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A Parametric Study of PWR Pressure Vessel Integrity During Overcooling Accidents, Considering Both 2-D and 3-D Flaws

R. D. Cheverton   D. G. Ball

Prepared for the U.S. Nuclear Regulatory Commission
Office of Nuclear Regulatory Research
Under Interagency Agreements DOE 40-551-75 and 40-552-75

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A PARAMETRIC STUDY OF PWR PRESSURE VESSEL INTEGRITY DURING OVERCOOLING ACCIDENTS, CONSIDERING BOTH 2-D AND 3-D FLAWS

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FOREWORD

The work reported here was performed at Oak Ridge National Laboratory (ORNL) under the Heavy-Section Steel Technology (HSST) Program, C. E. Pugh, 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 Milton Vagins.

This report is designated HSST Program Technical Report 81. Prior reports in this series are listed below:

1. S. Yukawa, *Evaluation of Periodic Proof Testing and Warm Prestressing Procedures for Nuclear Reactor Vessels*, HSSTP-TR-1, General Electric Company, Schenectady, N. Y. (July 1, 1969).
2. L. W. Loechel, *The Effect of Testing Variables on the Transition Temperature in Steel*, MCR-69-189, Martin Marietta Corporation, Denver, Colo. (November 20, 1969).
3. P. N. Randall, *Gross Strain Measure of Fracture Toughness of Steels*, HSSTP-TR-3, TRW Systems Group, Redondo Beach, Calif. (November 1, 1969).
4. C. Visser, S. E. Gabrielse, and W. VanBuren, *A Two-Dimensional Elastic-Plastic Analysis of Fracture Test Specimens*, WCAP-7368, Westinghouse Electric Corporation, PWR Systems Division, Pittsburgh, Pa. (October 1969).
5. T. R. Mager and F. O. Thomas, *Evaluation by Linear Elastic Fracture Mechanics of Radiation Damage to Pressure Vessel Steels*, WCAP-7328 (Rev.), Westinghouse Electric Corporation, PWR Systems Division, Pittsburgh, Pa. (October 1969).
6. W. O. Shabbits, W. H. Pryle, and E. T. Wessel, *Heavy-Section Fracture Toughness Properties of A533 Grade B Class 1 Steel Plate and Submerged Arc Weldment*, WCAP-7414, Westinghouse Electric Corporation, PWR Systems Divison, Pittsburgh, Pa. (December 1969).
7. F. J. Loss, *Dynamic Tear Test Investigations of the Fracture Toughness of Thick-Section Steel*, NRL-7056, Naval Research Laboratory, Washington, D.C. (May 14, 1970).
8. P. B. Crosley and E. J. Ripling, *Crack Arrest Fracture Toughness of A533 Grade B Class 1 Pressure Vessel Steel*, HSSTP-TR-8, Materials Research Laboratory, Inc., Glenwood, Ill. (March 1970).
9. T. R. Mager, *Post-Irradiation Testing of 2T Compact Tension Specimens*, WCAP-7561, Westinghouse Electric Corporation, PWR Systems Division, Pittsburgh, Pa. (August 1970).
10. T. R. Mager, *Fracture Toughness Characterization Study of A533, Grade B, Class 1 Steel*, WCAP-7578, Westinghouse Electric Corporation, PWR Systems Division, Pittsburgh, Pa. (October 1970).

11. T. R. Mager, *Notch Preparation in Compact Tension Specimens*, WCAP-7579, Westinghouse Electric Corporation, PWR Systems Division, Pittsburgh, Pa. (November 1970).

12. N. Levy and P. V. Marcal, *Three-Dimensional Elastic-Plastic Stress and Strain Analysis for Fracture Mechanics, Phase I: Simple Flawed Specimens*, HSSTP-TR-12, Brown University, Providence, R.I. (December 1970).

13. W. O. Shabbits, *Dynamic Fracture Toughness Properties of Heavy Section A533 Grade B Class 1 Steel Plate*, WCAP-7623, Westinghouse Electric Corporation, PWR Systems Division, Pittsburgh, Pa. (December 1970).

14. P. N. Randall, *Gross Strain Crack Tolerance of A533-B Steel*, HSSTP-TR-14, TRW Systems Group, Redondo Beach, Calif. (May 1, 1971).

15. H. T. Corten and R. H. Sailors, *Relationship Between Material Fracture Toughness Using Fracture Mechanics and Transition Temperature Tests*, T&AM Report 346, University of Illinois, Urbana, Ill. (August 1, 1971).

16. T. R. Mager and V. J. McLoughlin, *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, Westinghouse Electric Corporation, PWR Systems Division, Pittsburgh, Pa. (October 1971).

17. N. Levy and P. V. Marcal, *Three-Dimensional Elastic-Plastic Stress and Strain Analysis for Fracture Mechanics, Phase II: Improved Modelling*, HSSTP-TR-17, Brown University, Providence, R.I. (November 1971).

18. S. C. Grigory, *Tests of 6-in.-Thick Flawed Tensile Specimens, First Technical Summary Report, Longitudinal Specimens Numbers 1 through 7*, HSSTP-TR-18, Southwest Research Institute, San Antonio, Tex. (June 1972).

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

20. S. C. Grigory, *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, Southwest Research Institute, San Antonio, Tex. (June 1972).

21. L. A. James and J. A. Williams, *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, Hanford Engineering De-velopment Laboratory, Richland, Wash. (September 1972).

22. S. C. Grigory, *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, Southwest Research Institute, San Antonio, Tex. (October 1972).

23. S. C. Grigory, *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, Southwest Research Institute, San Antonio, Tex. (June 1973).

24. S. P. Ying and S. C. Grigory, *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, Southwest Research Institute, San Antonio, Tex. (November 1972).

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

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

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

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

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

30. Canceled.

31. J. A. Williams, *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, Hanford Engineering Development Laboratory, Richland, Wash. (August 1973).

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

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

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

35. T. R. Mager, J. D. Landes, D. M. Moon, and V. J. McLaughlin, *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, Westinghouse Electric Corporation, Pittsburgh, Pa. (December 1973).

36. J. A. Williams, *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, Hanford Engineering Development Laboratory, Richland, Wash. (January 1975).

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

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

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

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

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

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

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

44. J. A. Williams, *The Ductile Fracture Toughness of Heavy Section Steel Plate*, NUREG/CR-0859, Hanford Engineering Development Laboratory, Richland, Wash. (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, *Test of 6-in.-Thick Pressure Vessels. Series 3: Intermediate Test Vessel V-7A Under Sustained Loading*, ORNL/NUREG-9, Oak Ridge Natl. Lab., Oak Ridge, Tenn. (February 1978).

46. R. D. Cheverton and S. E. Bolt, *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*, ORNL/NUREG-22, Oak Ridge Natl. Lab., Oak Ridge, Tenn. (December 1977).

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

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

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

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

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

52. R. D. Cheverton, S. K. Iskander, and S. E. Bolt, *Applicability of LEFM to the Analysis of PWR Vessels Under LOCA-ECC Thermal Shock Conditions*, NUREG/CR-0107 (ORNL/NUREG-40), Oak Ridge Natl. Lab., Oak Ridge, Tenn. (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, *Test of 6-in.-Thick Pressure Vessels, Series 3: Intermediate Test Vessel V-8*, NUREG/CR-0675 (ORNL/ NUREG-58), Oak Ridge Natl. Lab., Oak Ridge, Tenn. (December 1979).

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

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

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

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

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

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

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

61. A. Shukla, W. L. Fourney, and G. R. Irwin, *Study of Energy Loss and Its Mechanisms in Homalite 100 During Crack Propagation and Arrest*, NUREG/CR-2150 (ORNL/Sub-7778/1), University of Maryland, College Park, Md. (August 1981).

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

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

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

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

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

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

68. D. G. Ball, R. D. Cheverton, J. B. Drake, and S. K. Iskander, *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*, NUREG/CR-3491 (ORNL-5934), Oak Ridge Natl. Lab., Oak Ridge, Tenn. (February 1984).

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

70. 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), Oak Ridge Natl. Lab., Oak Ridge, Tenn. (May 1984).

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

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

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

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

75. C. W. Schwartz, R. Chona, W. L. Fourney, and G. R. Irwin, *SAMCR: A Two-Dimensional Dynamic Finite Element Code for the Stress Analysis of Moving CRacks*, NUREG/CR-3891 (ORNL/Sub/79-7778/3), University of Maryland, College Park, MD (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, *Effects of Stainless Steel Weld Overlay Cladding on the Structural Integrity of Flawed Steel Plates in Bending, Series 1*, NUREG/CR-4015 (ORNL/TM-9390), Oak Ridge Natl. Lab., Oak Ridge, Tennessee (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, *Pressurized-Thermal-Shock Test of 6-in.-Thick Pressure Vessels. PTSE-1: Investigation of Warm Prestressing and Upper-Shelf Arrest*, NUREG/CR-4106 (ORNL-6135), Oak Ridge National Laboratory, Oak Ridge, Tenn. (April 1985).

