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SUGGESTED INTERIM STRUCTURAL
DESIGN CRITERIA
FOR THE FTR PRIMARY PIPING SYSTEM

December 1969

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SUGGESTED INTERIM STRUCTURAL DESIGN CRITERIA
FOR THE FTR PRIMARY PIPING SYSTEM

By

Marlyn T. Jakub et al.

December 1969

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SUGGESTED INTERIM STRUCTURAL DESIGN CRITERIA FOR THE FTR
PRIMARY PIPING SYSTEM

Marlyn T. Jakub et al.

ABSTRACT

This report contains a set of structural design criteria for the primary system piping of the Fast Test Reactor (FFTF). This piping is to provide a 20-year-life, thin-walled, type 304H SS conduit for 1200 °F liquid sodium at pressures of ~300 psi. This report, as the official minutes of the meeting of AEC and industry experts who formulated the criteria, also records the background discussion and reasoning. Future development needs for design with materials at creep temperatures are outlined.

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SUGGESTED INTERIM STRUCTURAL DESIGN CRITERIA FOR THE FTR
PRIMARY PIPING SYSTEM

Marlyn T. Jakub

INTRODUCTION

To assist in the formulation of appropriate interim design criteria for the piping, BNW invited a group of experts to a one-day meeting at Richland, Washington on May 6, 1969. This report is the official minutes of that meeting.

Schedules at that time indicated that Bechtel would initiate FFTF primary system layout design on about July 1, 1969. At that time, Bechtel must have interim structural design criteria for the piping. The appropriate design criteria were to be issued to Bechtel by Westinghouse. These criteria would represent a significant step in piping design because the metal temperatures go into the creep range of Type 304 SS material.

Attendees

BNW/FFTF	M. T. Jakub, R. A. (Dick) Moen
WARD	Alfred Snow
Bechtel	Fred Vinson, Byron H. Leonard
Teledyne (TMR)	W. E. (Bill) Cooper, Don F. Landers
C. F. Braun	Lee E. Wright
UNC	R. J. (Dick) Slember
AEC-RDT	L. E. (Les) Alsager
LMEC	Ernest N. Pearson
BMI	E. C. (Ev) Rodabaugh

Other Richland-based personnel attended as observers.

CONCLUSIONS

The participants agreed to the criteria listed below as appropriate interim design criteria for the FTR primary piping system. The assumption was that the piping system will use Type 304H austenitic stainless steel to contain 1200 °F liquid sodium.

1. Primary stress limits will involve the following allowable stress intensities:
 - S_m = design limit; a fixed number based on standard E31.7 criteria (Reference 1)
 - S_{ms} = operating long-term limit based on the life fraction rule and the "x" hour life under a given operating condition. For temperatures below 800 °F, S_{ms} is equivalent to S_m .
 - S_{mt} = Upset conditions limit based on 90% of the yield strength (0.27% total strain) from low-strain-rate tensile tests. For temperatures below 800 °F, S_{ms} is equivalent to S_m .
 - S_{ms} is the lowest of: (a) 60% of the stress-to produce-rupture in "x" hours based on average data, (b) 80% of the stress-to produce-rupture in "x" hours based on minimum data, and (c) 100% of the average stress-to-produce a total creep strain of 1% in "x" hours.
2. Primary-bending-plus-primary-membrane stress intensities will follow E31.7 and the associated elevated temperature code case.
3. Primary-plus-secondary stress intensities will follow the rules of 331.7 and the associated elevated temperature code case. The limit curve shall be $3S_{mt}$ for elevated temperatures. Exceeding the $3S_{mt}$ limit for primary-plus-secondary stress intensities is acceptable on an interim basis by use of E31.7 Simplified Elastic-Plastic Analysis Method.

However, the total number of such cycles of a given type shall be 25 or less. Acceptance of this criterion will be re-examined through additional studies and by actual cyclic stress-strain data at temperature.

