MAXIMUM ALLOWABLE TEMPERATURE FOR STORAGE OF SPENT NUCLEAR REACTOR FUEL

AN INTERIM REPORT

Hanford Engineering Development Laboratory

L. D. Blackburn
D. G. Farwick
S. R. Fields
L. A. James
R. A. Moen

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ABSTRACT

Evaluations are made as to recommended maximum allowable cladding temperatures of spent nuclear fuels for extended dry storage. A number of near-surface dry storage concepts have been evaluated, as well as concepts for geologic storage. Work is continuing to upgrade and revise maximum temperature evaluations as the program proceeds.

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I. SUMMARY

A maximum allowable cladding temperature of 380°C (715°F) is recommended on an interim basis for extended dry storage of spent nuclear reactor fuel. Cladding temperatures of 396°C (745°F) may be acceptable for short periods of storage or processing applications. Lower allowable temperatures may be required if present estimates of clad stress corrosion cracking are verified.

11. INTRODUCTION

The objective of the Spent Fuel Handling and Packaging Program is to provide packaging of spent nuclear reactor fuel prior to geologic storage or disposal. A number of near-surface dry storage concepts were evaluated in the early phases of the program.⁽¹⁾ These concepts, as well as geologic storage, all have a potential for relatively high fuel temperatures early in the storage life because the nuclear decay heat must be rejected through poor thermal conductivity paths (soil, geological formation, etc.). In addition, projected storage or disposal conditions of spent fuel may call for packaging processes that require elevated temperature processing. These considerations require an assessment of the maximum allowable temperature to which spent fuel may be subjected. The purpose of this report is to provide such an assessment for early program use; work is in progress to upgrade and revise these evaluations as the program proceeds.

III. CONDITIONS AND ASSUMPTIONS

The performance criterion selected for the present evaluations is that spent fuel rod cladding shall not be damaged or perforated so that gaseous fission products would be released. This criterion was selected to yield the most conservative limit on maximum allowable temperature. Fuel assemblies are assumed to be held at relatively low temperature (e.g., water basin storage) for 5 yr after reactor discharge before being placed in dry storage. All spent fuel assemblies are assumed to be packaged in carbon steel canisters [49-m (16-ft) long with an internal diameter of 43 cm (17 in.)] filled with helium gas at nominally atmospheric pressure. The low external pressure on fuel rods relative to in-reactor service produces important changes in the magnitude and direction of stresses resulting from fuel rod internal pressure. A review of fuel rod internal pressures and stresses for pressurized water reactor (PWR) and boiling water reactor (BWR) systems is presented in Appendix A. Selected values of fuel rod internal pressure at 25°C are:

- 3.8 MPa (550 psi) for a typical condition and 7.7 MPa (1120 psi) for a design condition for PWR fuel and
- 0.2 MPa (30 psi) for a typical condition and 1.8 MPa (265 psi) for a design condition for BWR fuel.

Hoop stress levels in the cladding wall for various pressures and temperatures are shown in Figure A-1. Fuel rod cladding material is assumed to be cold-worked and stress-relieved Zircaloy-4 (PWR) or annealed Zircaloy-2 (BWR).⁽²⁾ These evaluations concern steady-state storage conditions only; effects of transient or accident conditions are not included. Consideration is given to both undefected cladding (cladding without cracks or flaws) and defected cladding (cladding with flaws) since reactor service can produce sharp cracks extending halfway through the wall of unfailed cladding.⁽³⁾

IV. FAILURE OR DEGRADATION MECHANISMS

Specific cladding failure or degradation mechanisms considered in these evaluations include the following:

1. Rupture from mechanical overload under rapid loading conditions,
2. Stress rupture,
3. Stress corrosion cracking (SCC),
4. Rapid fracture of flawed cladding,
5. Delayed hydrogen cracking and fatigue-crack propagation,
6. Internal hydriding, and
7. Oxidation (internal and external).

The first three mechanisms are of primary concern for spent fuel storage conditions; maximum allowable temperatures for these mechanisms will be developed in subsequent sections. Yield and ultimate strength values for short-term loadings needed in assessing temperature limits are summarized in Appendix B. Correlations of stress-rupture data needed for subsequent calculations are developed in Appendix C. Interim correlations of available SCC data for use in later evaluations are presented in Appendix D.

The last four mechanisms in the list do not appear to be active for spent fuel storage conditions. Appendix E summarizes fracture toughness data needed to validate the integrity of flawed cladding. The calculations in Appendix D show that stress intensity factors for cladding with flaws extending halfway through the wall are still well below the critical values required for rapid, unstable crack propagation. Delayed hydrogen cracking and fatigue crack propagation are unlikely since SCC should predominate and many applications of cyclic loadings are not anticipated. Internal hydriding is not considered to be an active spent fuel storage failure mechanism (see Appendix F); flaws produced by in-reactor hydriding should not cause rapid fracture in material having high fracture toughness values, such as those in Appendix E. Oxidation of fuel cladding is not a degradation mechanism because both the internal and external oxygen supply to the rod is too

limited (see Appendix G). This position is based on the assumption that no air leaks into the canister during storage; leakage effects could be treated by the methods discussed in Reference 4.

A. TEMPERATURE LIMIT FOR MECHANICAL OVERLOAD

Failure from mechanical overload is typically restricted by limiting service or design stress levels to values below material strength properties. Fuel rod design for reactor service limits pressure-induced stress to less than $2/3 S_y$ or $1/2 S_u$, whichever is smaller, where S_y is the material yield stress and S_u is the material ultimate strength.⁽⁵⁾ Minimum values of S_y and S_u are given in Appendix B. The principal loading of fuel rod cladding comes from internal pressure; therefore, the relationship between this biaxial loading condition and the uniaxial loading conditions used to determine S_y and S_u must be established. Zirconium alloys develop different textures during various fabricating operations, and this texture development affects the relationship between biaxial and uniaxial strength properties. The results presented in References 6-8 indicate that ratios of 1.1 and 1.0 are appropriate for biaxial to uniaxial yield stress and biaxial to uniaxial ultimate strength, respectively. Thus, service hoop stress values should be limited by the lesser of $(2/3)(1.1 S_y)$ or $1/2 S_u$. Figures 1 and 2 show that the maximum allowable temperature for design conditions is 396°C (745°F) for PWR spent fuel and >427°C (800°F) for BWR spent fuel.

Typical service stress levels in fuel rod cladding are well below design stress levels; this difference becomes part of the design margin for preventing failure. Cladding that contains cracks or flaws can be viewed as having a lesser design margin. For example, in PWR cladding the ratio of design stress to typical stress is about 2, and if $1/2 S_u$ limits the stress, the total design margin is a factor of four on stress. However, at the location of a flaw halfway through the cladding wall, the typical net section stress is twice that of unflawed cladding. Thus, the total design margin at the flaw location is only a factor of two on stress.

FIGURE 1. Maximum Allowable Temperature for Mechanical Overload of PWR Fuel Cladding.

FIGURE 2. Maximum Allowable Temperature for Mechanical Overload of BWR Fuel Rod.

B. TEMPERATURE LIMIT FOR STRESS-RUPTURE

Failure **by a** stress-rupture mechanism is typically guarded against by restricting design stress levels to less than $2/3 S_r$, where S_r is the minimum stress to produce rupture in some time (t). The present evaluation will use S_r values that depend on the storage lifetime; this approach parallels the use of time-dependent allowable stress intensity at elevated temperatures in ASME Code Case 1592.⁽⁹⁾ Using the correlations for unirradiated materials from Appendix C, curves of $2/3 S_r$ vs temperature for various storage lifetimes are readily constructed (see Figures 3 and 4 for PWR and BWR cladding, respectively). When the design stress level curve is added to the figures, the points of intersection determine the maximum allowable cladding temperature for isothermal storage. These limits range from 361°C (682°F) for 1-yr storage to 321°C (610°F) for 100-yr storage of PWR fuel. Higher temperature limits are obtained for BWR fuel, primarily because of the lower design stress level.

FIGURE 3. Determination of Maximum Allowable Temperature for PWR Fuel Storage at Constant Temperature.

FIGURE 4. Determination of Maximum Allowable Temperature for BWR Fuel Storage at Constant Temperature.

Isothermal conditions are, however, not expected in dry storage of spent fuel because the decay heat varies markedly over the storage life.⁽¹⁾ For the calculations described below, it was assumed that the temperature difference between fuel cladding and the heat sink was proportional to the decay heat for any arbitrarily selected initial fuel cladding temperature. The variation of decay heat and cladding temperature for a PWR assembly with storage life is illustrated in Figure 5. A creep damage rule must therefore be used to assess the effect of varying stress and temperature utilizing the isothermal stress-rupture data. The life fraction rule⁽¹⁰⁾ was selected for the present calculations in the form

$$D = \int_0^t \frac{dt}{t_d(T, \sigma)} \quad (1)$$

where D = creep damage,

t = time,

t_d = design allowable life (which is a function of temperature and stress and hence time, as shown in Figure 5).

FIGURE 5. Decay Heat and Fuel Cladding Temperature as a Function of Storage Time.

The design allowable life is obtained from isothermal rupture life by applying an appropriate design margin, in this case a factor of 2/3 on rupture stress. The allowable storage lifetime for any given temperature history is defined by the condition $D = 1$. Creep damage calculations for various initial cladding temperatures are described in greater detail in Appendix H and are summarized in Figure 6. As the temperature decreases during storage, the rate of damage accumulation slows, so that indefinite storage is possible at a starting temperature just under 380°C (715°F). The anticipated thermal history, therefore, has an important effect in raising the maximum allowable temperature above values predicted for isothermal storage.