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

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

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

A PARAMETRIC STUDY OF PWR PRESSURE VESSEL INTEGRITY DURING OVERCOOLING ACCIDENTS, CONSIDERING BOTH 2-D AND 3-D FLAWS

R. D. Cheverton and D. G. Ball

ABSTRACT

A continuing analysis of the pressurized water reactor pressurized thermal-shock problem indicates that the previously accepted degree of conservatism in the fracture-mechanics model needs to be more closely evaluated and, if excessive, reduced. One feature that was believed to be conservative was the use of two-dimensional as opposed to finite-length flaws. The degree of conservatism could not be adequately investigated because of computational limitations and a lack of knowledge regarding flaw behavior; however, that situation has changed to the extent that some cases involving finite-length flaws can be studied. A flaw of particular interest is one that is located in an axial weld of a plate-type vessel. For those vessels that suffer relatively high radiation damage in the welds, the length of the flaw will be no greater than the length of the weld, and recent calculations indicate that a deep flaw of that length (~2 m) is not effectively infinitely long, contrary to previous thinking.

The benefit to be derived from consideration of the 2-m flaw and also a semielliptical flaw with a length-to-depth ratio of 6/1 was investigated by analyzing several postulated transients. In doing so the sensitivity of the benefit to a specified maximum crack-arrest toughness and to the duration of the transient was investigated. Results of the analysis indicate that for some conditions the benefit in using the 2-m flaw is substantial, but it decreases with increasing pressure, and above a certain pressure there may be no benefit, depending on the duration of the transient and the limit on crack-arrest toughness.

1. INTRODUCTION

A class of transients referred to as overcooling accidents (OCA's) may pose a threat to the integrity of the pressure vessel in a pressurized water reactor (PWR).[1-3] The potential problem involves the propagation of preexistent shallow crack-like defects during thermal-shock loading conditions associated with the postulated OCA's, and in an extreme situation the thermal loads, in combination with pressure loads, could conceivably result in a breach of the vessel. This threat tends to exist because the OCA-induced thermal shock results in high thermal

stresses and a reduction in fracture toughness. The fracture toughness is also reduced by radiation damage, and this introduces a time dependence of the severity of the problem; that is, the longer the vessel is in service the greater the potential for propagation of preexistent flaws.

Radiation damage in the vessel wall is enhanced by the presence of copper, an impurity, and nickel, an alloying element.[4-6] In most PWR vessels so-called high concentrations of copper are found only in the welds; thus, the regions of the vessel wall of greatest concern are the welds in the vicinity of the reactor core, where radiation damage is relatively high. As a result the length of a flaw tends to be limited by the length of a weld and also by gradients in the fast-neutron fluence along the length of a weld.

Fracture-mechanics models used for analyzing flaw behavior during OCA's have generally been restricted to two-dimensional (2-D) flaws: infinitely long axial flaws and continuous circumferential flaws for plate-type vessels, which have both axial and circumferential welds; and continuous circumferential flaws for vessels fabricated from ring forgings. In a plate-type vessel the axial welds tend to be of greatest concern because the stress intensity factor (K_I) for a long axial flaw can be substantially greater than for a circumferential flaw, and fluence gradients are generally less in the axial direction.

Long flaws have been analyzed because (1) they are amenable to accurate analysis; (2) the more probable finite-length flaws were considerably more difficult and expensive to analyze; (3) there were indications that under thermal-shock loading conditions short flaws would grow to become long flaws;[7] and (4) the consideration of long flaws appeared to be a conservative approach, yet not excessively so. However, a continuing study of postulated OCA's and a few OCA's that have actually occurred[1] indicate that the problem may be severe enough to warrant a closer examination of the degree of conservatism in the fracture-mechanics model; if it is excessive, an effort should be made to reduce it.

Fortunately, recent advances in three-dimensional fracture-mechanics-analysis techniques have made it practical to examine to some extent the behavior of finite-length flaws during postulated OCA's. This report discusses the possible benefits, in terms of vessel integrity, to be gained by considering the more probable finite-length flaws in the OCA studies. In addition, the sensitivity of this effect to a specified maximum crack-arrest toughness and to the duration of the transient is investigated.

2. THE TENDENCY FOR INNER-SURFACE FLAWS TO PROPAGATE DURING AN OCA

The tendency for inner-surface flaws to propagate as a result of thermal-shock loading is illustrated in Fig. 1*, which shows the temperature, resultant thermal stress, and fracture toughness distributions through the wall of the vessel (exclusive of cladding) at a particular time during a postulated large-break loss-of-coolant accident (LBLOCA). Also included in the figure for the same time in the transient are the stress intensity factors (K_I) for long axial flaws of different depths and the radial distribution of the fast neutron fluence. As indicated, the positive gradient in temperature and the steep attenuation of the fluence result in positive gradients in the crack initiation toughness (K_{Ic}) and the crack arrest toughness (K_{Ia}), and these positive gradients tend to limit crack propagation. However, K_I for the assumed long axial flaw also increases with flaw depth, except near the back surface, and for the particular case and time analyzed it is evident that both shallow and deep flaws can initiate; that is, $K_I > K_{Ic}$ for a broad range of crack depths. As the crack tip moves through the wall it encounters

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*In Fig. 1 and throughout this report, a = crack depth or radial position in wall, w = wall thickness.

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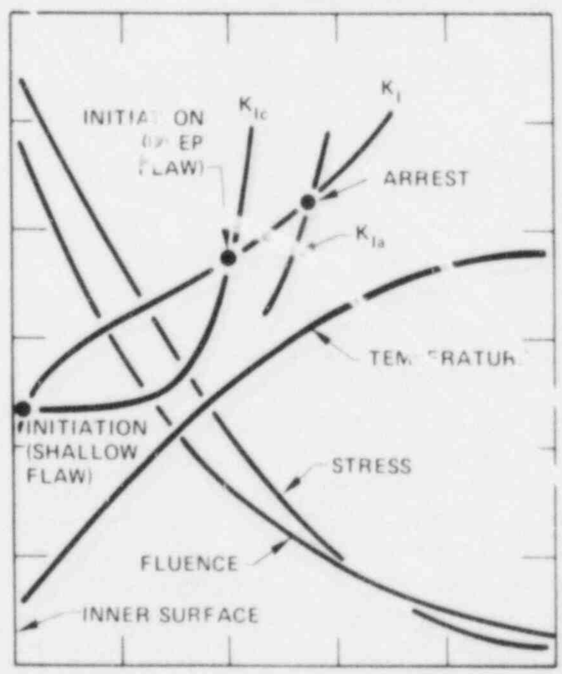

Fig. 1. Radial distributions in a vessel wall of several fracture-mechanics-related parameters at a specific time during a PWR LBLOCA.

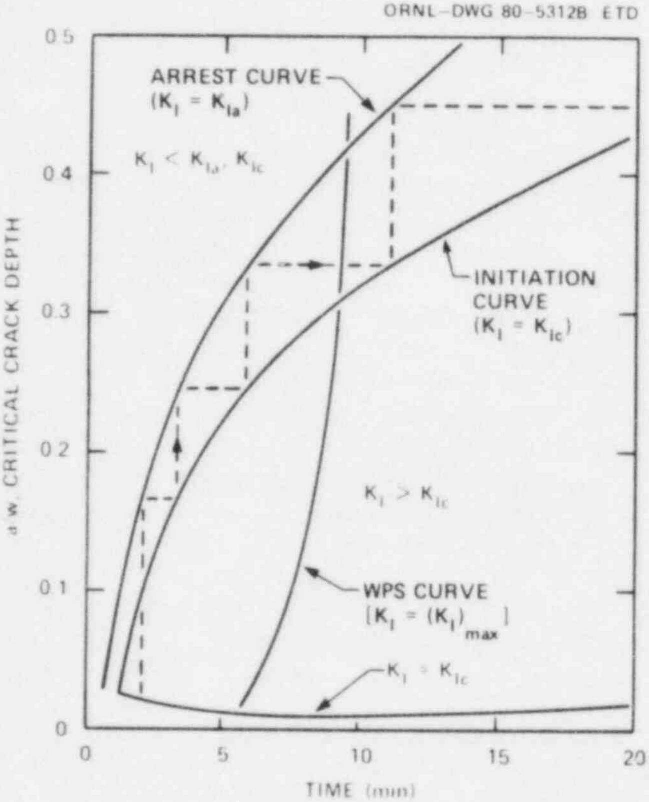

Fig. 2. Critical-crack-depth curves for a PWR LBLOCA assuming a long axial flaw, high concentrations of copper and nickel, and normal end-of-life fluence.

higher-toughness material and for this particular case eventually arrests.

If the crack depths corresponding to the initiation and arrest events are plotted as a function of the times in the transient at which the events take place, a set of curves is obtained that indicates the behavior of the flaw during the entire transient. A typical set of critical-crack-depth curves for a LBLOCA is shown in Fig. 2. As indicated by the dashed lines, the long axial flaw would propagate in a series of initiation-arrest events and, if a phenomenon referred to as warm prestressing[8] (WPS) were not effective, would penetrate deep into the wall.

The LBLOCA represents an extreme OCA in the sense that the thermal transient is very severe, while the pressure is essentially zero. For a more typical postulated OCA the pressure is substantial. As a result the slope of the K_I curve is steeper, particularly for the deeper flaws, and this increases the possibility of deep penetration of the flaw. Furthermore, repressurization during an OCA, following a reduction in pressure, may negate the effects of WPS. For this reason there is some hesitancy at this time to take advantage of WPS in a safety analysis, and it is not considered further in this paper.

If the flaw propagates deep into the wall of the vessel, it is possible that the vessel will fail due to plastic instability in the remaining ligament. Presumably such a failure is not possible under thermal-loading conditions only;[8] however, if the pressure is substantial, plastic instability must be considered.