4. Primary-plus-secondary-plus-peak stress intensities are evaluated as in B31.7.
5. The linear damage equation $\sum_{i=1}^K \left(\frac{t_i}{t_{ri}} + \frac{N_i}{N_{fi}} \right) \leq 1$, shall be used for creep-fatigue analysis. The first term, which relates to time-to-rupture, considers primary stresses including the effect of local structural discontinuities on the primary membrane and bending stresses. However, secondary stresses resulting from both gross structural discontinuities and temperature distributions are disregarded. All load sources are considered for the second term, consistent with the fatigue analysis method of B31.7.
6. B31.7 and the associated code cases shall be used as a guideline in all other areas. For example, Tresca (or Modified Tresca) method shall be used to relate uniaxial data to multiaxial loadings in elastic analysis.

DISCUSSION OF BACKGROUND

M. T. Jakub opened the May 6 meeting with a review of the interim design criteria adopted for the FFTF reactor vessel. A brief summary is given in Appendix C of this report. Mr. Jakub reported that, except for one additional creep-fatigue rule, the vessel will be designed by elastic analysis and compared to tensile materials data by means of the usual S_m stress intensities of Section III of the ASME Boiler Code (Reference 2). In his opinion, the important differences between the piping code criteria and the vessel criteria were as follows:

- The vessel criteria assumes that the vessel will not be exposed to temperatures above 1000 °F whereas the piping will be designed for 1200 °F sodium. The extra 200 °F should be considered as creep range temperatures for Type 304 austenitic stainless steel ($0.49 \leq T/T_{\text{melt}} \leq 0.56$).
- The vessel wall under normal and upset conditions is not expected to be exposed to severe transient or steady state thermal gradients. Thermal ratchetting is thus minimized in the thermally lined vessel whereas the unlined pipe sees significant transient thermal gradients even under normal and upset conditions.
- Shakedown should occur in the vessel; but in the hot leg piping, the creep range temperatures will require a design procedure which allows for continuing inelastic behavior.

The key design features of the FTR primary hot leg piping were discussed briefly. As the FFTF project nears completion of conceptual design, the design features are as follows (tentative data):

Sizes of pipe (hot leg)	30 in. OD, 20 in. OD, 26 in. OD with up to 3/4 in. wall thickness
Material	Type 304 SS, H grade
Fluid	High-purity sodium
System pressure at pump discharge	~300 psi
Maximum Sodium Temperature	1200 °F
Thermal Shock, Emergency	-30 °F/sec for 4 sec -300 °F in 20 sec
Thermal Shock, Upset	-2.5 °F/sec for 50 sec -260 °F in 180 sec
Sodium Hammer	~450 psig
Design Life	20 years at 75% plant factor (plant factor at 1200 °F would be less than 75%)

MATERIALS ASSUMPTIONS

1. Pipe wall material is assumed to be uniform and homogeneous with properties that are similar to those of small test specimens.

This assumption eliminates material defects from direct consideration in the design process.

2. The anticipated low levels of irradiation of the pipe material will not change the structural properties adversely.

In the reactor vessel criteria, structural material is not expected to be degraded as long as temperatures do not exceed 1000 °F and the residual total tensile test elongation is at least 10%. In the primary piping, inlet downcomer pipes receive the highest-fluence (almost as high as the vessel wall), but temperatures do not exceed 900 °F. Although the hot leg portion of the pipe is exposed to temperatures exceeding 1000 °F, the fluence level is greatly reduced from that of the vessel wall. In this pipe, the above assumption implies that the higher temperatures are offset by the reduced fluence -- leaving the structural material at full design strength.

3. The low-oxygen sodium environment will not change the structural properties adversely.

Surveillance tests will check the validity of this assumption. If accidents do lead to short-term exposure to high-oxygen sodium, then the operators must reassess plant availability. Rules should exist for this procedure -- but the formulation of such rules was outside the scope of this meeting.

4. Corrosion of the pipe material will affect, at most, one-tenth inch of surface material. Corrosion phenomena are not expected to degrade the structural properties of the rest of the pipe wall.

Dick Slember estimated that the corrosion penetration in FTR piping would be 30 mils or less during the 20 year plant life. Analytical models exist for the corrosion.

~~The above assumption also implied that the strength elements, carbon and nitrogen, will not be leached from the material during its lifetime.~~ UNC personnel had performed a diffusion calculation on a 1-in. pipe wall exposed to pure sodium fluid. After 30 years, the surface layer was calculated to have lost much of its original 0.1% carbon, but 70% of the wall still retained greater than the minimum acceptable carbon level for Type 304H SS, 0.04%. The nitrogen may be leached from the outer pipe wall material as a result of the gas environment; however, no data were available.