Conservatism is introduced into these calculations by providing a design margin on the rupture stress and by using design cladding stress levels that are higher than values expected during service. This procedure translates to

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FIGURE 6. Accumulation of Creep Damage for Various Starting Temperatures.

a conservatism in the maximum allowable temperature limit, illustrated in Figure 7 for 1-yr of isothermal storage. Damage calculations for the anticipated thermal history confirm the increase of 60°C (110°F) on relaxing from $2/3 S_r$ to nominal rupture stress. Thus, the total conservatism of 105°C (190°F) indicated in Figure 7 is also appropriate for **nonisothermal** storage.

Cracked or flawed cladding can again be viewed as having a reduced design margin or conservatism. The margin associated with the use of the design stress level rather than the typical stress level for **PWR** cladding (a factor of 2) is lost at the location of a flaw halfway through the cladding wall. At this location, only the factor of $2/3$ on the minimum rupture stress (or $60^{\circ}\text{C}/110^{\circ}\text{F}$ in the temperature limit) remains as design margin.

FIGURE 7. Conservatism in Maximum Allowable Temperature Limit.

C. TEMPERATURE LIMIT FOR STRESS CORROSION CRACKING

Pellet-cladding interaction (PCI) is responsible for some failure in fuel rod cladding during reactor service by an SCC mechanism.⁽³⁾ Cladding with satisfactory in-reactor performance could still fail by SCC in storage if the required environmental conditions are produced.

As outlined in Appendix D, SCC can be limited by: 1) assuring that the cladding stress intensity factor never exceeds the threshold value K_{ISCC} or 2) if K_{ISCC} is exceeded, assuring that the crack growth rate is too low for a crack to penetrate the cladding wall in the design lifetime. Calculations of the type illustrated in Appendix D indicate that once K_{ISCC} is exceeded crack growth leads to wall penetration in a short time for temperatures above 300°C (572°F). The temperature at which K_{ISCC} is exceeded thus becomes the controlling factor in establishing the temperature limit.

To provide a design margin, it is appropriate to limit the stress intensity factor to less than $1/2 K_{Isc}$. The maximum allowable temperature becomes dependent on initial flaw size for this failure mechanism. For flaw sizes that may be representative of as-fabricated cladding, ⁽¹¹⁾ temperature limits of $\sim 350^{\circ}\text{C}$ (662°F) and $\sim 340^{\circ}\text{C}$ (644°F) are obtained for flaw sizes $a = 8\mu$, $a/c = 0.9$ and $a = 8\mu$, $a/c = 0.2$, respectively, where a is the crack depth and c is the crack length. On the other hand, cladding subjected to PCI during reactor service may have cracks extending halfway through the wall⁽³⁾ with an associated temperature limit of $\sim 290^{\circ}\text{C}$ (554°F). Additional experimental evidence to verify the evaluation of K_{Isc} and provide a better definition of starting flaw sizes is needed to validate these preliminary estimates.

V. RECOMMENDATIONS

It is recommended that a maximum allowable cladding temperature of 380°C (716°F) for spent fuel storage be used for preliminary design scoping studies. This limit is based on stress-rupture considerations for spent PWR fuel assemblies; higher temperatures can be allowed for BWR assemblies if necessary. Detailed design studies may benefit from recalculation of the maximum allowable temperature using the methods employed here if the actual thermal history is different from that selected for the present computations.

Estimates of maximum allowable temperatures based on SCC behavior are lower than the value recommended above. At the present time, the potentially large uncertainties in an SCC temperature limit (due to an inadequate mechanistic understanding and a very limited data base) dictate against placing severe restraints on spent fuel storage concept selection or design scoping studies. Further experimental work to verify potential SCC effects will be performed as the program proceeds. Present estimates should be viewed as a warning that maximum allowable temperatures could be lower than 380°C (716°F) if SCC effects are verified.

Maximum allowable temperatures for very short-term applications as determined from material strength properties are 395°C (745°F) for PWR fuel and >427°C (800°F) for BWR fuel. These limits assume that only internal pressure loading is applied to the cladding. If other mechanical or thermal loads are superimposed on the pressure load, the temperature limits must be reevaluated.

REFERENCES

1. "Spent Unreprocessed Fuel Facility Engineering Studies," RHO-LD-2, February 1978.
2. D. H. Locke, "Review of Experience with Water Reactor Fuels 1968-1973," Nucl. Eng. and Design, Vol. 33, 1975, p. 94.
3. J. T. A. Roberts, E. Smith, N. Fuhrman, and D. Cubicciotti, "On the Pellet-Cladding Interaction Phenomenon," Nucl. Tech., Vol. 35, 1977, p. 131.
4. D. G. Roase and T. T. Vandergraaf, "The Canadian Spent Fuel Storage Canister: Some Materials Aspects," Nucl. Tech., Vol. 32, 1977, p. 60.
5. H. E. Williamson and D. C. Ditmore, "Current BWR Fuel Design and Experience," Reactor Tech., Vol. 14, 1971, p. 68.
6. D. G. Hardy, "The Effect of Neutron Irradiation on the Mechanical Properties of Zirconium Alloy Fuel Cladding in Uniaxial and Biaxial Tests," Irradiation Effects on Structural Alloys for Reactor Applications, ASTM STP-484, American Society for Testing and Materials, Philadelphia, 1970, p. 215.
7. C. R. Woods, ed., "Properties of Zircaloy-4 Tubing," WAPD-TM-585, December 1966.
8. D. B. Scott, "Physical and Mechanical Properties of Zircaloy-2 and 4," WCAP-3269-41, May 1965.
9. ASME Boiler and Pressure Vessel Code Case 1592, Class 1 Components in Elevated Temperature Service, Section 111, American Society of Mechanical Engineers, New York.
10. M. M. Abo El Ata and J. L. Finnie, "A Study of Creep Damage Rules," J. Basic Eng., September 1972, p. 533.
11. P. H. Kreyns, G. L. Spahr, and J. E. McCauley, "An Analysis of Iodine Stress Corrosion Cracking of Zircaloy-4 Tubing," Journal of Nuclear Materials, Vol. 61, 1976, p. 203.

APPENDIX A

INTERNAL GAS PRESSURE AND HOOP STRESS IN SPENT LWR FUEL RODS

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INTERNAL GAS PRESSURE AND HOOP
STRESS IN SPENT LWR FUEL RODS

Internal gas pressure in spent LWR fuel rods produces stresses in the cladding wall that can be important in determining cladding integrity for long-term storage. This appendix documents a rationale for pressures and stresses selected to assess spent fuel storage performance.

Fuel rods for light water reactors (LWRs) are designed using criteria that preclude outward creep of the cladding.⁽¹⁾ Outward creep of the cladding reduces the gap (between fuel and cladding) conductance. Resulting increases in fuel temperature also increase fission gas release, and this causes further outward creep of the cladding. An unstable situation leading to cladding failure can result. One way to preclude such outward creep is to limit the maximum internal fuel rod pressure so that it is less than the coolant pressure. This criterion is specifically stated for PWR systems.^(2,3) An upper limit on internal gas pressure is not specified for BWR fuel; however, the statement that "the maximum internal pressure generally does not exceed 1800 psia"⁽⁴⁾ would seem to allow internal pressures to exceed the external coolant pressure of 7.0-7.2 MPa (1020-1040 psia). In actual fact, the fuel design appears to keep internal pressures well below the coolant pressure.

A maximum internal pressure (p_m) in a spent fuel rod can therefore be estimated by the external reactor system (coolant) pressure (p_{ex}). That is,

$$p_m = p_{ex} \quad (A-1)$$

at the reactor operating temperature (T_o). Values of T_o can be approximated by the coolant temperature, by a maximum clad temperature, or by some mean clad temperature. Since cladding temperatures are greater than the coolant temperature, the use of the latter gives conservative predictions (i.e., large values of p_m). Using the results from Reference 5 for outlet coolant temperatures

$$T_o = 326^{\circ}\text{C} \text{ (619}^{\circ}\text{F) for PWRs} \quad (\text{A-2})$$

$$T_o = 290^{\circ}\text{C} \text{ (554}^{\circ}\text{F) for BWRs} \quad (\text{A-3})$$

At any other reference temperature (T_r)

$$p_m = p_{ex} \frac{T_r V_o}{T_o V_r} \quad (\text{A-4})$$

where V_o and V_r are the void volumes of the fuel rod at operating and reference conditions, respectively. The void volume consists, primarily of the plenum volume at the upper end of the fuel column and the annular gas gap volume between fuel and cladding. Since the annular gas gap and plenum volume tend to decrease as a result of differential expansion when the reactor goes to power,

$$\frac{V_o}{V_r} < 1 \quad (\text{A-5})$$

Then p_m can be estimated conservatively as

$$p_m = p_{ex} \frac{T_r}{T_o} \quad (\text{A-6})$$

taking T_r as 25°C (77°F),

$$\begin{aligned} p_m &= \frac{(2250)(298)(6.894757 \times 10^{-3})}{(599)} \text{ MPa absolute} \quad (\text{A-7}) \\ &= 7.72 \text{ MPa (1120 psi) for a PWR} \end{aligned}$$

and

$$p_m = \frac{(1040)(298)(6.894757 \times 10^{-3})}{(563)} \text{ MPa absolute} \quad (\text{A-8})$$

$$= 3.8 \text{ MPa (550 psi) for a BWR}$$

where p_{ex} values were obtained from Reference 5.