3. FRACTURE-MECHANICS MODEL

Linear elastic fracture mechanics (LEFM) was used to analyze the behavior of the flaws up to a point where one of two specified failure criterion were assumed to be controlling. One of these failure criterion is based on the assumption that a fast-running crack will not arrest if K_I exceeds a value corresponding to the upper shelf crack arrest toughness, as illustrated in Fig. 3, and the other involves the inability of a crack to arrest after development of plastic instability in the remaining ligament.

The largest measured value of dynamic fracture toughness included in the ASME Code (Sect. XI)[9] for PWR pressure vessel material is 220 MPa $\sqrt{m}$, while more recent data[10] indicate that appropriate K_{Ia} values could be as high as 330 MPa $\sqrt{m}$. Previous studies[11] demonstrated that for some postulated OCA's this range of $(K_{Ia})_{max}$ values could make a substantial

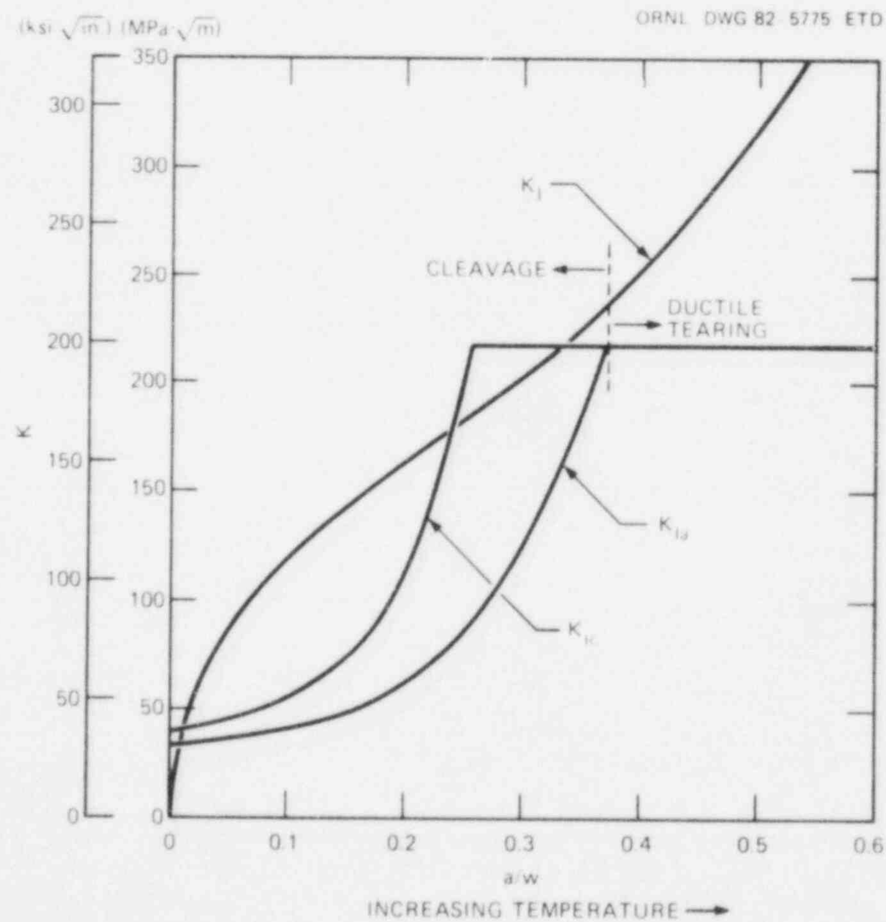

Fig. 3. Plots of K_I, K_{Ic}, and K_{Ia} vs crack depth at a specific time in an OCA transient, indicating crack initiation but no arrest unless on the upper shelf.

difference in the calculated behavior of a flaw; thus, both values were included in this study.

With regard to plastic instability, a critical crack depth corresponding to this failure condition was specified for each pressure, assuming no three-dimensional or thermal-stress effects, a uniform pressure stress in the ligament, and a failure stress of 550 MPa.

The reactor pressure vessel included in the analysis was typical of existing PWR's except that the thin layer of stainless steel cladding on the inner surface was excluded. Recent studies[12] indicated that complete exclusion of the cladding (no thermal or stress effects) resulted in essentially the same potential for vessel failure as estimated by including the cladding as a discrete region; thus, omission of the cladding appeared justified for the purpose of this study.

The flaws analyzed were oriented in either an axial or circumferential direction; they extended to the inner surface of the vessel; and with regard to shape and size there were three types: infinitely long flaws (2-D flaws), semielliptical flaws with a length-to-depth ratio of 6 (6/1 flaw), and semielliptical flaws with a length of ~2 m (2-m flaw). Appropriate combinations of these flaws (see Table 1) were selected for the purpose of evaluating the effect of flaw type on vessel integrity.

Table 1. Combinations of flaws used to determine effect of flaw type on vessel integrity

Flaw combination	Flaw events	Flaw orientation
2-D, 2-D	2-D for initial initiation 2-D for all subsequent events	Axial and circumferential
2-D, 2-m	2-D for initial initiation 2-m for all subsequent events	Axial
6/1, 2-m	6/1 for initial initiation 2-m for all subsequent events	Axial
6/1, 2-D	6/1 for initial initiation 2-D for all subsequent events	Circumferential

For both the 6/1 and 2-m flaws, radial propagation of the flaw, in terms of both initiation and arrest, was assumed to be governed by the K_I, K_{Ic}, and K_{Ia} values at the deepest point (midlength) of the flaw. The rationale for making this assumption and for considering the particular flaw geometries and combinations is discussed in the next section.

Stress intensity factors for both the 2-D and semielliptical flaws were calculated using superposition techniques, and this required the

availability of appropriate influence coefficients. Several investigators[13-15] have calculated coefficients for semielliptical surface flaws in cylinders; however, not all of the coefficients necessary for the range of flaw sizes considered in this study were available. Rather than attempt to extrapolate the existing data, influence coefficients were calculated specifically for the flaws of interest here. Their derivation and the application of the superposition technique are discussed in Refs. 16 and 17.

Fracture toughness data (K_{Ic} and K_{Ia} vs T — RTNDT, where T is the temperature and RTNDT is the reference nil-ductility temperature) were taken from ASME Sect. XI,[9] and the reduction in toughness due to radiation damage was estimated using Eq. (1), which was recently proposed by Randall[18] as a revision to Reg. Guide 1.99, Rev. 1.[19]

$$\Delta RTNDT = 0.56\ (-10 + 470\ Cu + 350\ Cu\ Ni)(F \times 10^{-19})^{0.27}\ , \qquad (1)$$

where

$2 \times 10^{17} \leq F \leq 6 \times 10^{19}$ neutrons/cm^2,
F = fast neutron fluence (E > 1 MeV) at tip of flaw,
ΔRTNDT = increase in RTNDT at tip of flaw due to fast neutron exposure, °C,
RTNDT = $RTNDT_o$ + ΔRTNDT,
$RTNDT_o$ = initial (zero fluence) value of RTNDT,
Cu, Ni = copper and nickel concentrations, wt %.

A typical attenuation of the fluence through the wall of the vessel that includes a correction for the effect of the change in neutron spectrum through the wall on radiation damage was also recently proposed by Randall[18] and is being used in the ORNL studies. The relation is

$$F = F_o\ e^{-0.0094a}\ , \qquad (2)$$

where

F = fast neutron fluence at tip of flaw,
F_o = fast neutron fluence at inner surface of vessel,
a = depth of flaw, mm.

For the purpose of comparing the calculated effects of the choice of flaw combination and $(K_{Ia})_{max}$ on vessel integrity, threshold or critical values of RTNDT corresponding to incipient initiation of a flaw and incipient failure of the vessel (extension of the flaw through the wall) were calculated. For convenience the particular values of RTNDT that are compared with each other are the values corresponding to the inner surface of the vessel wall; they are referred to herein as $(RTNDT_s)_{ci}$ for incipient initiation and $(RTNDT_s)_{cf}$ for incipient failure.

The critical value of RTNDT is the minimum value, with respect to both time in the transient and crack depth, that results in $K_I = K_{Ic}$ and/or crack penetration of the wall (no arrest). Since K_{Ic}, K_{Ia} = f (T, $RTNDT_o$, ΔRTNDT) only,[9] where T is the temperature at the crack tip,

it is only necessary to determine these three parameters and K_I to perform the analysis. Values of ΔRTNDT were calculated from Eq. (3), which was obtained by combining Eqs. (1) and (2).

$$\Delta RTNDT = \Delta RTNDT_s \, e^{-0.00254a} \; . \tag{3}$$

The complete analysis for obtaining $(RTNDT_s)_c$ was performed with the computer code OCA-II,[20] which accepts as input the downcomer-coolant-temperature and primary-system-pressure transients and automatically searches for $(\Delta RTNDT_s)_c$.

For some OCA's, $(\Delta RTNDT_s)_{ci}$ corresponds to incipient initiation followed by crack arrest and no reinitiation, as shown in Fig. 4 (assuming WPS to be ineffective). However, increasing $\Delta RTNDT_s$ will eventually result in failure (no arrest), and the corresponding minimum value is $(\Delta RTNDT_s)_{cf}$. For other OCA's, $(\Delta RTNDT_s)_{ci} = (\Delta RTNDT_s)_{cf}$ because, as shown in Fig. 5, there is no arrest following initiation of a shallow flaw. This latter situation tends to be typical of high-pressure transients and the former of low-pressure transients.