5. Weld zone material will provide the least acceptable structural properties.

There was concurrence that the weld zone material required special attention since the problems of creep-fatigue design are further complicated by the material complexity of the weld region. Extensive test data are needed to adequately define a creep concept for the weld region. Errors from testing the weld zone material are expected to exceed the errors from all other material assumptions.

6. Pipe material is isotropic.

Ev Rodabaugh explained that previous pipe material tests show little anisotropy. W. Cooper concurred and stated that pipe material is expected to be isotropic. Alsager pointed out that anisotropy may occur where concentrated loads occur in the short transverse direction of the pipe wall [e.g. support pads].

7. Plastic flow and creep are constant volume processes.

No one could recall when this assumption was invalid. The few checks that had been made in the past had confirmed this assumption.

MINIMUM FAILURE POINTS OF MATERIAL AT CREEP TEMPERATURES

M. T. Jakub started the discussion by proposing that all failure points be described in terms of total strain. Section III of the vessel code uses strain theory and fictitious stresses for both "fatigue" and primary-plus-secondary" criteria. Perhaps design at creep range temperatures left no other reasonable alternative but to use strain concepts for all criteria. As an example he explained how the creep-rupture criterion could be the strain at onset of third stage creep. This strain would then depend on temperature and strain rate (i.e., the loading). His method of obtaining low estimates of this strain was to use some of Manjoine's work⁽³⁾ for primary creep strain, then use the product of minimum secondary creep rate times one-half the time-to rupture on ~10,000 hour tests for the secondary creep strain. As strain rates increased, the "failure" strain would eventually reach the uniform elongation value in tensile tests.

The above viewpoint was rejected in favor of the combined strain and stress criteria of the present nuclear codes. Since the rationale for this decision may not be obvious to those familiar with creep analysis, some of the reasoning and comments are mentioned in the following paragraphs:

Several members pointed out that strain bases were the obvious goal of the codes, yet the lack of information and consistent data means that only slow evolution seems possible toward a complete strain bases method.

Dick Slember stated that he was once in favor of establishing failure points at the start of third stage creep. Yet he found that creep curves and the times for start of third stage creep had too much scatter. Data were sensitive to any small variations that occurred in the fabrication procedure.

M. Jakub pointed out several material data difficulties such as: measuring primary creep strains, obtaining strains at start of tertiary creep, uniform strain on failed specimens, and the need to perform tests that didn't always go to failure. He mentioned that the major long-time creep tests, which were run at Babcock and Wilcox, are currently being re-evaluated for data near the initiation of third stage creep.⁽⁴⁾

Bill Cooper stated that the only long-term materials data, come from creep-rupture tests. At present, the usual recorded data are: minimum secondary creep rate, time-to-rupture, load, and total elongation. He would like to see total strain history as well as necking data. The test times have exceeded 10,000 hours on only a couple of dozen tests in the U.S. -- with the longest Type 304 SS test at ~30,000 hours. The Swiss data out to 50,000 hours was on Type 304-like material; but the originator of the tests, Dr. Robert Widmer, has told Bill that the test results are not applicable to Type 304 SS.

Even though the codes seem to be tending toward strain criteria for design in the creep range, builders of critical structures today must utilize proven procedures, available data and applicable experience. This philosophy was adopted by the members as appropriate for primary piping in the FTR.

PRIMARY STRESS CRITERIA

Everyone at the meeting recognized that somehow the problems of creep design must differentiate between long-term phenomena which involve creep strains, and short-term transients, which depend only upon tensile data. The vessel code also points the way by establishing "normal" and "upset" load categories. The design method finally adopted is a modification of the work by Dick Slember (UNC) and John Soehrens (C. F. Fraun) on a breakdown method for primary and secondary stresses.* The primary

* *Braun and UNC are developing a piping design guide for liquid-metal-cooled reactor service for the AEC.*

stress intensities now have three criteria to meet instead of the one S_m of present nuclear codes.

