inservice inspections to date. The chapter then concludes with a much longer discussion of plans for future inspections with particular attention to the beltline region of the vessel. The PNL review has addressed this Chapter of the report.

Section 8.3 of Reg. Guide 1.154 gives brief note to inservice inspection as an optional part of a plant specific analysis of PTS risk. Discussion of ISI is required in the PTS risk evaluation only if state-of-the-art nondestructive examinations (NDE) are used as a basis for decreasing any conservatism in the flaw density value used in the analyses. The Yankee vessel evaluation does not decrease any conservatisms using NDE as the basis, and in this context the discussion of Chapter 7 is not essential to the report and could be deleted. Nevertheless, this Chapter does provide useful information, and contributes to a more complete understanding of the status of activities by Yankee Atomic in the area of vessel integrity.

Inspection of the beltline of the Yankee vessel is made difficult by access problems, and by the partial bonding of the cladding to the basemetal. Yankee appears to be making a systematic effort to find solutions to these problems, and some of the proposed approaches are encouraging. It should be noted that these efforts are being driven by an NRC requirement that the beltline [illegible] Yankee vessel be inspected by 1993. It should be noted that this requirement is only indirectly related to concerns for pressurized thermal shock.

A PNL expert on NDE technology (Dr. S.R. Doctor) has reviewed the content of Chapter 7 of the Yankee Report. The information contained in Chapter 7 provides some very good background on fabrication history, and on the extent and type of both PSI and ISI that has been conducted on the Yankee Rowe vessel in the past. These inspections were conducted to the standards of the day, but in many cases does not provide the inspection effectiveness that is needed for the flaws of concern to the PTS issue. The chapter then discusses alternative means of access to both the inside and the outside surfaces of the vessel while listing the advantages and disadvantages of each access alternative. It would appear that the most likely possibility is to conduct the inspection from the inside. For the belt line region this will require a scanner that fits within a 2 inch annulus between the thermal shield and the vessel wall. Scanners will need to be developed to operate with this physical constraint. Finally, the chapter contains an overview of some preliminary research conducted to address the inspection of the stitch cladding using a combination of eddy current and ultrasonic methods. The work conducted on these techniques shows that the inspection is not a hopeless case. However, until techniques and procedures have been fully developed, it is not possible to comment on the effectiveness of the proposed inspection methods. Ultimately, blind testing of the proposed methods will needed to determine technique reliability, and then the measured inspection sensitivity will need to be compared to the sizes of flaws that are important to the PTS issue.

Please call me at 509-375-2087 if you have any questions or comments.

Sincerely,

Fred Simonen

F. A. Simonen
Theoretical and Applied Mechanics Group
Energy Sciences Department

/fas

cc: ME Mayfield - USNRC
SR Doctor

ATTACHMENT #1

PARAMETER	Case 1 VISA-II (Novetech)	Case 1 VISA-II (PNL)	Case 2 VISA-II (NOVETECH)	Case 2 VISA-II (PNL)
# initiations	56	349	164	669
# arrests	56	349	156	669
# stable arrests	6	299	114	665
# failures	50	50	50	4
probability initiation	.048	.0099	.051	.0067
probability failure	.043	.0012	.015	.00004

1. Total number of vessels simulated was 100,000 for all cases
2. Case 1 is for upper plate, max mean DRTNDT=248
3. Case 2 is for lower axial weld, max mean DRTNDT=288
4. PNL's version of VISA-II is the original VISA-II with only the corrections for counting of initiation events and consideration of crack depth for plastic instability analysis.

ATTACHMENT #2

VISA-II SENSITIVITY STUDIES

Baseline case lower plate - SBLOCA
Yankee input file LP-315.IN
YANKEE, SBLOCA 7, LOWER PLATE, MAX MEAN DRTNDT-315

CASE	VARIATION FROM BASELINE CASE	INITIATION PROBABILITY	FAILURE PROBABILITY
1	None (baseline)	1.85E-03	0.00
2	Pressure = const. = 670 psi	1.98E-03	0.00
3	Min. pressure = 1000 psi	3.22E-03	2.00E-05
4	Min. pressure = 1500 psi	4.96E-03	1.87E-03
5	Min. pressure = 2000 psi	1.04E-02	7.98E-03
6	All material at max. fluence	2.14E-02	0.00
7	flaw aspect ratio = 999.0	9.34E-03	0.00
8	Clad stress	1.25E-03	0.00
9	Octavia flaw distribution	0.72E-03	0.00
10	Threshold flaw size	1.92E-03	0.00
11	Infinite flaw length after initiation	1.83E-03	1.36E-3
12	Upper shelf toughness = 150	1.85E-03	0.00
13	Upper shelf toughness = 100	1.89E-03	0.00
14	Fluence sigma = 0.30	2.04E-03	0.00
15	Better polynomial fit of early transient	1.28E-03	0.00
16	Flaw length after initiation full height of beltline (122.6)	1.85E-03	3.8E-05
17	Flaw length after initiation = 90 inch	1.84E-03	2.0E-06

ATTACHMENT #2
(continued)