The above estimate of p_m for PWR fuel compares favorably with the estimate of ~8.3 MPa (1200 psi) given by Johnson.⁽⁶⁾ On the other hand, the upper internal pressure of 11.7 MPa (1700 psi) given in Reference 2 (Figure 4.2-9) yields a value of 5.8 MPa (845 psi) at the reference temperature of 25°C (77°F). This is in reasonable agreement with Johnson's estimate of 5.5 MPa (800 psi) as pressures that are occasionally achieved in PWR rods. Johnson estimates that pressures of 1.7-3.8 MPa (250-550 psi.) are typical end-of-life pressures for prepressurized PWR fuel. Locke⁽⁷⁾ gives a range of 1.4-2.8 MPa (200-400 psi) for prepressurized PWR fuel. The specific helium prepressure may be different for each fuel region and for different plants and depends on fuel burnup as well as fuel densification potential.⁽²⁾

The estimate of p_m based on p_{ex} given for BWR fuel is substantially higher than Johnson's estimate of 1.4-2.0 MPa (200-300 psi) for rarely achieved internal pressures. Another estimate of BWR internal pressure can be made as follows

$$p_r = p_f + p_b \quad (\text{A-9})$$

where p_r is the total rod pressure, p_f is the fission gas pressure, and p_b is the backfill pressure which is one atmosphere.⁽⁴⁾ Then

$$p_f = p_r - p_b = \frac{f n_f R T_r}{V_r} \quad (\text{A-10})$$

where f is the fraction of the generated fission gas released to the void volume and n_f is the number of gram atoms of fission gas generated. The maximum fission gas release fraction in the highest design power density rod is 20% for current 8 x 8 BWR fuel.⁽⁴⁾ Fission gas generation is 1.56×10^{-5} gram moles/GJ (1.35×10^{-3} gram moles/MWd).⁽⁴⁾ Analysis of the information presented in Reference 5 gives the exposures listed in Table A-1. The maximum GJ (MWd) for BWR fuel is 943.5 (109.2). Calculating V_r as the cold gap and plenum volumes using the dimensions given in Reference 4 yields a value of 42.3 cm³. Thus,

$$p = \left[14.7 + \frac{(0.2)(1.35 \times 10^{-3})(109.2)(82.057)(298)(14.7)}{42.3} \right] 6.894757 \times 10^{-3}$$

$$= 1.83 \text{ MPa (265 psi)} \quad . \quad (A-11)$$

TABLE A-1

RANGE OF EXPOSURE PER FUEL ROD FOR OPERATING
AND PLANNED BWR REACTORS*

Range of GJ/rod (MWd/rod)	Number of Reactors	
	Operating	Planned
11,000-12,000 (130-140)	0	1
10,000-11,000 (120-130)	0	0
9,503-10,000 (110-120)	0	0
6,600-9,500 (100-110)	3	0
7,700-8,600 (90-100)	3	1
6,900-7,700 (60-90)	6	11
6,000-6,900 (70-80)	2	5
5,200-6,000 (60-70)	3	7
4,300-5,200 (50-60)	1	2
3,500-4,300 (40-50)	1	0
2,600-3,500 (30-40)	1	0
1,700-2,600 (20-30)	1	0
660-1,700 (10-20)	0	0
Maximum GJ/rod (MWd/rod)	9435 (109.2)	11932 (138.1)

*Reference 5

This estimate is in good agreement with Johnson's upper limit.⁽⁶⁾ Johnson estimates that 0.5 MPa (75 psi) is reached only occasionally and that 0.2 MPa (30 psi) is a typical internal pressure for BWR fuel rods.

Stresses in the cladding wall produced by the combination of an internal pressure (p) and an external pressure (q) are given by (8)

$$\sigma_{\phi} = (p - q) \frac{a^2 (r^2 + b^2)}{r^2 (b^2 - a^2)} - q \quad (\text{A-12})$$

$$\sigma_z = (p - q) \frac{a^2}{b^2 - a^2} - q \quad (\text{A-13})$$

$$\sigma_r = (p - q) \frac{a^2 (r^2 - b^2)}{r^2 (b^2 - a^2)} - q \quad (\text{A-14})$$

where σ_{ϕ} = circumferential stress component,
 σ_z = axial stress component,
 σ_r = radial stress component,
 r = radial position in the tube wall,
 a = internal tube radius, and
 b = external tube radius.

The maximum stress (for elastic loading) occurs at the internal radius $r = a$, and the quantity $(a_{\phi} + q)/(p - q)$ for this location is tabulated in Table A-2 for various cladding sizes used in LWRs.^(2-5,9,10) Figure A-1 presents hoop stress as a function of internal pressure at 25°C (77°F) and temperature for the maximum values of $(a_{\phi} + q)/(p - q)$ obtained from Table A-2.

The preceding estimates of fission gas pressure and stresses all assume that negligible release of fission gas occurs during spent fuel storage because fuel temperatures are much lower in storage than during reactor operation.

TABLE A-2

FUEL CLADDING DIMENSIONS AND STRESS: PRESSURE RATIO

Reactor Type	Cladding OD		Cladding Thickness		$\frac{\sigma_{\phi} + q}{p - q} \bigg _{r=a}$
	(cm)	(in.)	(cm)	(in.)	
BWR	1.448	0.570	0.090	0.0355	7.561
	1.430	0.563	0.0940	0.037	7.143
	1.430	0.563	0.0813	0.032	8.327
	1.252	0.493	0.0864	0.034	6.787
PWR	1.072	0.422	0.0617	0.0243	8.214
	0.950	0.374	0.0572	0.0225	7.843
	1.118	0.440	0.0660	0.026	7.993
	0.970	0.382	0.0635	0.025	7.175
	1.092	0.430	0.0673	0.0265	7.646
	0.963	0.379	0.0597	0.0235	7.597

REFERENCES

1. Core Performance Branch, "The Role of Fission Gas Release in Reactor Licensing," NUREG-75/077, November 1975.
2. "Reference Safety Analysis Report," RESAR 3S, Vol. 2, July 1975, Docket STN 50545-2.
3. "Babcock and Wilcox Safety Analysis Report," B-SAR 205, Vol. 1, February 1976, Docket STN 50561-1.
4. "Standard Safety Analysis Report," GESSAR-251, Vol. 2, February 1975, Docket STN 50531-23.
5. Nucl. Eng. International, Vol. 22, No. 258, April 1977 Supplement.
6. A. B. Johnson, Jr., "Behavior of Spent Nuclear Fuel in Water Pool Storage," BNWL-2256, September 1977.
7. D. H. Locke, "Review of Experience with Water Reactor Fuels 1968-1973," Nucl. Eng. and Design, Vol. 33, 1975, p. 94.
8. J. Prescott, Applied Elasticity, Dover Publications, New York, 1946, pp. 329-332.
9. "La Salle County Station Units 1 and 2 Preliminary Safety Analysis Report," Docket 50373-2, Vol. 1, November 1970.
10. "Millstone Point Nuclear Power Station Unit 1, Final Safety Analysis Report," Docket 50245-1, Amendment 5, Vol. 1, March 1968.

APPENDIX B

STRENGTH PROPERTIES OF ZIRCALOY-2 AND ZIRCALOY-4

FIGURES

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STRENGTH PROPERTIES OF ZIRCALOY-2 AND ZIRCALOY-4

Strength properties of irradiated Zircaloy-2 and Zircaloy-4 were measured in a conventional tension test.¹⁻²⁷⁾ Plots of yield and ultimate strength vs temperature and neutron fluence are shown in Figures B-1 through B-8. These data cover a fluence range from 3×10^{18} n/cm² (E > 1 MeV) to 6×10^{21} n/cm² (E > 1 MeV), encompassing the range expected for LWR fuel rod cladding.

The data in these figures represent a range of preirradiation alloy conditions (e.g., degree of cold work and heat treatment schedule) and irradiation temperatures, which are not treated as separate variables here. Minimum expected strength values were selected by constructing lower bound lines in the figures so that all data fell above the line. The shape of the lower bound line on the strength vs temperature plots was based on strength-temperature relationships for unirradiated materials.^(13, 28)

FIGURE B-1. Yield Strength of Zircaloy-2 vs Temperature.

FIGURE B-2. Ultimate Tensile Strength of Zircaloy-2 vs Temperature.

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FIGURE B-3. Yield Strength of Zircaloy-2 vs Neutron Fluence.

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FIGURE B-4. Ultimate Tensile Strength of Zircaloy-2 vs Neutron Fluence.

FIGURE B-6. Ultimate Tensile Strength of Zircaloy-4 vs Temperature.

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FIGURE B-7. Yield Strength of Zircaloy-4 vs Neutron Fluence.

HEDL 7803-309.8

FIGURE B-8. Ultimate Tensile Strength of Zircaloy-4 vs Neutron Fluence.