1. DESIGN MEMBRANE STRESS INTENSITY, S

This will be a number depending upon material and operating temperature; no time dependence is implied. The number would correspond closely to the present S_m in nuclear codes and be used for design conditions [solid lines of Figure N-414 in Reference 2.] With materials in the creep range temperatures, transients stresses and other secondary and peak stresses are no longer compared to S_m or multiples of S_m but are compared to the S_{ms} and S_{mt} values discussed below.

Several attending members pointed out that the vessel and piping code committees will always require a so-called "system design pressure," "system design temperature," and "allowable stress for the system." The S_m fulfills the latter role.

As in 831.7 and Section III, the S_m is the lowest of:

- 1/3 of specified minimum tensile strength at room temperature
- 1/3 of the ultimate strength* at temperature
- 90% of the yield strength* at temperature but not above 2/3 of specified minimum room temperature yield strength
- 80% of the minimum 10^5 hour stress-to-rupture at temperature
- ⁶⁷60% of the average 10^5 hour stress-to-rupture at temperature
- Average stress to cause secondary creep strain rate of 0.01% per 1000 hours.

* *These properties are listed in Section III. The listed values of yield strength are obtained from best-fit trend curves (normalized to tensile yield) multiplied by the minimum yield of the ASIM material specification. The same is true of ultimate strengths except that the values are increased by 10% in order to maintain the same design values as before. The factor for S_m is closer to 1/2.7 rather than 1/3.*

R. A. Moen objected to the 80% of the minimum stress-to-rupture stress criterion when dealing with Type 304 SS because of the wide spread of data and the minimum points being from poor Type 304 heats many years ago. W. Cooper pointed out that code committees are aware of the problem and that all stress values are being reviewed by the Code Committee on the basis of a data review by the Metals Properties Council.* The participants at this meeting recommended using the new material properties data for FTR design.

For Type 304H SS material at creep temperatures, the 80% of minimum stress-to-rupture in 10^5 hours criterion has, in the past, determined the S_m curve. This criterion still dominates when G. V. Smith's new trend curves⁽⁵⁾ are used, as can be seen in Appendix A. However, the statistically defined minimum has resulted in slightly higher S_m values for the creep temperature range.

2. SUSTAINED GENERAL MEMBRANE STRESS INTENSITY, S_s

For the expected long-term transients and the long-term loadings of normal service, the S_{ms} values will use the actual service lifetime at a given condition. In the creep range, the criteria for S_{ms} are similar to S_m criteria but involve a time based on the actual plant. S_{ms} is equal to S_m as long as tensile properties dominate at the higher temperatures. S_{ms} is the minimum of:

- 2/3 of average stress to produce rupture in "x" hours
- 80% of minimum stress to produce rupture in "x" hours
- 100% of average stress to produce a total creep strain of 1% in "x" hours

The "x" hours refers to the actual total operating hours at a given condition. Where several different long-term

* *The revised values were approved by the Code Committee as a result of the June, 1969 meetings.*

loads may exist in the plant, the linear life-fraction rule can be used in combining the various loadings. Justification for this method for accumulation is based on Bolton's recent presentation to the Elevated Temperature Design Subgroup in ASME. (See Appendix B).

The main advantage of S_{ms} over present nuclear code rules is the use of actual lifetime and long-term condition temperatures. Short-term phenomena would no longer be governed by design criteria based on the assumption of long-term service. The S_m represented a design condition, and now the various operating conditions are represented by separate S values. S_{ms} relates to normal and S_{mt} (discussed below) relates to upset loading conditions. S_{ms} and S_{mt} will reflect large fractions of plant life spent at lower temperatures and pressures.

3. TRANSIENT GENERAL MEMBRANE STRESS INTENSITY, S_t

A new limit, S_{mt} , relates to the combination of sustained loading plus the momentary overload (or transient) membrane stress from upset conditions. Examples of such loads include:

- Earthquake
Sodium hammer
- Single component failure accidents
- Any other transient whose total time of occurrence is less than 10 hours

This new limit shall be based on 90% of the yield strength as defined by 0.27% total strain from low- and-constant strain rate tensile tests. These data do not currently exist at the lowest strain rates (corresponding to ~10 hour total test time). However, R. Moen was able to utilize 0.2% offset data and linear extrapolation to complete

the curve in Appendix A. To better define S_{mt} , additional tests should be performed utilizing low-and-constant strain rates for metal temperatures between 800 °F to 1300 °F.*

Dick Slember also warned that short-term transients may influence subsequent creep behavior. If so, new criteria may be required to limit transient loads. Ev Rodabaugh again emphasized that the weld tests may determine the lowest allowable design stress curves in every category and that test data should come from weld cross-sections of the actual pipe material.