VISA-II SENSITIVITY STUDIES
Baseline case lower plate - SBLOCA
Yankee input file LP-315.IN
YANKEE, SBLOCA 7, LOWER PLATE, MAX MEAN DRTNDT-315

CASE	VARIATION FROM BASELINE CASE	INITIATION PROBABILITY	FAILURE PROBABILITY
18	Flaw length after initiation = 160 inch	1.90E-03	1.66E-04
19	Kia truncated at 99 standard deviations	1.94E-03	0.00
20	Kia truncated at 99 standard deviations	1.85E-03	0.00
21	Kic standard deviation 0.3 of mean	1.15E-02	0.00
22	Kia standard deviation 0.2 of mean	1.85E-03	0.00
23	Zero sigma on Kic & Kia Upper shelf toughness = 100	1.32E-04	0.00
24	Zero sigma on Kic & Kia Upper shelf toughness = 50	3.50E-02	3.50E-02
25	Zero sigma on Kic & Kia Upper shelf toughness = 75	1.34E-04	0.00
26	Zero sigma on Kic & Kia Upper shelf toughness = 65	2.17E-03	2.07E-03
27	Sigma on Kic = 0.0 Sigma on Kia = 0.1 of mean Upper shelf toughness = 75	1.34E-04	0.00

ATTACHMENT #2
(continued)

VISA-II SENSITIVITY STUDIES
Baseline case lower plate - SBLOCA
Yankee input file LP-315.IN
YANKEE, SBLOCA 7, LOWER PLATE, MAX MEAN DRTNDT-315

CASE	VARIATION FROM BASELINE CASE	INITIATION PROBABILITY	FAILURE PROBABILITY
28	Sigma on Kic = 0.15 of mean Sigma on Kia = 0.0 Upper shelf toughness = 75	4.13E-03	1.11E-04
29	Upper shelf Kic & Kia = 75	3.96E-03	6.85E-05
30	Upper shelf Kic & Kia = 65	8.84E-03	7.37E-03
31	Upper shelf Kic & Kia = 70	5.82E-03	1.19E-03
32	Upper shelf Kic & Kia = 80	2.77E-03	0.00
33	Sigma on Initial RTndt = 17 Sigma on shift = 0	2.13E-03	0.00
34	Sigma on copper = 0.025 Sigma on nickel = 0.0	1.76E-03	0.00
35	Rerun of case #1	1.97E-03	0.00
36	Sigma on Initial RTndt = 17 Sigma on shift = 0 Min pressure = 1000 psi	3.57E-03	4.00E-05
37	Sigma on Initial RTndt = 17 Sigma on shift = 0 Min pressure = 1500 psi	4.67E-03	3.74E-03
38	Sigma on Initial RTndt = 17 Sigma on shift = 0 Sigma on copper = 0.025 Sigma on nickel = 0.0 Sigma on fluence = 0.30 of mean	2.25E-03	0.00

ATTACHMENT #3
VISA-II
NOTICE OF CORRECTION

Date: September 17, 1990

Subject: Output of Summary Table

Problem: When a large number of initiations and arrests occur the program can terminate due to stack overflow. This is due to a coding error for the parameter list of the call to subroutine WRITEP.

Also, in certain cases the summary table giving examples of initiation and arrest events can greatly exceed the intended list of 50 examples. This is occurs when a large fraction of the flaw initiations arrest without vessel failure.

Correction: The logic has been changed to terminate the table when 50 initiations occur, rather than after 50 vessel failures. The following changes to the Fortran coding are recommended in the main program:

<u>Before</u>:

```
550 WRPSUM(ITOT) = WRPSUM(ITOT) + WRP(ITOT)
    IF(NF.LE.50) CALL WRITEP(NF,NNF,NI)


580 WRPSUM(ITOT) = WRPSUM(ITOT) + WRP(ITOT)
    IF(NF.LE.50) CALL WRITEP(NF,NNF,NI)
```

<u>After</u>:

```
550 WRPSUM(ITOT) = WRPSUM(ITOT) + WRP(ITOT)
    IF(NI.LE.50) CALL WRITEP


580 WRPSUM(ITOT) = WRPSUM(ITOT) + WRP(ITOT)
    IF(NI.LE.50) CALL WRITEP
```

Effect: All prior calculations should be correct. An output table may be longer than desired. Some calculations may abort before failure probability calculations are complete.

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PO Box 999, Richland, WA. 99352

ATTACHMENT #4
VISA-II
NOTICE OF CORRECTION

Date: September 17, 1990

Subject: Tabulation of Initiation and Arrest Events

Problem: The tabulation sometimes indicates arrested flaw depths that are less than the initial depth of the flaw. Also the table of summary statistics has inconsistencies in the numbers of arrests and arrested nonfailures.