REFERENCES

1. C. L. Howard, "Fuel Cycle Program - A Boiling Water Reactor Research and Development Program - Fifteenth Progress Report," GEAP-4641, July 15, 1964, pp. 23-24.
2. C. D. Williams et al., "Effects of Boiling Water Reactor Irradiation on Tensile Properties of Zircaloy," European Conference on Irradiation Behavior of Fuel Cladding and Core Component Materials, Karlsruhe, December 3-5, 1974, pp. 189-192.
3. L. M. Howe and W. R. Thomas, "The Effect of Neutron Irradiation on the Tensile Properties of Zircaloy-2," AECL-809, 1959.
4. L. M. Howe, "The Annealing of Irradiation Damage in Zircaloy-2 and the Effect of High Temperature Irradiation on the Tensile Properties of Zircaloy-2," AECL-1024, 1960.
5. A. L. Bement, "Effects of Cold Work and Irradiation on the Tensile Properties of Zircaloy-2," HW-74955, April 1963.
6. D. G. Hardy, "The Effects of Neutron Irradiation on the Mechanical Properties of Zirconium Alloy Fuel Cladding in Uniaxial and Biaxial Tests," Irradiation Effects on Structural Alloys for Nuclear Reactor Applications, ASTM STP-484, 1979, pp. 215-258.
7. L. M. Howe and W. R. Thomas, "The Effect of Neutron Irradiation on Tensile Properties of Zircaloy-2," Journal of Nuclear Materials, Vol. 2, No. 3, 1960, pp. 248-260.
8. C. L. Howard, "Fuel Cycle Program - A Boiling Water Reactor Research and Development Program - Thirteenth Progress Report," GEAP-4383, October 1963.
9. H. H. Klepfer and C. N. Spalaris, "Mechanical Behavior of Cold-Worked Nuclear Grade Zircaloy-2 Tubing," Nuclear Metallurgy, Vol. VII (AIME-IMD Special Report Series No. 10), 1960.
10. B. Lustman et al., "Zircaloy Cladding Performs Well in PWR," Nucleonics, Vol. 19, No. 1, January 1961, pp. 58-63.
11. A. R. Kephart, "Review of Selected Physical and Mechanical Properties of Zircaloy-2," Trans. ANS, Vol. 4, No. 2, November 1961.
12. F. H. Megerth, "Zircaloy-Clad UO₂ Fuel Rod Evaluation Program," GEAP-10079, August 1969.
13. D. B. Scott, "Physical and Mechanical Properties of Zircaloy-2 and 4," WCAP-3269-41, May 1965.

REFERENCES (Continued)

14. E. B. Schwenk et al., "Poisson's Ratio in Zircaloy-4 Between 24°C and 316°C," BN-SA-752.
15. A. A. Bauer et al., "Evaluating Strength and Ductility of Irradiated Zircaloy," Quarterly Progress Report, January through March 1976, BMI-NUREG-1948, March 1976.
16. F. H. Megerth et al., "Zircaloy-Clad UO₂ Fuel Rod Evaluation Program Final Report," November 1967 through June 1971, GEAP-10371, June 1971.
17. C. L. Mohr et al., "A State-of-the-Art Report on the Anisotropic Deformation and Analysis of Zircaloy," RP-251-1, March 1976.
18. C. R. Woods, "Properties of Zircaloy-4 Tubing," WAPD-TM-585, December 1966.
19. P. J. Pankaskie, "Irradiation Effects on the Mechanical Properties of Zirconium and Dilute Zirconium Alloys - A Review," BN-SA-618, July 15, 1976.
20. R. W. Nichols and B. Watkins, "Implications of Embrittlement During the Life of a Zirconium Pressure Tube Reactor," Effects of Radiation on Structural Metals, ASTM STP-426, 1967, pp. 371-390.
21. C. D. Williams et al., "Effects of Boiling Water Reactor Irradiation on Tensile Properties of Zircaloy," European Conference on Irradiation Behavior of Fuel Cladding and Core Component Materials, Karlsruhe, December 3-5, 1974.
22. F. J. Azzarto et al., "Unirradiated In-Pile and Post-Irradiation Low Strain Rate Tensile Properties of Zircaloy-4," Journal of Nuclear Materials, Vol. 30, 1969, pp. 208-218.
23. W. R. Smalley, "Effects of Irradiation on Mechanical Properties of CVTR Pressure Tube Material," CVNA-159, September 1962.
24. H. R. Higgy and F. H. Hammand, "Effect of Neutron Irradiation on the Tensile Properties of Zircaloy-2 and Zircaloy-4," Journal of Nuclear Materials, Vol. 44, 1972, pp. 215-227.
25. C. L. Howard, "Fuel Cycle Program - A Boiling Water Reactor Research and Development Program - Thirteenth Quarterly Progress Report - July-September 1963," GEAP-4383, October 1963.

REFERENCES (Continued)

26. W. R. Smalley, "Effects of Irradiation on Mechanical Properties of CVTR Pressure Tube Materials," CVNA-159, 1962.
27. A. A. Bauer et al., "Progress on Evaluating Strength and Ductility of Irradiated Zircaloy,
  - a. July-September 1975," BMI-1938, September 1975,
  - b. January-March 1976," BMI-NUREG 1948, March 1976,
  - c. April-June 1976," BMI-NUREG 1956, June 1976,
  - d. July-September 1976," BMI-NUREG 1961, October 1976,
  - e. April-June 1977," BMI-NUREG 1976, July 1977,
  - f. July-September 1977," BMI-NUREG 1985, October 1977.
28. C. R. Tipton, Jr., ed., Reactor Handbook, Volume I Materials, 2nd ed., Interscience Publishers, Inc., New York, 1960, p. 713.

APPENDIX C

STRESS-RUPTURE OF ZIRCALOY CLADDING

FIGURES

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C-1	Values of Constants in the Relation	c-3
	$\log\left(\frac{\sigma}{K}\right) = a + b\left(\frac{P}{10^4}\right) + c\left(\frac{P}{10^4}\right)^2 + d\left(\frac{P}{10^4}\right)^3$	

STRESS-RUPTURE OF ZIRCALOY CLADDING

Spent fuel rods stored at elevated temperatures are potentially susceptible to failure by stress-rupture since the internal pressure in the rod produces a tensile hoop stress in the cladding wall. The objective of this appendix is to establish a relevant stress-rupture correlation for use in assessing integrity during spent fuel storage (or processing for storage).

Post-irradiation stress-rupture data on pressurized cladding tubes would be the most appropriate information; unfortunately, only pre-irradiation test results appear to be available for Zircaloy-2 and Zircaloy-4, the alloys of interest. Extensive in-reactor creep deformation data are available in support of LWR fuel rod design, but creep failure data on irradiated materials are not available. It will therefore be necessary to assume that unirradiated properties can be used to represent post-irradiation failure. Although substantial irradiation hardening and reduced ductility in short-term tests is known to result from reactor service, it is possible that time-to-rupture in creep tests is not markedly affected since irradiation hardening can lower the creep rate and result in little change in failure times in spite of reduced ductility. Zirconium alloys do not appear to be susceptible to helium embrittlement-type effects as are the stainless steels; therefore, the large irradiation-induced reductions in rupture life of stainless steels may not be typical of Zircaloy fuel cladding.

Within the data base⁽¹⁻⁷⁾ for stress-rupture of unirradiated Zircaloy-2 and Zircaloy-4 there appears to be no clear distinction between stress-rupture behavior of the two alloys, thus single correlations for both alloys are used.

Annealed, cold-worked, and cold-worked and stress-relieved materials are represented in the data base. Annealed material is typical of current BWR cladding while cold-worked or cold-worked and stress-relieved material is more typical of PWR cladding.⁽⁸⁾ Therefore, these two material conditions are treated separately in the correlations.

Both uniaxial and pressurized tube (biaxial) test results are included in the data base, although the latter results are more **representative** of the service application. Uniaxial and biaxial stress-rupture results can generally be correlated based on either maximum principal stress or on effective stress, but the correct choice for zirconium alloys is not known. One reason for uncertainty in the stress-state effect is the complicating influence of texture effects. Anisotropy associated with texture introduced in various processing routes can lead to substantial differences in uniaxial and biaxial properties. In-reactor test results suggest a biaxial stress adjustment factor as low as 0.5,⁽⁹⁾ but creep tests on unirradiated material have suggested that texture effects on creep deformation are **significant** only at high stress (*i.e.*, creep rates $> 1 \times 10^{-4} \text{ hr}^{-1}$).⁽¹⁰⁾ The present data base appears to show a difference between uniaxial and biaxial results on cold-worked or cold-worked and stress-relieved materials; and correlations based on only the biaxial results seem reasonable. However, the data base for annealed materials is much smaller and the difference between uniaxial results (**Zircaloy-2**) and biaxial results (**Zircaloy-4**) is apparently quite large. **It** therefore seems most appropriate to select a correlation based on the uniaxial data for conservatism.

Experimental stress-rupture data were correlated using the Larson-Miller parameter (**P**) given by

$$P = T(20 + \log t) \quad (C-1)$$

where **T** is the temperature in °R and **t** is the rupture time in hours. No attempt was made to optimize the value of the constant, which was set at 20. The relationship between stress (**a**) and **P** is given by

$$\log \sigma = a + bP + cP^2 + dP^3 \quad (C-2)$$

The constants **a**, **b**, **c**, and **d** were evaluated by fitting results from a single set of uniaxial test data obtained over a wide temperature range. Examples of the data **fit** are shown in Figure C-1 for cold-worked and for annealed **Zircaloy-2**.