The sets of critical-crack-depth curves in Figs. 4 and 5 include the locus of points for constant values of K_I. This allows one to determine if arrest takes place at or below the specified maximum value for K_{Ia} (220 or 330 MPa $\sqrt{m}$ for these studies); in Fig. 4 it does and in Fig. 5 it does not. [The initiation and arrest curves in Figs. 4 and 5

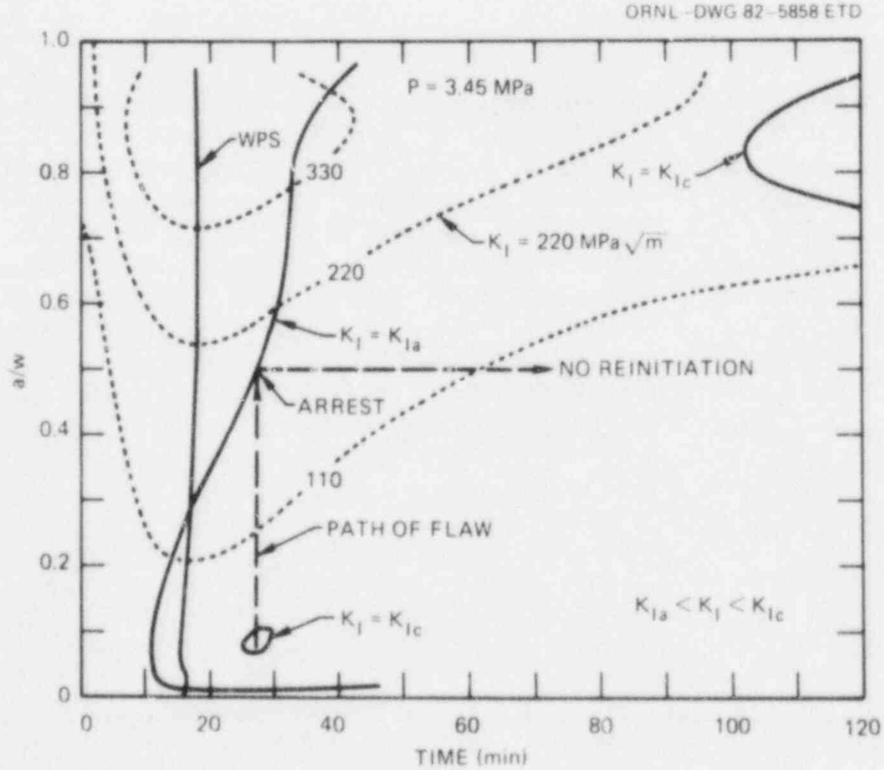

Fig. 4. Critical-crack-depth curves for an OCA illustrating incipient initiation followed by arrest and no initiation.

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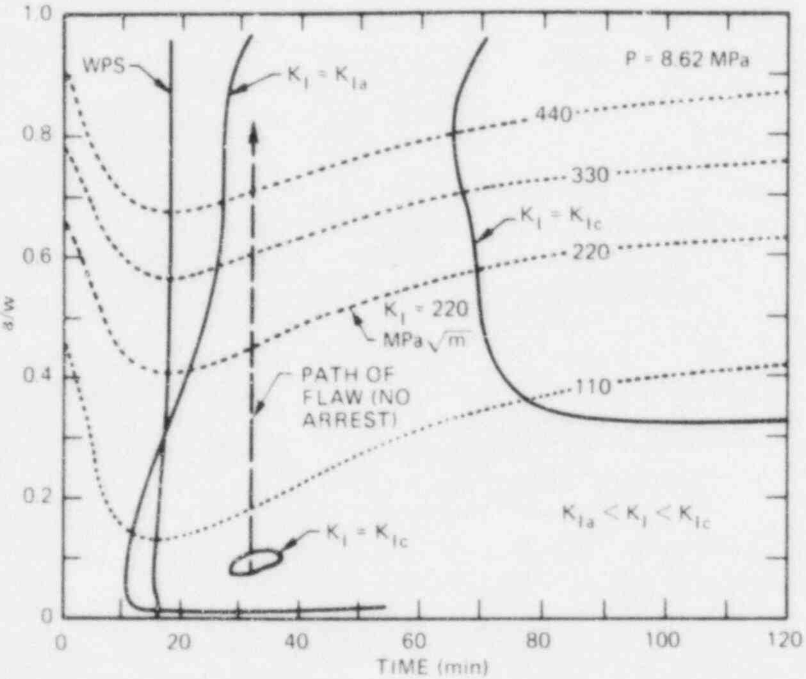

Fig. 5. Critical-crack-depth curves for an OCA illustrating incipient initiation and failure (no arrest unless on the upper shelf).

were extended beyond points corresponding to existing maximum values for K_{Ic} and K_{Ia} (~200 MPa $\sqrt{m}$) using the K_{Ic} and K_{Ia} equations in Ref. 9 for extrapolation purposes; thus, the extensions of the initiation and arrest curves beyond these points are fictitious to some extent but nevertheless allow one to apply different upper-shelf toughness values when using the critical-crack-depth curves to evaluate flaw behavior.]

The existence of two initiation loops (locus of points for $K_I = K_{Ic}$) in Figs. 4 and 5 suggests that for the purpose of calculating $(\Delta RTNDT_s)_{ci}$ a reasonable range of depths for initial flaws should be specified. Based on statistical analyses[21] of actual flaw depths, the maximum critical depth for the initial flaw was limited to ~30 mm (a/w = 0.15).

4. RATIONALE FOR SELECTION OF FLAW TYPES

As mentioned in the introduction, the flaws that tend to have the greatest potential for deep penetration are those in the axial welds of a plate-type vessel. As shown in Fig. 6, the axial welds of adjacent shell courses are staggered so that the length of a continuous axial weld region is no greater than the height of a shell course. If the concentration of copper in the weld is high compared to that in the base material, it is not likely that crack propagation will take place outside of the weld region. Thus, the length of an axial flaw would be no greater than the height of a shell course, which in some reactor vessels is ~2 m.

Shallow flaws with a length of 2 m are effectively infinitely long. However, recent calculations[21] indicate, as illustrated in Fig. 7, that rather deep, semielliptical, 2-m-long flaws ($a/w > 0.4$) have maximum K_I values, during a typical postulated OCA, that are substantially less than those for infinitely long flaws of the same depth. This indicates

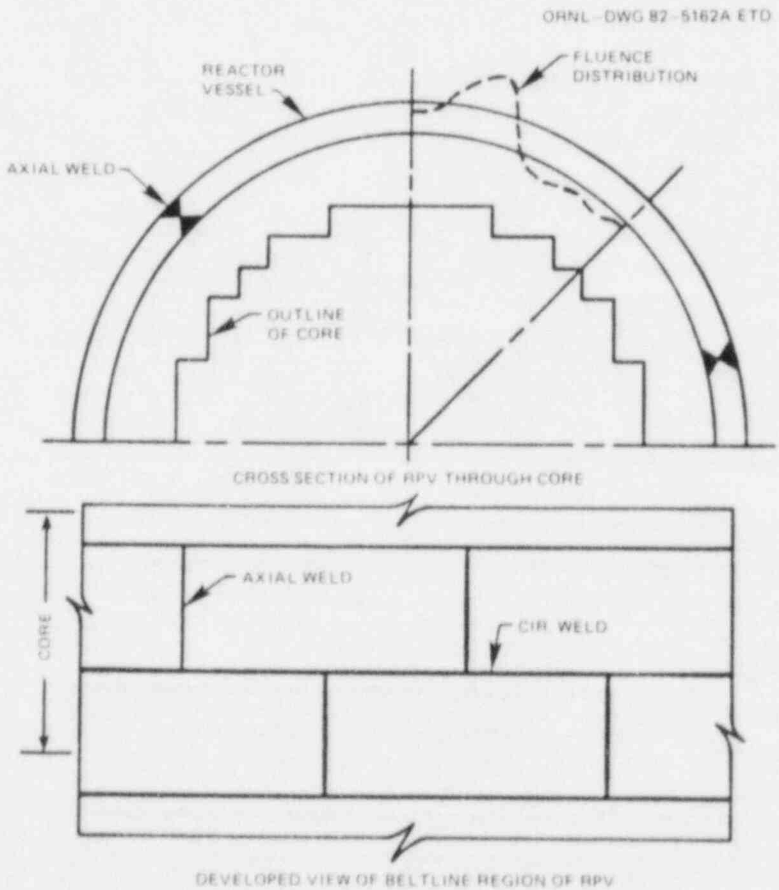

Fig. 6. Partial schematic diagram of PWR vessel showing typical azimuthal variation in fluence and weld configuration in belt-line region of plate-type vessel.