Correction: The logic has been changed to reset flags, which cause certain counters to correctly tabulate summary statistics. The following changes to the Fortran coding are recommended in the main program:

<u>Before</u>:

```
C     SET FLAGS FOR FLAW INITIATION AND ARREST
      INITIA = 0
      IARRST = 0
C     RETURNS HERE TO SIMULATE NEXT FLAW
   80 CONTINUE
```

<u>After</u>:

```
C     RETURNS HERE TO SIMULATE NEXT FLAW
   80 CONTINUE
C     SET FLAGS FOR FLAW INITIATION AND ARREST
      INITIA = 0
      IARRST = 0
```

Effect: Prior calculated probabilities of failure should be correct. However, probabilities of flaw initiation may be slightly overestimated. Such errors are greatest when the specified number of flaws per vessel is much greater than one, and when large fractions of initiations arrest before vessel fracture occurs.

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ATTACHMENT #5

PARAMETER	Case 1 OCA-P	Case 2 VISA-II (original)	Case 3 VISA-II (modified)	Case 4 VISA-II (original)
# initiations	3926	3711	3422	2745
# arrests	983	3104	474	1747
# stable arrests	488	3275	144	1383
# failures	3438	436	3278	1362
probability initiation	.039	.037	.034	.027
probability failure	.034	.0043	.033	.014

1. Total number of vessels simulated was 100,000 for all cases

2. Case 2 is for original VISA-II which included error in counting of initiation events.

3. Case 3 used a revised and corrected VISA-II. The only revision was to simulate the error in RTndt only once per vessel, without repeated simulation of this error with each advance of the crack depth during the crack arrest calculations. Corrections included the error in counting of initiation events and consideration of crack depth for plastic instability calculation.

4. Case 4 used the original VISA-II with only the corrections for counting of initiation events and consideration of crack depth for plastic instability analysis. The repeated simulation of error in RTndt was retained from the original VISA-II. However, the error in shift in RTndt was set equal to zero in accordance with the recommendation and standard practices used in prior applications of VISA-II.

5. Cases 1-4 are based on a standard deviation of 24F in the shift in RTndt to estimate the error in this parameter. This practice has been customary for applications of OCA-P, but not for applications of VISA-II. The two codes make different assumptions to simulate the error in shift, and a standard deviation of zero (versus 24F for OCA-P) is appropriate to the assumptions made in the VISA-II code.

ATTACHMENT #6

ID WALL TEMPERATURES, °F

Time,	VISA-II		ANSYS		
min.	R/T=6.916	R/T=100	R/T=6.916	R/T=10	R/T=1000
10	357	357	364	364	363
20	260	260	267	267	266
30	214	214	219	219	218
40	197	197	201	200	200
50	192	192	195	195	194
60	188	188	190	190	189
70	179	179	181	181	180
80	166	166	167	167	167
90	153	153	155	154	154
100	154	154	155	155	155

ATTACHMENT #6 (Continued)

OD WALL TEMPERATURES, °F

Time,	VISA-II		ANSYS		
min.	R/T=6.916	R/T=100	R/T=6.916	R/T=10	R/T=1000
10	509	509	510	509	509
20	466	466	470	470	468
30	405	405	412	410	407
40	346	346	354	353	348
50	300	300	300	306	301
60	266	266	273	271	267
70	241	241	248	246	241
80	222	222	227	225	222
90	205	205	209	207	204
100	189	189	193	192	189

ATTACHMENT #6 (Continued)

ID HOOP THERMAL STRESS, ksi

Time,	VISA-II		ANSYS		
min.	R/T=6.916	R/T=100	R/T=6.916	R/T=10	R/T=1000
10	34.6	34.6	34.1	34.0	33.6
20	43.5	43.5	44.1	43.7	42.8
30	39.0	39.0	40.5	40.1	38.9
40	30.1	30.1	31.8	31.3	30.0
50	21.6	21.6	23.2	22.7	21.5
60	15.7	15.7	17.0	16.6	15.5
70	12.7	12.7	13.8	13.4	12.5
80	11.6	11.6	12.5	12.2	11.3
90	10.6	10.6	11.4	11.1	10.3
100	6.8	6.8	7.6	7.4	6.7

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10. SUPPLEMENTARY NOTES

11. ABSTRACT (200 words or less)

The Yankee Atomic Electric Company has performed an Integrated Pressurized Thermal Shock (IPTS)-type evaluation of the Yankee Rowe reactor pressure vessel in accordance with the PTS Rule (10 CFR 50.61) and U. S. Regulatory Guide 1.154. The Oak Ridge National Laboratory (ORNL) reviewed the YAEC document and performed an independent probabilistic fracture-mechanics analysis. The review included a comparison of the Pacific Northwest Laboratory (PNL) and the ORNL probabilistic fracture-mechanics codes (VISA-II and OCA-P, respectively). The review identified minor errors and one significant difference in philosophy. Also, the two codes have a few dissimilar peripheral features. Aside from these differences, VISA-II and OCA-P are very similar and with errors corrected and when adjusted for the difference in the treatment of fracture toughness distribution through the wall, yield essentially the same value of the conditional probability of failure. The ORNL independent evaluation indicated RT_{NDT} values considerably greater than those corresponding to the PTS-Rule screening criteria and a frequency of failure substantially greater than that corresponding to the "primary acceptance criterion" in U. S. Regulatory Guide 1.154. Time constraints, however, prevented as rigorous a treatment as the situation deserves. Thus, these results are very preliminary.

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