FIGURE C-1. Polynomial Fit to Individual Sets of Stress-Rupture Data for Zircaloy-2.

The log a - P curve gives a good representation of the stress dependence of other data sets when translated along the log σ axis. Therefore, the constants b , c , and d from the single data set were retained, while values for the constant a were based on all data sets. A summary of the constants is presented in Table C-1.

TABLE C-1

VALUES OF CONSTANTS IN THE RELATION*

$$\log \frac{\sigma}{K} = a + b\left(\frac{P}{10^4}\right) + c\left(\frac{P}{10^4}\right)^2 + d\left(\frac{P}{10^4}\right)^3$$

Condition	Loading Type	Number of Data Points	a	Standard Deviation on a	b	c	d
Cold Worked (With or Without Stress Relief)	Uniaxial	77	7.17213	0.03428	-3.39085	1.65427	-0.284513
	Biaxial	70	7.28010	0.03858	-3.39085	1.65427	-0.284513
Annealed	Uniaxial	22	8.37086	0.02864	-5.19855	2.29955	-0.346344

* $K = 6.894757 \times 10^{-3}$ for σ in MPa and 1.0 for σ in psi.

A correlation for minimum stress-rupture behavior can be defined by using a minimum value of the constant a in Equation (C-2). This minimum value of a is defined as the lower 95%/95% tolerance limit on a . Comparisons of the data base with this minimum stress-rupture correlation are illustrated in Figures C-2 and C-3. Examples of isothermal stress-rupture curves obtained from this correlation are shown in Figure C-4.

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FIGURE C-2. Minimum Stress-Rupture Behavior of Cold-Worked Zircalloys for Uniaxial and Biaxial Loading.

FIGURE C-3. Minimum Stress-Rupture Behavior of Annealed Zircalloys.

FIGURE C-4. Isothermal Minimum Stress-Rupture Curves for Cold-Worked Zircalloys Under Biaxial Loading.

REFERENCES

1. C. R. Tipton, Jr., ed., Reactor Handbook Volume 1 Materials, 2nd ed., Interscience Publishers, Inc., New York, 1960, p. 716.
2. J. A. Van Echo and S. W. Porambka, "Long-Term Creep Rupture of Sintered Aluminum Powder and Zircaloy-2 Alloys," BMI-X-10113, January 1965.
3. R. C. Aungst, "Stress Rupture Tests of Zircaloy-2 Pressure Tubes," BNWL-8, January 1965.
4. B. Watkins and D. S. Wood, "The Significance of Irradiation-Induced Creep on Reactor Performance of a Zircaloy-2 Pressure Tube," Applications-Related Phenomena in Zirconium and Its Alloys, ASTM STP-458, American Society for Testing and Materials, Philadelphia, 1969, p. 226.
5. D. S. Wood and B. Watkins, "A Creep Limit Approach to the Design of Zircaloy-2 Reactor Pressure Tubes at 275°C," Journal of Nuclear Materials, Vol. 41, 1971, p. 327.
6. C. C. Busby, R. P. Tucker, and J. E. McCauley, "~~Halogen~~ Stress Corrosion Cracking of Zircaloy-4 Tubing," Journal of Nuclear Materials, Vol. 55, 1975, p. 64.
7. C. R. Woods, ed., Properties of Zircaloy-4 Tubing, WAPD-TM-585, December 1976, p. 126.
8. D. H. Locke, "Review of Experience with Water Reactor Fuels 1968-1973," Nucl. Eng. and Design, Vol. 33, 1975, p. 94.
9. E. F. Ibrahim, "In-Reactor Creep of Zirconium-Alloy Tubes and Its Correlation with Uniaxial Data," Applications-Related Phenomena in Zirconium and Its Alloys, ASTM STP-458, American Society for Testing and Materials, Philadelphia, 1969, p. 18.
10. V. Fidleris, "The Effect of Texture and Strain Aging on Creep of Zircaloy-2," Applications-Related Phenomena in Zirconium and Its Alloys, ASTM STP-458, American Society for Testing and Materials, Philadelphia, 1969, p. 1.

APPENDIX D

THE POTENTIAL FOR EXTENSION OF FUEL CLADDING
FLAWS UNDER SPENT FUEL STORAGE CONDITIONS

THE POTENTIAL FOR EXTENSION OF FUEL CLADDING FLAWS UNDER SPENT FUEL STORAGE CONDITIONS

One possible mechanism for degradation of spent fuel cladding under storage conditions is the subcritical extension of flaws until cladding wall penetration occurs. Estimation of the flaw extension kinetics is, under the proper conditions, among those problems that can be treated using linear-elastic fracture mechanics (LEFM) analysis techniques. The potential for stress corrosion cracking (SCC), fatigue-crack propagation, and hydrogen-induced delayed cracking will be briefly reviewed, but as will be apparent, only SCC is considered to have any significant potential for occurring under spent fuel storage conditions.

Failure (i.e., wall penetration) could occur through one of two modes: 1) a subcritical flaw extends partway through the wall until the material fracture toughness is exceeded (i.e., a "critical" crack),^{3'} thus leading to rapid crack extension through the remainder of the wall or 2) subcritical flaw extension continues until wall penetration occurs. The fracture toughness (K_{Ic}) of irradiated Zircaloy is sufficiently high at storage temperatures (see Appendix E) that the first possibility seems remote, as illustrated by the calculations in Example D-1. Hence, subcritical flaw extension continuing until wall penetration occurs is the more likely possibility.

LEFM methods have been used to describe three types of subcritical cracking in zirconium-base alloys: fatigue-crack propagation,⁽¹⁻⁵⁾ hydrogen-induced delayed cracking,⁽⁶⁻⁸⁾ and stress corrosion cracking.⁽⁹⁻¹⁴⁾ Although cyclic loadings could be imposed upon spent fuel assemblies during transportation and handling, cyclic loadings would not be anticipated during spent fuel storage with the possible exception of seismic events involving a small number of fatigue cycles. Hence, subcritical fatigue-crack propagation will not be considered further, nor will hydrogen-induced delayed cracking since the crack growth rates observed to date for this phenomenon appear to be considerably lower than those due to SCC under similar conditions (stress intensity factor, temperature, etc.).

EXAMPLE D-1

Calculate the K-level for flaws on the inner surface of a PWR fuel pin.

Assumptions :

Fuel Pin OD = 10.7 mm (0.422 in.), ID = 9.5 mm (0.3734 in.), and thickness (t) = 0.62 mm (0.0243 in.)

Flaw sizes (a = crack depth, 2c = crack length)

1. a = 8×10^{-3} mm (3×10^{-4} in.); a/c = 0.9
2. a = 8×10^{-3} mm (3×10^{-4} in.); a/c = 0.2
3. a = 0.31 mm (0.01215 in.); a/c = 0.2

Temperature = 360°C (680°F)

Typical hoop stress (σ) = 66.2 MPa (9600 psi) and design. hoop stress (σ) = 136.0 MPa (19720 psi)

Typical internal pressure (p) = 8.05 MPa (1168 psi) and design internal pressure (p) = 16.40 MPa (2378 psi)

Equations:

$$K = (\sigma + 1.13P) M_e f(\lambda) \sqrt{\frac{\pi a}{Q}}$$

$$\lambda = [12(1 - \nu^2)]^{0.25} \frac{c}{\sqrt{Rt}}$$

where ν = Poisson's ratio
R = avg pin radius

$$A_m = 0.481\lambda + 0.614 + 0.386 \exp(-1.25\lambda)$$

$$f(\lambda) = \left(A_m - 1\right) \frac{a}{t} + 1$$

$$M_e = \left[M_1 + \left(\sqrt{\frac{Qc}{a}} - M_1 \right) \left(\frac{a}{t} \right)^{\sqrt{\pi}} + \sqrt{\frac{Qc}{a}} (M_2 - 1) \left(\frac{a}{t} \right)^{2\sqrt{\pi}} \right] F_w$$

$$F_w = 1$$

$$Q = 1 + 1.47 \left(\frac{a}{c} \right)^{1.64}$$

$$M_1 = 1.13 - 0.1(f) \quad \text{for } \frac{a}{c} < 1$$

$$M_2 = \sqrt{\frac{\pi}{4}}$$

Results of calculations:

σ , MPa	a, mm	$\frac{a}{c}$	K, MN/m ^{3/2} (psi $\sqrt{\text{in.}}$)
9600	8×10^{-3}	0.9	0.256 (233)
	8×10^{-3}	0.2	0.389 (354)
	0.31	0.2	3.93 (3578)
19720	8×10^{-3}	0.9	0.525 (478)
	8×10^{-3}	0.2	0.797 (726)
	0.31	0.2	8.06 (7343)

LEFM will be employed to evaluate the potential for SCC under spent fuel storage conditions. The application of LEFM methods to SCC problems has been illustrated in three early review articles. (15-17) Basically, the method allows characterization of two quantities: the threshold for initiation of SCC (K_{Isc}) and the rate at which subcritical crack extension proceeds (da/dt). Both of these quantities are characterized in terms of the stress intensity factor (K). If the combination of a given crack size, stress level, and other geometrical considerations results in a value of K that is less than K_{Isc} for that material/environment combination, then SCC should not occur. If on the other hand, the value of K is greater than K_{Isc} , then SCC should occur and the crack growth behavior can, in general, be described by

$$\frac{da}{dt} = C(K)^n \quad (D-1)$$

where C and n are functions of the specific material/environment. The time required to extend the crack from some initial size (a_0) to some greater size (a_1) is given by

$$t = \int_{a_0}^{a_1} dt = \frac{1}{C} \int_{a_0}^{a_1} K^{-n} da \quad (D-2)$$

where the integration, in practice, is usually done numerically.