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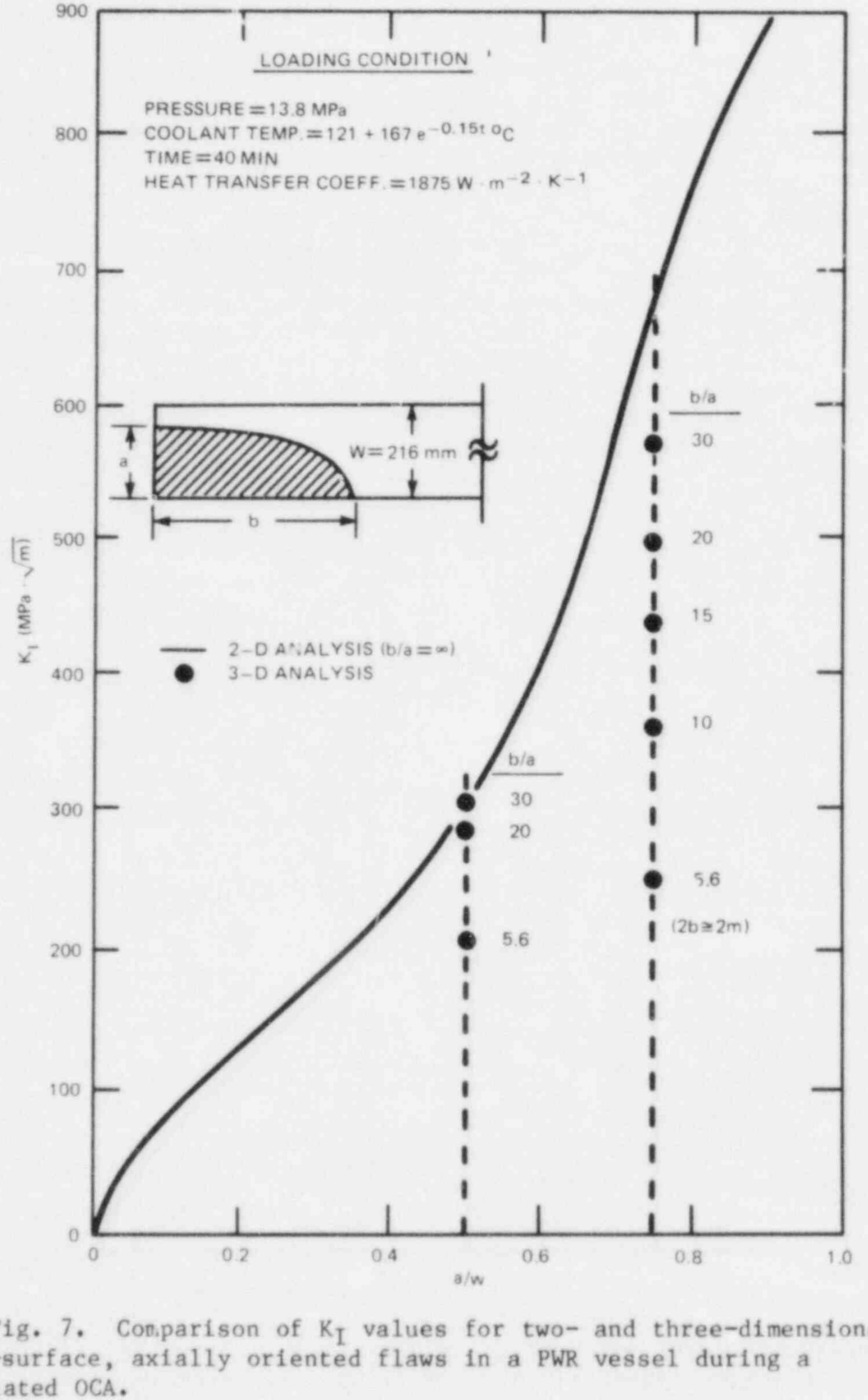

Fig. 7. Comparison of K_I values for two- and three-dimensional inner-surface, axially oriented flaws in a PWR vessel during a postulated OCA.

a greater tendency for arrest of the 2-m flaw, and since the analytical capabilities are now available for including such flaws in the OCA analysis, axially oriented semielliptical flaws with a length of ~2 m were included in OCA-II.

Initial flaws presumably would be much shorter than 2 m, and under thermal-shock loading conditions the shorter a flaw of a given depth the greater the potential (within limits) for crack initiation. However, rather than growing deep, the short flaw tends to grow in length and, barring dynamic effects, will not grow radially until a fluence greater than that necessary for incipient initiation of a 2-D flaw is achieved. Thus, barring dynamic effects, selection of a 2-D flaw for the first initiation event would tend to be conservative, but the degree of conservatism would depend upon the actual or most-probable length of the initial flaw.

Once the short flaw initiates, propagation tends to be governed by dynamic rather than static toughness. Conceivably this could reduce the critical fluence for radial propagation below that for a 2-D flaw, in which case the use of a 2-D flaw in the model for the first initiation event would not be conservative. This tendency increases with increasing length of the initial flaw, but with increasing length the difference in critical fluence between the finite-length and 2-D flaws decreases.

The problem associated with the finite-length initial flaw is quite complex, involving a changing crack shape and details of the transient. At this time there does not appear to be adequate technical justification for selecting any particular shape and size of initial flaw other than an effectively infinitely long (2-D) flaw for the initial initiation event, and thus a 2-D flaw was included in these studies for the initial flaw. However, since other investigators have included the 6/1 flaw in their OCA studies, the 6/1 flaw was also included in this study as an initial flaw.

For both the 2-D and 6/1 initial flaws it was assumed that once the crack initiated it immediately became a 2-m-long semielliptical flaw, and the first arrest and subsequent events for axially oriented flaws were calculated using the 2-m flaw.

Radial propagation of the 6/1 and 2-m flaws was assumed to be governed by the K_I, K_{Ic} and K_{Ia} values at the deepest point (midlength) of the flaw. This is a reasonable assumption for the 2-m flaw since it cannot grow in length. However, the assumption tends to result in an underestimation of the potential for radial propagation of the 6/1 flaw because as the flaw grows in length K_I/K_{Ic} at the deepest point increases, approaching that for a 2-D flaw. A reasonable alternative would be to use the maximum value of K_I/K_{Ic} on the crack front, provided that it was less than that for a 2-D flaw; otherwise, use the 2-D value. However, because of the added computational complexity this alternative was not included for the study discussed herein.

Arguments can also be made for imposing limits on the length of circumferential flaws, not because of restrictions on the length of the welds but because of an azimuthal variation in the fluence, as illustrated in Fig. 6. Since this makes the problem very plant specific, no attempt was made to limit the length of the circumferential flaw in accordance with the azimuthal variation in fluence. Instead, the study

included both 2-D and 6/1 flaws for the initial initiation and only 2-D flaws for the first arrest and subsequent events for the circumferential direction.

A complete evaluation of the circumferential flaw was not conducted for this study; rather, the circumferential flaw was included only to the extent of determining whether it might have a lower value of $(RTNDT_s)_{cf}$ than the (6/1, 2-m) axial flaw combination. Of course, a determination of which flaw is actually limiting depends not only on relative values of $(RTNDT_s)_{cf}$ but also on relative values of the fluence and the concentrations of copper and nickel. Once again this makes the problem plant specific and thus beyond the scope of this report.

5. ANALYSIS OF SEVERAL OCA'S

The results of a previous study[11] that considered only 2-D flaws indicated that changing from 2-D to 2-m flaws for crack arrest would not necessarily result in an increase in $(RTNDT_s)_{cf}$ for all postulated OCA's. As mentioned earlier, for primary-system pressures above some critical value p_c, $(RTNDT_s)_{cf} = (RTNDT_s)_{ci}$, a condition depicted in Fig. 5. For pressures less than p_c, a higher value of $RTNDT_s$ is required for failure than for incipient initiation; that is, the crack will arrest following incipient initiation and will not reinitiate, as shown in Fig. 4. For this case it was apparent that replacing the 2-D axial flaw with a 2-m flaw (for crack arrest) would increase p_c, and that only for pressures less than p_c would the change in flaw type result in an increase in $(RTNDT_s)_{cf}$. A point of interest was whether p_c would be high enough relative to expected pressures during OCA's for the change in flaw type (2-D to 2-m) to have a significant effect on vessel integrity.

A quantitative evaluation of the effect of the different assumed flaw types on vessel integrity was obtained for the present study by comparing values of $(RTNDT_s)_{cf}$ for several postulated OCA's and for the Rancho Seco transient that occurred in 1978 (see Fig. 8). The postulated transients consisted of a constant primary-system pressure, p, and a temperature transient defined by

$$T_c = T_f + (T_i - T_f)\, e^{-nt} , \qquad (4)$$

where

T_c = downcomer coolant temperature,
T_i = initial temperature of vessel wall and coolant,
T_f = final (asymptotic) temperature of coolant,
n = decay constant,
t = time in transient.

The duration of the transient, t_{max}, was varied to determine the sensitivity of vessel integrity to this parameter. Input data for the various cases calculated are summarized in Table 2.

Calculated values of $(RTNDT_s)_{cf}$ for the (2-D, 2-D) axial flaw combination are presented in Fig. 9, and the increases resulting from replacement of the (2-D, 2-D) combination with the (2-D, 2-m) and (6/1, 2-m) combinations are illustrated in Figs. 10 and 11 for T_f = 66 and 121°C, respectively. As indicated by Figs. 10 and 11, there can be an advantage associated with the 2-m flaw, and as expected it decreases with increasing pressure. The critical pressure, p_c, (the maximum pressure for which the 2-m flaw has an advantage over the 2-D flaw) ranges from ~7 MPa for t_{max} = 120 min to 17.2 MPa for t_{max} = 45 min, indicating a greater advantage of the 2-m flaw for a shorter duration of the transient, although, as indicated in both figures, there can be a small reversal of this trend for some combinations of t_{max} and $(K_{Ia})_{max}$. The results also show that the advantage of the 2-m flaw is greater for a $(K_{Ia})_{max}$ value of 330 than 220 MPa $\sqrt{m}$, provided t_{max} is somewhat less

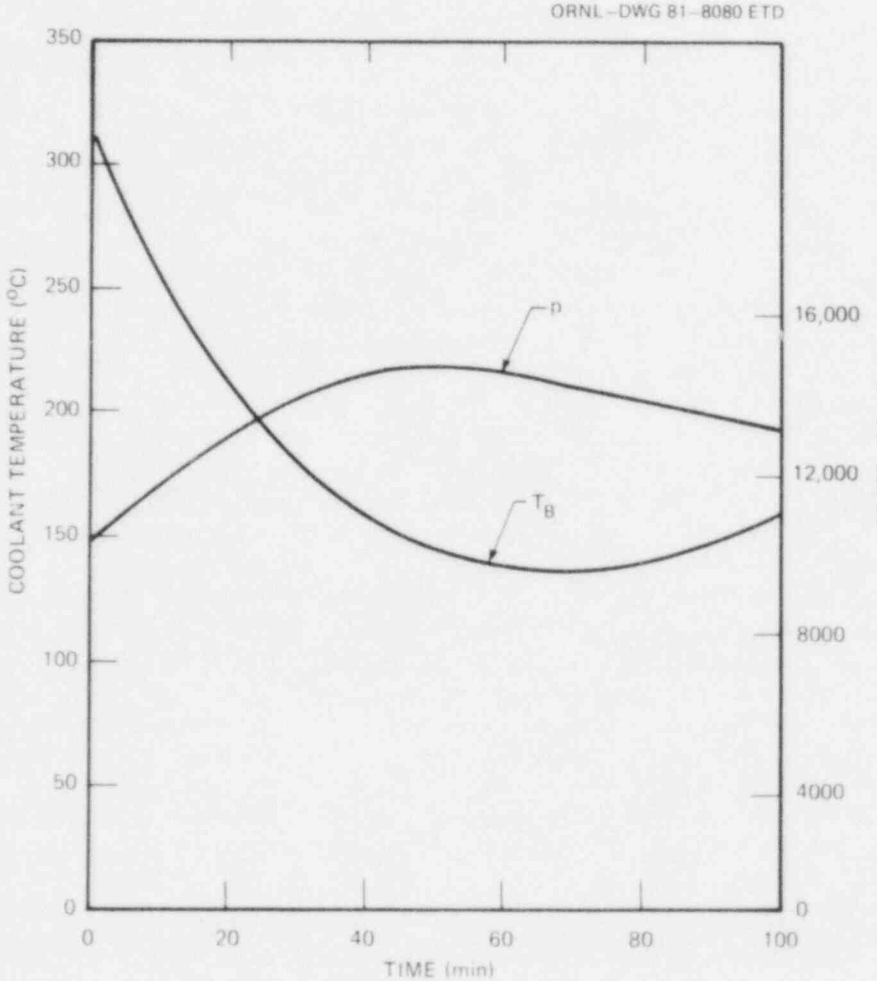

Fig. 8. Rancho Seco 1978 OCA coolant-temperature and pressure transients (smoothed).

than 60 min, and provided that the pressure is greater than ~10 MPa. For $t_{max} \geq 60$ min, there is no effect of increasing $(K_{Ia})_{max}$ from 220 to 330 MPa $\sqrt{m}$.

From a quantitative point of view, the advantage in replacing the (2-D, 2-D) flaw combination with the (2-D, 2-m) combination for T_f = 121°C and p = 10 MPa corresponds to an increase in $(RTNDT_s)_{cf}$ of 76, 31 and 0°C for t_{max} = 30, 45 and ≥60 min, respectively, and the increase is independent of the value of $(K_{Ia})_{max}$ (220—330 MPa $\sqrt{m}$). At a pressure of 17.2 MPa, there is no advantage except for t_{max} = 30 min and $(K_{Ia})_{max}$ = 330 MPa $\sqrt{m}$, and for this case the increase in $(RTNDT_s)_{cf}$ is ~34°C.

As shown in Figs. 10 and 11, replacement of the 2-D initial flaw with the 6/1 flaw increases $(RTNDT_s)_{cf}$ for pressures that are greater than a value that is a little less than p_c. For pressures greater than this value the increase is nearly independent of pressure, and is greater for larger values of t_{max}; for t_{max} = 30 min the increase in $(RTNDT_s)_{cf}$ is ~6°C, and for t_{max} = 120 min it is ~12°C.

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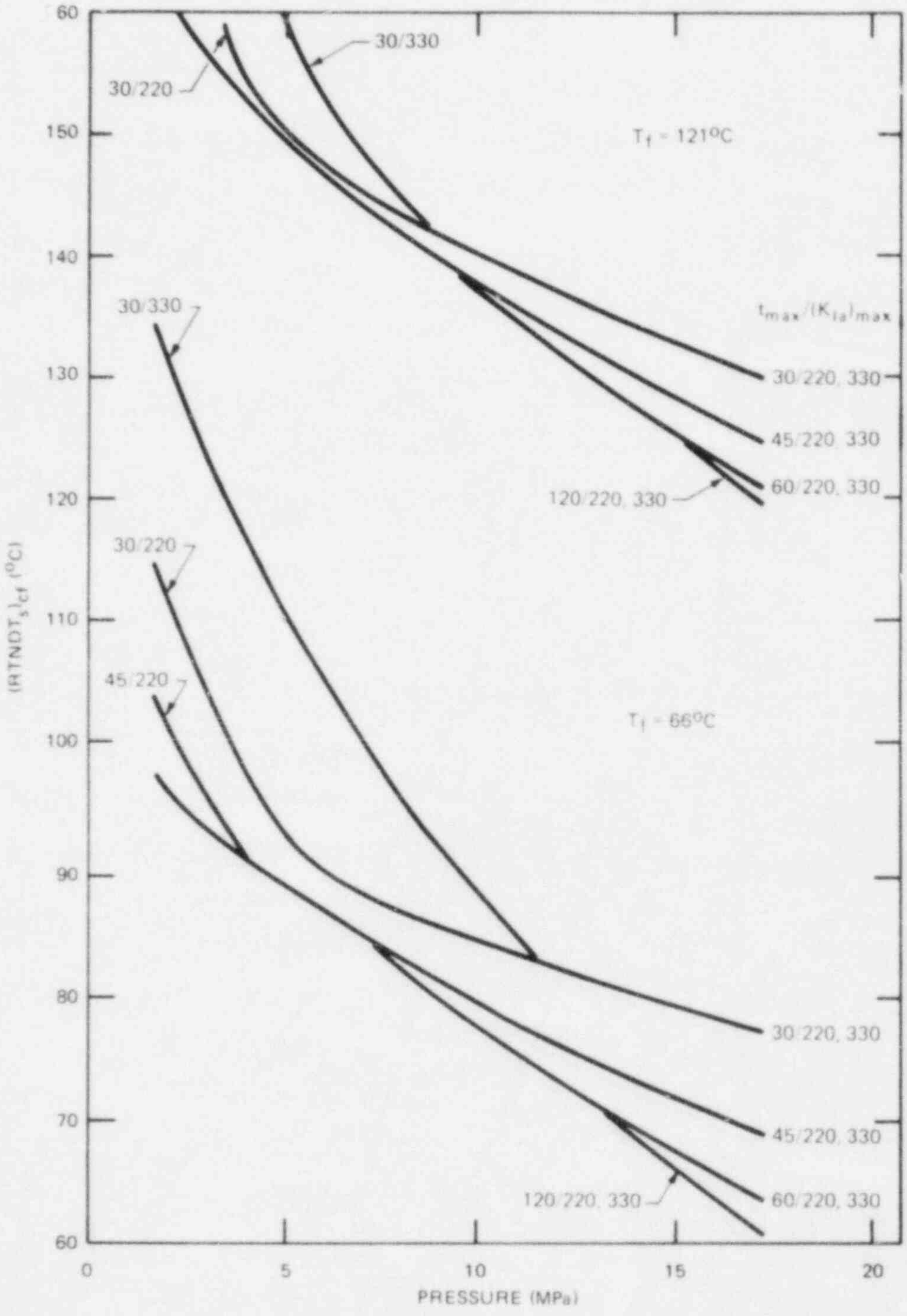

Fig. 9. $(RTNDT_s)_{cf}$ vs pressure for several values of T_f, $(K_{Ia})_{max}$ and t_{max}.