The application of this procedure to obtain accurate quantitative estimates of fuel assembly SCC lifetimes under storage conditions is presently not possible due to a lack of understanding of the precise SCC mechanism in fuel pins and a lack of experimental data on K_{Isc} and da/dt as a function of K in the proper environment. It may be possible to use the data presently available (although it is generally inadequate) to predict SCC lifetimes for flaws of various initial sizes in fuel assemblies stored under storage conditions, but it must be recognized that such estimates could contain large errors.

The corrosive environment responsible for SCC in fuel pins comes from the fission products generated during irradiation, but the specific agent, or agents, causing SCC has not been positively identified. Iodine and cadmium are most suspect, but other potentially reactive agents that cannot be positively eliminated include tellurium, selenium, palladium, molybdenum, iron, antimony, aluminum, silicon, silver, and chlorine. (18)

Of the two principal suspects, iodine vapor released from solid CsI by gamma radiolysis is the expected aggressive agent. In addition, cadmium should be present in metallic form and is an aggressive agent. (18-20) It is not clear that vapor transport of these agents from the fuel to the cladding continues after removal from the reactor, when fuel temperatures are substantially lower. In addition, gamma ray intensity decays markedly with time after fuel discharge, so radiolysis of CsI during storage may be reduced or stop altogether. Thus, the supply of embrittling agents could be limited in a storage situation.

The threshold for SCC in operating fuel pins has not been determined directly, but Ranjan and Smith⁽¹⁴⁾ have estimated K_{Isc} to be 1.1-3.3 MN/m^{3/2} (1000-3000 psi $\sqrt{\text{in.}}$) based on theoretical modeling and observations on failed fuel pins and in-reactor performance. This estimate is associated with a temperature of ~347°C (657°F), the maximum cladding temperature for PWR fuel. (21) Two estimates of K_{Isc} have been made for iodine vapor SCC of unirradiated Zircaloy based on laboratory tests; a value of 9.9 MN/m^{3/2} (9000 psi $\sqrt{\text{in.}}$) was obtained at 300°C (572°F),⁽¹⁰⁾ and values of 0.39-0.81 MN/m^{3/2} (355-735 psi $\sqrt{\text{in.}}$) were obtained at 360-400°C (680-752°F) depending on the tubing fabrication process. (11) The environment for the laboratory tests may, of course, not be typical of the actual fuel pin internal environment. Nevertheless, if these estimates are accepted, a plausible description of K_{Isc} seems to be

$$K_{Isc} = K_0 \exp \left(\frac{Q}{RT} \right) \quad (D-3)$$

where K_o = a constant equal to $4.94 \times 10^{-7} \text{ MN/m}^{3/2}$ ($4.5 \times 10^{-10} \text{ psi } \sqrt{\text{in.}}$),
 Q = 35,000 calories/mol,
 R = gas constant, and
 T = absolute temperature.

The prediction of Equation (D-3) is compared with experimental estimates in Table D-1.

TABLE D-1
 COMPARISON OF PREDICTED AND EXPERIMENTAL VALUES OF K_{Isc}

Temperature (°C)	K_{Isc} , $\text{MN/m}^{3/2}$ ($\text{psi } \sqrt{\text{in.}}$)	
	Predicted	Experimental
300	11 (10,000)	9.9 (9,000)*
347	1.07 (974)	1.1-3.3 (1000-3000)†
360	0.60 (544)	0.5-0.8 (455-735)**
400	0.11 (104)	0.4-0.7 (355-636)**

*Reference 10
 **Reference 11
 †Reference 14

The value of Q in Equation (D-3) was selected to give the same temperature dependence reported for da/dt (discussed later). Predicted K_{Isc} values generally agree with experimental results.

The laboratory studies with iodine vapor^(10,11) provide information for the evaluation of the constants in Equation (D-2). Thus,

$$\frac{da}{dt} = C K^n \quad (D-4)$$

where $C = 8.17 \times 10^{-9} \exp \left(-\frac{Q}{RT} \right)$ cm/s ($1.93 \times 10^{-7} \exp \left(-\frac{Q}{RT} \right)$ in./min),
 $Q = 35,000$ calories/mol,
 R = gas constant,
 T = absolute temperature, and
 $n = 4$.

When evaluated at 347°C (657°F), this relation gives crack-growth rates that are about one order of magnitude smaller than those required to explain in-reactor SCC failures of operating fuel pins.⁽¹⁸⁾ This difference may arise because the in-reactor failures involve crack growth of irradiated Zircaloy or because the in-reactor SCC environment is somehow different. However, the difference is not particularly important since even the lower crack growth rates predict cladding penetration after short storage times.

A number of sample calculations using the estimates developed above are informative when considering spent fuel storage. The calculations in Example D-1 give K values for three different initial flaw sizes in a specified PWR fuel pin geometry. The two small flaws may be representative of defects in as-fabricated cladding, while the large flaw may be representative of incipient cracks in fuel pins damaged by pellet-cladding interaction during in-reactor service.⁽¹⁸⁾ For both typical and design stress levels, the K -level for the large flaw is above K_{ISCC} ; therefore, crack growth would be expected. For both small flaws, K -levels associated with typical stresses are below K_{ISCC} . However, for design stresses, only the semicircular flaw gives a K -level below K_{ISCC} . The elongated small flaw would be expected to grow at design stress levels. In all cases, the K -levels are well below the K_{IC} values required for rapid, unstable crack propagation (see Figure E-1).

The calculations in Example D-2 illustrate the growth of the large flaw in Example D-1. The results indicate that penetration of the cladding should occur within 3 hr at typical stress levels. A much shorter time would be predicted for design stress levels. Other calculations suggest that even for the smallest flaw of Example D-1, it would hypothetically take only ~700 hr

EXAMPLE D-2

Calculate the growth of the large flaw of Example D-1 for typical stress level.

Assumptions:

Same as for Example D-1; crack length does not increase.

Equations:

Same as for Example D-1 and

$$\frac{da}{dt} = C_0 \exp\left(-\frac{Q}{RT}\right) K^4$$

$$\Delta t_i = \frac{\Delta a}{\frac{da}{dt}}$$

$$\Delta t_c = \sum_i \Delta t_i$$

Results of the calculations:

Increment, i	$Aa, \text{ mm}$	a_{avg}	$\frac{a_{avg}}{t}$	$\frac{a_{avg}}{c}$	M_I	Q
1	0.0617	0.3396	0.55	0.22	1.108	1.123
2	0.0613	0.4013	0.65	0.26	1.104	1.161
3	0.0617	0.4630	0.75	0.30	1.100	1.204
4	0.0617	0.5248	0.85	0.34	1.096	1.251
5	0.0617	0.5865	0.95	0.38	1.092	1.301

Increment, i	$f(\lambda)$	M_e	$\sqrt{\frac{\pi a}{Q}}, \text{ mm}^{1/2}$	$\sigma + 1.13P, \text{ MPa}$	$K, \text{ MN/m}^{3/2}$
1	1.237	1.476	0.9747	75.3	4.23
2	1.280	1.522	1.0417	75.3	4.83
3	1.323	1.561	1.0992	75.3	5.40
4	1.366	1.590	1.1481	75.3	5.93
5	1.409	1.608	1.1899	75.3	6.41

Increment, i	$\frac{da}{dt}, \text{ mm/s}$	$\Delta t_i, \text{ s}$	$\Delta t_c, \text{ s (hr)}$
1	1.49 E-5	4128	4128 (1.15)
2	2.53 E-5	2442	6570 (1.83)
3	3.96 E-5	1560	8130 (2.26)
4	5.76 E-5	1074	9204 (2.57)
5	7.87 E-5	786	9990 (2.76)

for wall penetration at the design stress level which corresponds to a K-level below K_{Isc} . The estimated value of K_{Isc} , even though it is quite low, thus may provide the only protection against fairly early failures at elevated temperatures.

The calculations in Examples D-1 and D-2 do point out the possibility for SCC under spent fuel storage conditions. They also point out the lack of definitive information regarding SCC degradation mechanisms, threshold levels, and flaw extension kinetics. It is clear that considerable additional work is required in these areas before the subject of SCC can be accurately assessed.