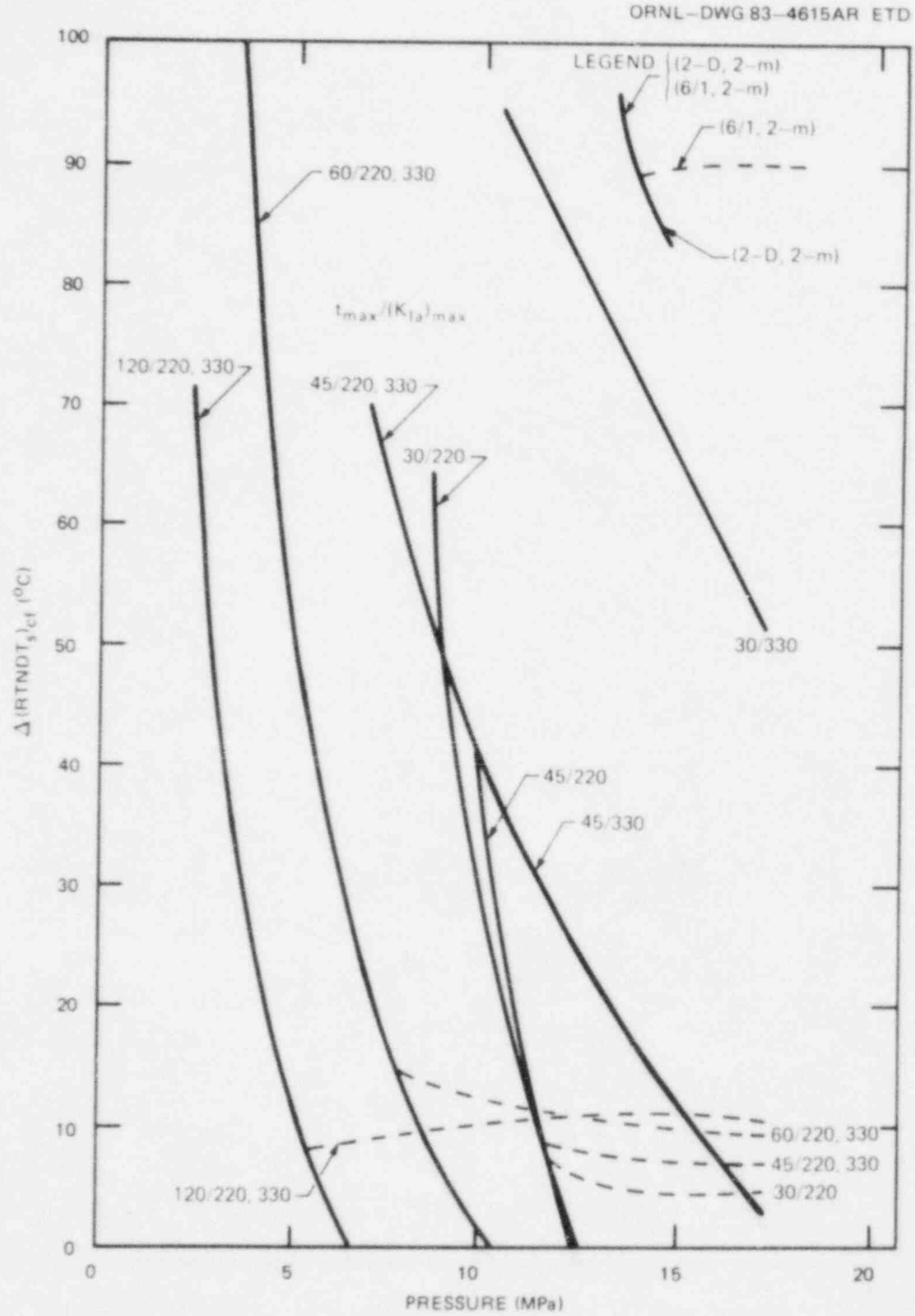

Fig. 10. Increase $[\Delta RTNDT_s)_{cf}]$ in $(RTNDT_s)_{cf}$ for (2-D, 2-m) and (6/1, 2-m) flaw combinations relative to (2-D, 2-D) combination for T_f = 66°C.

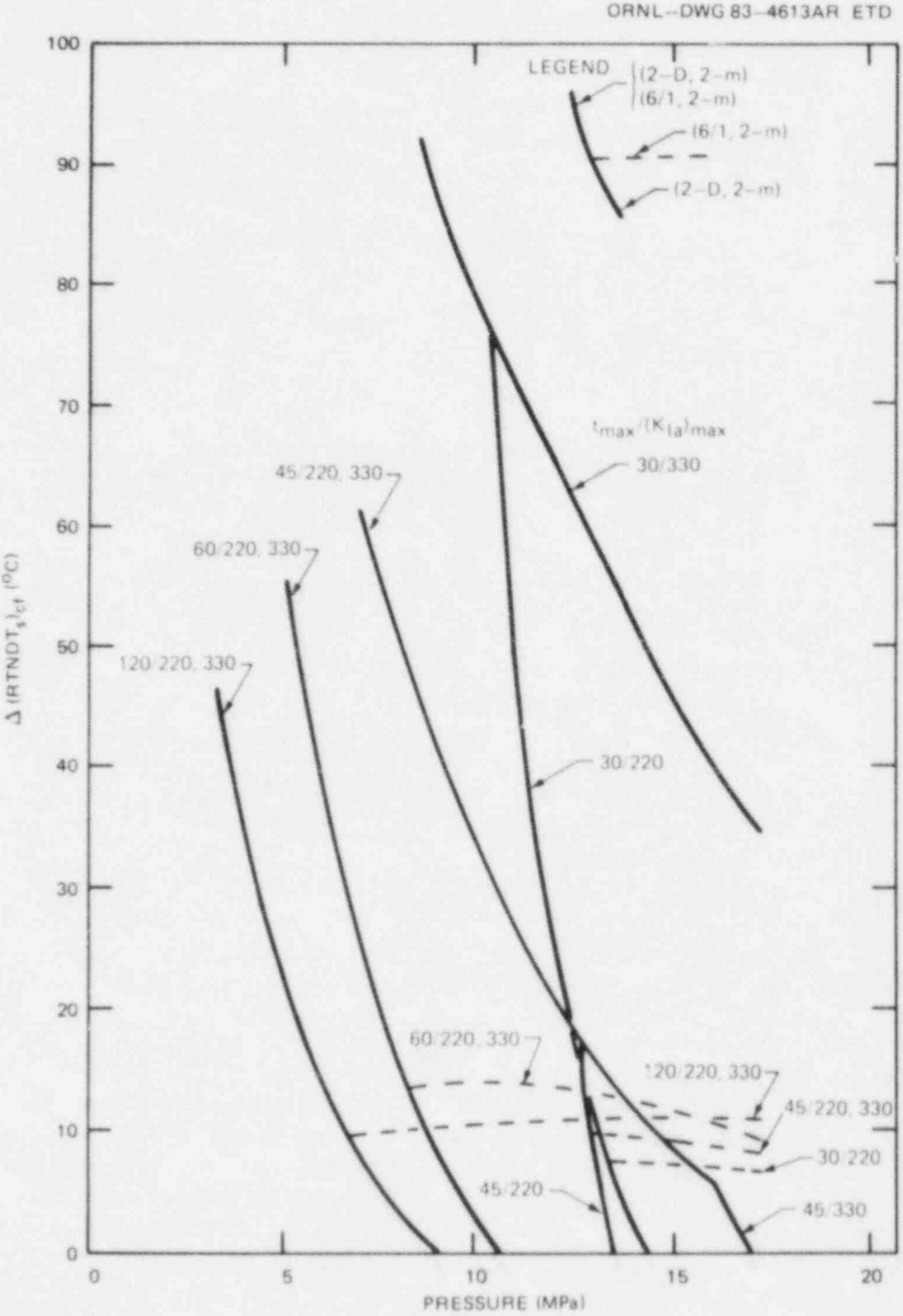

Fig. 11. Increase [$\Delta(RTDNT_s)_{cf}$] in $(RTNDT_s)_{cf}$ for (2-D, 2-m) and (6/1, 2-m) flaw combinations relative to (2-D, 2-D) combination for T_f = 121°C.

Table 2. Input data for OCA cases calculated

Vessel dimensions, mm	
OD	4800
ID	4370
T_f, °C	66, 121
T_i, °C	288
n, min^{-1}	0.15
t_{max},[a] min	30, 45, 60, 120
h_f,[b] $W \cdot m^{-2} \cdot °C^{-1}$	1870[c,d]
p, MPa	0–17.2
$RTNDT_o$, °C	−18[d]

[a]Duration of transient.

[b]Fluid-film heat transfer coefficient.

[c]Corresponds to main pumps off.

[d]Used for both Rancho Seco and postulated OCA's.

A comparison of Figs. 10 and 11 indicates that the advantage of the (2-D, 2-m) and (6/1, 2-m) flaw combinations over the (2-D, 2-D) combination is about the same for T_f = 66 and 121°C, although there are some significant differences for t_{max} = 30 min.

Estimates of the extension in calculated lifetime of a PWR pressure vessel due to increases in $(RTNDT_s)_{cf}$ can be obtained using Eq. (1) and typical PWR fluence rates ($\dot{F}_o$). As an example, estimates corresponding to a change in flaw combination from (2-D, 2-D) to (2-D, 2-m) were made for a case in which T_f = 121°C, n = 0.15 min^{-1}, p = 10 MPa, $RTNDT_o$ = −18°C, Cu = 0.3%, Ni = 0.8% and $(K_{Ia})_{max}$ = 220 MPa $\sqrt{m}$. The results are presented in Table 3 for t_{max} = 30, 45 and ≥60 min and for the approximate extremes in fluence rates among the existing PWR's (0.3 and 1.5 × 10^{18} neutrons$\cdot cm^{-2} \cdot EFPY^{-1}$). As shown in the table there is no extension of the lifetime for t_{max} ≥ 60 min. However, if the 2-D initial flaw were replaced with a 6/1 flaw, there would be an extension for t_{max} ≥ 60 min of 9 and 2 EFPY for F_o = 0.3 and 1.5 × 10^{18} neutrons$\cdot cm^{-2} \cdot EFPY^{-1}$.

Analysis of the Rancho Seco transient described in Fig. 8 indicated no increase in $(RTNDT_s)_{cf}$ as a result of changing the flaw combination

Table 3. Estimates of the extension in calculated vessel lifetime due to changing the assumed flaw combination from (2-D, 2-D) to (2-D, 2-m) (sample case described in text)

t_{max} (min)	$(RTNDT_s)_{cf}$[a] (°C)	$F_o \times 10^{-19}$[b] (n/cm^2)	ΔEFPY[c]	
			$\dot{F}_o \times 10^{-18}$[d]	
			0.3	1.5
30	214	2.45	49	10
45	169	1.65	22	4
≥60	138	0.98	0	0

[a]Values for (2-D, 2-m) flaw combination.

[b]Inner-surface fluence to achieve indicated $(RTNDT_s)_{cf}$.

[c]Extension in vessel life due to change in flaw combination.

[d]Fluence rate at inner surface ($neutrons \cdot cm^{-2} \cdot EFPY^{-1}$).

from (2-D, 2-D) to (2-D, 2-m). However, changing the combination from (2-D, 2-D) to (6/1, 2-m) increased $(RTNDT_s)_{cf}$ by ~11°C.

Circumferential flaws were analyzed in a similar fashion to the extent of determining whether the circumferential flaw might have a lower value of $(RTNDT_s)_{cf}$ than the axial flaw. The comparison was made between the (2-D, 2-D) circumferential-flaw combination and the (2-D, 2-m) axial-flaw combination, and for none of the cases considered was the circumferential flaw limiting.

6. SUMMARY

For the purpose of evaluating the degree of conservatism in the state-of-the-art fracture-mechanics model used for analyzing flaw behavior in PWR pressure vessels during overcooling accidents, the effect of replacing the conventional two-dimensional flaw with specific finite-length flaws was investigated. In doing so, the sensitivity of this effect to a specified maximum crack-arrest toughness and to the duration of postulated transients was investigated.

One of the finite-length flaws was oriented in the axial direction with a length equal to the height of a shell course (~2 m). This flaw is of particular interest for plate-type vessels that suffer relatively high radiation damage in the welds since the length of a flaw in an axial weld would tend to be limited to the length of the weld. The other finite-length flaw was semielliptical in shape with a length-to-depth ratio of 6/1.

Arguments were made for using an effectively infinitely long (2-D) axial flaw for the initial shallow flaw and using a 2-m-long (2-m) flaw for the first arrest and subsequent events. An even less conservative combination consisted of a 6/1 flaw for the first initiation event and the 2-m flaw for subsequent events. For the circumferential direction a 6/1 flaw was used for the first initiation event and a 2-D flaw for subsequent events. Using these several flaw combinations, including the 2-D flaw for all events, critical values of RTNDT corresponding to vessel failure were calculated and compared to determine the benefit of considering finite-length flaws.

The results indicate that the benefit in using the (2-D, 2-m) combination can be substantial and increases with decreasing primary-system pressure, decreasing duration of the transient and by increasing the limit on the crack arrest toughness. However, there are critical pressures above which there may be no benefit, depending on the duration of the transient and the limit on crack-arrest toughness. The benefit of using the 6/1 initial flaw in combination with the 2-m flaw is limited to ~11°C and exists only at pressures above the critical values. For high-pressure, long-duration transients such as the Rancho Seco transient there is no benefit in using the (2-D, 2-m) flaw combination over the (2-D, 2-D) flaw.

REFERENCES

1. R. D. Cheverton, S. K. Iskander and G. D. Whitman, "The Integrity of PWR Pressure Vessels During Overcooling Accidents," *Proceedings of the International Meeting on Thermal Nuclear Reactor Safety*, NUREG/CP-0027, Vol. 1, pp. 421-430, February 1983.

2. K. Kussmaul, J. Jansky and J. Föhl, "The Consequence of the Coincidence of Irradiation Embrittlement; Surface Cracking and Pressurized Thermal Shock (PTS) in RPVs of LWRs," *Proceedings of the International Meeting on Thermal Nuclear Reactor Safety*, NUREG/CP-0027, Vol. 1, pp. 631-643, February 1983.

3. V. K. Chexal, T. U. Marston and B.K.H. Sun, "The EPRI Program Concerning Reactor Vessel Pressurized Thermal Shock," *Proceedings of the International Meeting on Thermal Nuclear Reactor Safety*, NUREG/CP-0027, Vol. 1, pp. 644-660, February 1983.

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

5. Bernard Houssin et al., "Radiation Embrittlement of PWR Reactor Vessel Weld Material: Nickel and Copper Synergistic Effects," *Effects of Irradiation on Materials*, ASTM STP 782, August 1982.

6. L. E. Steele, editor, *Status of USA Nuclear Reactor Pressure Vessel Surveillance for Radiation Effects*, ASTM STP 784, January 1983.

7. R. D. Cheverton et al., "Thermal-Shock Investigations," *Heavy-Section Steel Technology Program Quart. Prog. Rep. for October-December 1979*, NUREG/CR-1305, ORNL/NUREG/TM-380, pp. 67-70, May 1980.

8. R. D. Cheverton et al., *Pressure Vessel Fracture Studies Pertaining to the PWR Thermal-Shock Issue: Experiments TSE-5, TSE-5A and TSE-6*, NUREG/CR-4249 (ORNL-6163), June 1985.

9. T. U. Martson, editor, *Flaw Evaluation Procedures: ASME Section XI*, EPRI NP-719-SR, August 1978.

10. Yoshifume Nakawa et al., *Assessment of Fracture Toughness of Heavy-Section Steels for Nuclear Pressure Vessels*, Research Laboratories, Kawasaki Steel Corporation, September 1980.

11. R. D. Cheverton, S. K. Iskander and D. G. Ball, *PWR Pressure Vessel Integrity During Overcooling Accidents: A Parametric Analysis*, NUREG/CR-2895, ORNL/TM-7931, February 1983.

12. A. Sauter, R. D. Cheverton and S. K. Iskander, *Modification of OCA-I for Application to a Reactor Pressure Vessel with Cladding on the Inner Surface*, NUREG/CR-3155, ORNL/TM-8649, April 1983.

13. J. Heliot, R. C. Labbens and A. Pellissier-Tanon, "Semi-Elliptical Cracks in a Cylinder Subjected to Stress Gradients," *Fracture Mechanics*, ASTM STP 677, American Society for Testing and Materials, 1979, pp. 342-364.

14. I. S. Raju and J. C. Newman, Jr., "Stress-Intensity Influence Coefficients for Internal and External Surface Cracks in Cylindrical Vessels," *Aspects of Fracture Mechanics in Pressure Vessels and Piping*, PVP-Vol. 58, American Society of Mechanical Engineers, 1982, pp. 37-48.

15. J. J. McGowan, M. Raymund, "Stress Intensity Factor Solutions for Internal Longitudinal Semi-Elliptical Surface Flaws in a Cylinder Under Arbitrary Loadings," *Fracture Mechanics*, ASTM STP 677, American Society of Mechanical Engineers, 1979, pp. 365-380.

16. D. G. Ball, B. R. Bass, J. W. Bryson, R. D. Cheverton, and J. B. Drake, *Stress Intensity Factor Influence Coefficients for Surface Flaws in Pressure Vessels*, NUREG/CR-3723 (ORNL/CSD/TM-216), Oak Ridge Natl. Lab., Oak Ridge, Tennessee (February 1985).

17. S. K. Iskander, R. D. Cheverton and D. G. Ball, *OCA-I, A Code for Calculating the Behavior of Flaws on the Inner Surface of a Pressure Vessel Subjected to Temperature and Pressure Transients*, ORNL/NUREG-84, August 1981.

18. P. N. Randall - personal communication.

19. USNRC, "Effects of Residual Elements on Predicted Radiation Damage to Reactor Pressure Vessels Materials," Reg. Guide 1.99, Rev. 1 (Sept. 16, 1976).

20. D. G. Ball, R. D. Cheverton and S. K. Iskander, *OCA-II, A Code for Calculating the Behavior of 2-D and 3-D Surface Flaws in a Pressure Vessel Subjected to Temperature and Pressure Transients*, ORNL-5934 (February 1984).

21. W. Marshall, *An Assessment of the Integrity of PWR Pressure Vessels*, Second Report, United Kingdom Atomic Energy Authority, March 1982.

22. J. G. Merkle et al., "Fracture Mechanics Analysis and Investigations," *Heavy-Section Steel Technology Program Quart. Prog. Rep. July-September 1982*, NUREG/CR-2751, Vol. 3, pp. 3-7, January 1983.

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A continuing analysis of the pressurized water reactor pressurized thermal-shock problem indicates that the previously accepted degree of conservatism in the fracture-mechanics model needs to be more closely evaluated and, if excessive, reduced. One feature that was believed to be conservative was the use of two-dimensional as opposed to finite-length flaws. The degree of conservatism could not be adequately investigated because of computational limitations and a lack of knowledge regarding flaw behavior; however, that situation has changed to the extent that some cases involving finite-length flaws can be studied. A flaw of particular interest is one that is located in an axial weld of a plate-type vessel. For those vessels that suffer relatively high radiation damage in the welds, the length of the flaw will be no greater than the length of the weld, and recent calculations indicate that a depp flaw of that length (~2 m) is not effectively infinitely long, contrary to previous thinking.

The benefit to be derived from consideration of the 2-m flaw and also a semielliptical flaw with a length-to-depth ratio of 6/1 was investigated by analyzing several postulated transients. In doing so the sensitivity of the benefit to a specified maximum of the analysis indicate that for some conditions the benefit in using the 2-m flaw is substantial, but it decreases with increasing pressure, and above a certain pressure there may be no benefit, depending on the duration of the transient and the limit on crack-arrest toughness.

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