REFERENCES

1. L. A. James, "Crack Propagation Behavior of Zircaloy-2," Nuclear Applications, Vol. 6, No. 4, 1969, pp. 307-312.
2. L. A. James, "Environmentally Aggravated Fatigue Cracking of Zircaloy-2," Nuclear Applications and Technology, Vol. 9, 1970, pp. 260-267.
3. B. W. Pickles and E. R. Johnson, "Pressure Cycling Tests on SGHWR Pressure Tubing," Journal of the British Nuclear Energy Society, Vol. 12, No. 1, 1973, pp. 47-56.
4. S. Kusumoto, A. Nishioka, H. Maki, S. Usami, K. Hayashi, and Y. Ando, "Effects of Hydrogen Content, Temperature, and Crack Configuration on Fatigue Crack Propagation and Unstable Fracture Behavior of Zr-2.5Nb Pressure Tube," 2nd International Conference on Structural Mechanics in Reactor Technology, Paper F2/3, 1973.
5. B. J. S. Wilkins and A. R. Reich, "Probabilistic Aspects of Fatigue Crack Propagation Data for Zirconium-2.5% Niobium," AECL-5529, Atomic Energy of Canada Limited, 1976.
6. L. A. Simpson and K. Nuttall, "On the Factors Controlling Hydrogen-Assisted Subcritical Crack Growth in Zr-2.5Nb Alloys," ASTM Symposium on Zirconium in the Nuclear Industry, Quebec City, August 1976 (to be published in an ASTM STP).
7. K. Nuttall, "Slow Crack Growth in Heat-Treated Zr-2.5 Wt% Nb Alloys," Scripta Metallurgica, Vol. 11, 1977, pp. 55-58.
8. R. Dutton, K. Nuttall, M. P. Puls, and L. A. Simpson, "Mechanisms of Hydrogen-Induced Delayed Cracking in Hydride Forming Materials," Metallurgical Transactions, Vol. 8A, No. 10, 1977, pp. 1553-1562.
9. J. C. Wood, "Factors Affecting Stress Corrosion Cracking of Zircaloy in Iodine Vapor," Journal of Nuclear Materials, Vol. 45, No. 2, 1972-1973, pp. 105-122.
10. B. Cox, "Environmentally Induced Cracking of Zirconium Alloys," Reviews on Coatings and Corrosion, Vol. 1, No. 4, 1975, pp. 367-422.
11. P. H. Kreyens, G. L. Spahn, and J. E. McCauley, "An Analysis of Iodine Stress Corrosion Cracking of Zircaloy-4 Tubing," Journal of Nuclear Materials, Vol. 61, 1976, pp. 203-212.

REFERENCES (Continued)

12. R. P. Tucker, P. H. Kreyns, and J. J. Kearns, "The Effects of Surface Flaws, Iodine Concentration, and Temperature on the Stress Corrosion Cracking Behavior of Zircaloy-4 Tubing," WAPD-TM-1248, Bettis Atomic Power Laboratory, 1976.
13. M. O. Speidel, "Stress Corrosion Cracking of Zirconium Alloy," preliminary draft of a chapter prepared for the forthcoming ARPA Handbook, Stress Corrosion Cracking (not yet publically available).
14. G. V. Ranjan and E. Smith, "Fracture of Zircaloy Cladding as a Consequence of Power Increases in Water Reactor Fuel Rods," Paper C2/1, 4th International Conference on Structural Mechanics in Reactor Technology, August 15-19, 1977, San Francisco.
15. B. F. Brown, "The Application of Fracture Mechanics to Stress Corrosion Cracking," Metallurgical Reviews, Vol. 13, 1968, pp. 171-183.
16. H. H. Johnson and P. C. Paris, "Subcritical Flaw Growth," Engineering Fracture Mechanics, Vol. 1, No. 1, 1968, pp. 3-45.
17. S. R. Novak and S. T. Rolfe, "Comparison of Fracture Mechanics and Nominal Stress Analyses in Stress Corrosion Cracking," Corrosion, Vol. 26, 1970, pp. 121-130.
18. J. T. A. Roberts, E. Smith, N. Fuhrman, and D. Cubicciotti, "On the Pellet-Cladding Interaction Phenomenon," Nuclear Technology, Vol. 35, 1977, pp. 131-144.
19. D. Cubicciotti and J. H. Davies, "Release of Iodine from Iodide Salts by Gamma Radiolysis," Nuclear Science and Engineering, Vol. 60, 1976, p. 314.
20. W. T. Grubb, "Cadmium Metal Embrittlement of Zircaloy-2," Nature, Vol. 265, 1977, p. 36.
21. Nuclear Engineering International, Vol. 22, No. 258, April 1977 Supplement.

APPENDIX E

PLANE STRAIN FRACTURE TOUGHNESS OF
ZIRCALOY-2 AND ZIRCALOY-4

FIGURES

<u>Number</u>		<u>Page</u>
E-1	Plane Strain Fracture Toughness of Annealed Zircaloy-2 and Zircaloy-4 for Cracks Propagating Transverse to the Rolling Direction.	E-1
E-2	Plane Strain Fracture Toughness of Annealed Zircaloy-2 and Zircaloy-4 for Cracks Propagating Transverse to the Rolling Direction.	E-2

PLANE STRAIN FRACTURE TOUGHNESS OF ZIRCALOY-2 AND ZIRCALOY-4

Plots of plane strain fracture toughness (K_{IC}) as a function of test temperature and neutron fluence obtained from the literature⁽¹⁻⁵⁾ are shown in Figures E-1 and E-2. These results are for cracks propagating transverse to the rolling direction in annealed, unhydrided Zircaloy-2 and Zircaloy-4, which Walker and Kass⁽²⁾ found to be the orientation that exhibits the minimum toughness in unirradiated Zircaloy-4 material. The line drawn below all the test data in the figures represents the minimum expected fracture toughness of Zircaloy-2 and Zircaloy-4 for initially annealed, unhydrided material out to the expected goal fluences for spent fuel cladding.

FIGURE E-1. Plane Strain Fracture Toughness of Annealed Zircaloy-2 and Zircaloy-4 for Cracks Propagating Transverse to the Rolling Direction.

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FIGURE E-2. Plane Strain Fracture Toughness of Annealed Zircaloy-2 and Zircaloy-4 for Cracks Propagating Transverse to the Rolling Direction.

REFERENCES

1. R. W. Nichols and B. Watkins, "Implications of Embrittlement During the Life of a Zirconium Pressure Tube Reactor," Effects of Radiation on Structural Metals, ASTM STP-426, 1967, pp. 371-390.
2. T. J. Walker and J. N. Kass, "Variation of Zircaloy Fracture Toughness with Irradiation," Zirconium in Nuclear Applications, ASTM STP-551, 1974, pp. 328-354.
3. R. G. Rowe and R. G. Hoagland, "The Effect of Cold Work, Thermal Transient, and Neutron Irradiation on the Fracture Toughness of Zircaloy-2," Irradiation Effects in Structural Alloys for Thermal and Fast Reactors, ASTM STP-457, 1969, pp. 3-16.
4. R. G. Hoagland, A. L. Bement, and R. G. Rowe, "Applications of Fracture Mechanics in Evaluating Initiation and Propagation of Brittle Fracture in Reactor Structural Components," Effects of Radiation on Structural Metals, ASTM STP-426, 1967, pp. 328-345.
5. R. G. Hoagland and R. G. Rowe, "Fracture of Irradiated Zircaloy-2," Journal of Nuclear Materials, Vol. 30, Nos. 1 and 2, 1969, pp. 179-195.

APPENDIX F

INTERNAL HYDRIDING OF CLADDING

INTERNAL HYDRIDING OF CLADDING

Internal hydriding of Zircaloy cladding has been a primary mechanism of fuel failures in operating LWR fuel rods.⁽¹⁻³⁾ This appendix explores internal hydriding from the standpoint of spent fuel storage.

Defects arising from internal hydriding are characterized by bulges or blisters on the external clad surface. These bulges result from the large volume increase accompanying hydriding of zirconium. Such hydride defects, commonly known as "sunbursts," can be large and constitute a defect; or they may be small and not lead to cladding penetration. The more massive defects can give rise to penetrating hydride needles; and cracking can occur, probably triggered by reactor power changes. In the spent fuel storage situation, it seems unlikely that gas pressure stresses would be high enough to crack hydrided cladding, particularly since hydride ductility increases as temperature (and, therefore, stress) increases.

Hydride defects generally, but not exclusively, arise early in fuel operation. Thus, if primary hydriding is going to produce defected fuel rods, such failures probably will occur in service before storage. The source of hydrogen for hydride defects has been small quantities of H_2O introduced into the fuel rod in the UO_2 . However, recent analysis of fuel performance for rods manufactured since 1972 indicates that the internal hydriding failure mechanism has been eliminated in BWR fuel through changes in fuel rod design and improved manufacturing processes.⁽³⁾ If these remedies are successful in eliminating primary hydriding failures in early service, presumably they also eliminate the potential for clad damage leading to failures in later reactor cycles. Thus, there would appear to be a definite limitation on hydrogen available for hydriding or cladding damage during storage.

Although hydride defects generally arise early in service, they can occur at any later stage. Hydrogen can diffuse out through the cladding, reconvertng the inner surface to zirconium with cladding damage in the form of cracks and laminations. This residual damage may lead to later failure under specific stress situations. For spent fuel storage, such flaws should be considered in a linear-elastic fracture mechanics analysis as discussed in Appendix D. Based on sample calculations (Appendix D), it would not appear that gas pressure loading during storage is high enough to cause a fracture problem even with rather large flaws (e.g., halfway through the clad wall).

These initial considerations generally dictate against hydriding as an active degradation mechanism in spent fuel storage.

REFERENCES

1. D. H. Locke, "Review of Experience with Water Reactor Fuels 1968-1978," Nucl. Eng. and Design, Vol. 33, 1975, p. 94.
2. D. O. Pickman, "Internal Cladding Corrosion Effects," Nucl. Eng. and Design, Vol. 33, 1975, p. 141.
3. R. A. Proebstle, W. E. Baily, and H. H. Klepfer, "Current Trends in BWR Fuel Performance," presented at ANS St. Charles Fuel Performance Meeting, May 1977.

APPENDIX G

OXIDATION OF ZIRCALOY CLADDING

OXIDATION OF ZIRCALOY CLADDING

The initial baseline case for spent fuel storage involves placing spent fuel assemblies in sealed canisters filled with helium gas. **Oxidation** of the Zircaloy cladding external surface by impurity oxygen in the helium is a potential degradation mechanism. The following calculations address the potential for extensive oxidation.

Canister Geometry and Dimensions: Cylinder 43.2-cm (17-in.) ID, 4.88-m (16-ft) long.

Canister Internal Volume:

$$\frac{\pi [(17)(2.54)]^2 (16)(12)(2.54)}{4} = 714151 \text{ cm}^3 \quad (\text{G-1})$$

Volume of PWR fuel rods:

$$\frac{(14 \times 14)(0.422)^2(2.54)^2 \pi (15)(12)(2.54)}{4} = 80862 \text{ cm}^3 \quad (\text{G-2})$$

$$\frac{(17 \times 17)(0.374)^2(2.54)^2 \pi (15)(12)(2.54)}{4} = 93649 \text{ cm}^3 \quad (\text{G-3})$$

Volume of BWR fuel rods:

$$\frac{(7 \times 7)(0.563)^2(2.54)^2 \pi (171)(2.54)}{4} = 34182 \text{ cm}^3 \quad (\text{G-4})$$

$$\frac{(8 \times 8)(0.493)^2(2.54)^2 \pi (176)(2.54)}{4} = 35235 \text{ cm}^3 \quad (\text{G-5})$$

Maximum canister gas volume for a canister containing one PWR assembly or three BWR assemblies:

$$\text{PWR} \quad V_{\max} = 714151 - 80862 = 633289 \text{ cm}^3 \quad (\text{G-6})$$

$$\text{BWR} \quad V_{\max} = 714151 - (3)(34182) = 611605 \text{ cm}^3 \quad (\text{G-7})$$

The average $V_{\max}$ is:

$$V_{\max} = 6.22 \times 10^5 \text{ cm}^3 \quad (\text{G-8})$$

Assume that the canister is filled with impure helium to a pressure of 0.138 MPa (20 psia). The quantity of gas in the canister is then

$$n = \left[\frac{20}{14.7} \right] \left[\frac{(6.22 \times 10^5)}{(82.057)(298)} \right] = 34.61 \text{ gram atoms} \quad (\text{G-9})$$

Let the percent O_2 in the helium be y . Then:

$ny(32)/100$ = The weight of O_2 in the canister,

$ny(32)(91.22)/(100)(32)$ = The weight of zirconium that can react with all the O_2 ,

p = The density of zirconium,

$ny(91.22)/100p$ = The volume of zirconium that can react with the O_2 ,

r_o = The external radius of the original cladding,

r_1 = The radius of unoxidized zirconium in the oxidized cladding,

l = The length of the fuel rod,

N = The number of fuel rods, and

t = The original cladding wall thickness.

Then

$$l(\pi r_0^2 - \pi r_1^2) = \frac{ny(91.22)}{100 p N} \quad (G-10)$$

$$r_1^2 = r_0^2 - \frac{ny(91.22)}{100 p N \pi} \quad (G-11)$$

Taking $p = 6.5 \text{ g/cm}^3$,
 $l = (176)(2.54) \text{ cm}$, and
 $N = (49)(3) \text{ for BWR}$
 196 for PWR

gives the following results:

<u>Y</u> <u>(%)</u>	<u>Reactor</u> <u>Type</u>	<u>$2r_0$</u> <u>(mm)</u>	<u>$r_0 - r_1$</u> <u>(mm)</u>	<u>t</u> <u>(mm)</u>
1	PWR	9.5	0.00018	0.57
		10.7	0.00018	0.62
	BWR	12.5	0.00018	0.86
		14.3	0.00018	0.81
2	PWR	9.5	0.00038	0.57
		10.7	0.00033	0.62
	BWR	12.5	0.00038	0.86
		14.3	0.00033	0.81
5	PWR	9.5	0.00094	0.57
		10.7	0.00081	0.62
	BWR	12.5	0.00094	0.86
		14.3	0.00081	0.81

These estimates show that uniform oxidation should be negligible, even at fairly high oxygen impurity levels. However, factors such as temperature gradients along and between fuel rods could lead to nonuniform oxidation because of the strong temperature dependence of reaction. As a hypothetical example, if all the oxygen reacted with only one fuel rod (i.e., $N = 1$), the following results would be obtained.

Y (%)	Reactor Type	$2r_o$ (mm)	$r_o - r_i$ (mm)	t (mm)
1	PWR	9.5	0.0366	0.57
		10.7	0.0324	0.62
	BWR	12.5	0.0277	0.86
		14.3	0.0242	0.81
2	PWR	9.5	0.0734	0.57
		10.7	0.0649	0.62
	BWR	12.5	0.0555	0.86
		14.3	0.0485	0.81
5	PWR	9.5	0.1857	0.57
		10.7	0.1638	0.62
	BWR	12.5	0.1397	0.86
		14.3	0.1220	0.81

In this example oxidation can be more important, resulting in as much as a 30% reduction in wall thickness for PWR cladding at a 5% O_2 impurity level. To assure that nonuniform oxidation is not a problem, it seems appropriate to specify maximum O_2 levels below 1-2% in filled canisters.

Standard free energies of formation (ΔF_f°) of UO_2 and ZrO_2 over the temperature range 300-500°C (572-932°F) indicate that UO_2 has a slightly more negative ΔF_f° ; ⁽¹⁾ therefore, zirconium will not be oxidized by the UO_2 at the internal cladding surface. However, zirconium can be oxidized by hyperstoichiometric UO_2 . This oxidation can also affect hydriding of Zircaloy as discussed by Proebstle et al. ⁽²⁾ Oxidation of the inner surface of Zircaloy cladding does not appear to be a problem during reactor operation, presumably because the oxygen supply (i.e., difference between any starting hyperstoichiometric composition and the stoichiometric composition of the fuel) is limited. There is no reason to expect that such oxidation should be a serious degradation mechanism during spent fuel storage.

REFERENCES

1. O. Kubaschewski, E. L. Evans, and C. B. Alcock, Metallurgical Thermochemistry, Pergamon Press, Oxford, 1967, pp. 428-429.
2. R. A. Proebstle, W. E. Bailey, and H. H. Klepter, "Current Trends in BWR Fuel Performance," presented at ANS St. Charles Fuel Performance Meeting, May 1977.

APPENDIX H

CREEP DAMAGE CALCULATIONS

CREEP DAMAGE CALCULATIONS

Creep damage calculations for variable temperature histories were based on the following system of equations:

$$D = \int_0^t \frac{dt}{t_d} \quad (H-1)$$

$$\log_{10} t_d = \frac{P}{T_R} - 20 \quad (H-2)$$

$$\log\left(\frac{\sigma}{m}\right) = a + b\left(\frac{P}{10^4}\right) + c\left(\frac{P}{10^4}\right)^2 + d\left(\frac{P}{10^4}\right)^3 \quad (H-3)$$

$$\sigma = C_1(p_f - p_{can}) - p_{can} \quad (H-4)$$

$$p_f = p_{fo} \left(\frac{T_f}{T_{fo}} \right) \quad (H-5)$$

$$p_{can} = p_{can_o} \frac{T_{can}}{T_{can_o}} \quad (H-6)$$

$$T_f = T_s + \frac{Q}{Q_o} (T_{fi} - T_s) \quad (H-7)$$

where: D = Creep damage,
 t = **Time** in storage,
 t_d = Design allowable life,
 P = Larson Miller Parameter,
 T_R = Absolute temperature in $^{\circ}R$,
 σ = Cladding hoop stress;
 m = A design margin on rupture stress = 2/3

$a = 7.191405$ (see Appendix C),
 $b = -3.39085$ (see Appendix C) ,
 $c = 1.65427$ (see Appendix C),
 $d = -0.284513$ (see Appendix C) ,
 $C_1 = A$ coefficient based on the dimensions of fuel rod = 8.327
 (see Appendix A),
 p_f = Internal pressure in fuel rod,
 p_{can} = Internal pressure in canister,
 p_{f0} = Reference pressure in fuel rod = 7.72 MPa (1120 psi),
 T_{f0} = Reference temperature in fuel rod = 298K (537°R),
 T_f = Fuel rod temperature,
 $p_{can,0}$ = Reference pressure in canister = 0.103 MPa (15 psi),
 $T_{can,0}$ = Reference temperature in canister = 298K (537°R),
 T_{can} = Canister temperature,
 T_s = Effective heat sink temperature = 18°C (65°F),
 T_{fi} = Initial fuel cladding temperature (arbitrarily selected),
 Q = Time-varying decay heat generated in fuel, ⁽¹⁾ and
 Q_0 = Decay heat in fuel 5 years after reactor discharge, ⁽¹⁾ =
 0.964 kW/assembly.

A model defined by the above system of equations was written in the Advanced Continuous Simulation Language (ACSL). ⁽²⁾ All integration in an ACSL program is handled by a centralized integration routine that allows the user a choice of four integration algorithms. The Runge-Kutta fourth-order algorithm was used in this model.

REFERENCES

1. Spent Unreprocessed Fuel Facility Engineering Studies, RHO-LD-2, February 1978.
2. Advanced Continuous Simulation Language (ACSL), User Guide/Reference Manual, Mitchell and Gauthier Associates, 1975, Concord, MA.