In using the S_{mt} curves, only "significant" events are to be considered; that is, events in which the load stresses exceed the S_m value by at least 10%. Two other rules are added to give designers a simple approach. First, multiple events are considered as the sum or total duration of their load times. Finally, for a variety of short-term loads, the life-fraction rule (See Appendix B) may be used.

PRIMARY-BENDING-PLUS-PRIMARY-MEMBRANE STRESS CRITERIA

Because of the inherent flexibility of the system (low pressure, thin walls, high temperature, large expansion) the stresses resulting from dead weight (pipe, insulation, contents) will exceed those of current light water reactors. This will require a more elaborate structural support for the system. Too much unstayed flexibility will concentrate strains at the bend.

The current criterion factor of paragraph 4(a) of Code Case 1331-4 was considered appropriate for the primary loads under consideration excluding pipe thermal expansion loads. Temperatures should be based on the actual cross-section

Note that temperatures above the 1200 °F system design temperature may be expected to exist for certain short transients.

average. "Stringers" (i.e., fluid temperature layering) will be considered in the fatigue evaluation. Temperatures are not restricted as in the code case; the equation applies for temperatures up to 1200 °F. The lower bound for the coefficient of S_m is still 1.0 when using the equation.

As with primary general membrane stresses, the criteria are separated into short-term and long-term loadings, S_{mt} and S_{ms} . The coefficient factor is the same for each type of loading.

PRIMARY-PLUS-SECONDARY STRESS CRITERIA

The areas of secondary stress, shakedown and ratchetting were discussed in detail at the meeting. The technical knowledge in these areas is far less than that in other areas such as primary stress and low temperature fatigue. Tests are recommended so that this lack of knowledge will be partially overcome during the next 15 months. Until the new test data are available, the following may be used:

- Primary-plus-secondary stresses shall be defined under the rules of B31.7 and the associated elevated temperature code case. However, the limiting stress intensity shall be $3 S_{mt}$ for elevated temperatures.
- On an interim basis, exceeding the $3 S_{mt}$ limit is acceptable for a transient loading of 25 cycles or less by using the methods of the B31.7 section on Simplified Elastic-Plastic Analysis.

The above rules do not differ significantly from the present B31.7 rules. The $3 S_{mt}$ limit was accepted as appropriate for the $3 S_m$ to $3 S_a$ criteria transition in Section III of the vessel code. "Cooper's Compromise" curve (See Appendix C) was first considered -- but the $3 S_{mt}$ values were adopted since they were available and almost

duplicated the values on Cooper's curve. The limit on cycles for the Simplified Elastic-Plastic analysis arose because of the fear of local overstrain and ratchetting problems and the belief that this approach is probably not technically correct." The group recommended that further work be done in this area, and, that a limitation of 25 cycles would provide an acceptable preliminary design risk. Some of the important parts of the discussion are reviewed in the following sections:

1. RATCHETTING AND SHAKEDOWN:

Lee Wright discussed the problems of analyzing the stress and strain history on a pipe component. Straight pipe sections can be analyzed by computer models, but the more important parts (i.e., elbows and tees) have been experimentally modelled. Current work by C. F. Braun and UNC will be published in about three months. One of their ways of setting allowable ratchetting stress loads was to consider the hot and cold yield strengths, then add on additional strength for strain hardening. Without some extra design room, thermal transients often force LMFHR piping designs to use unnecessary thermal sleeves. High primary stresses or high thermal expansion stresses that require increased wall thickness will also increase the secondary stresses arising from thermal shocks.

W. Cooper pointed out that the vessel and piping codes use high percentages of yield strength for a limit criteria just because strain hardening does occur in structural steel. Cooper did favor a separate ratchetting criterion similar to the one in Section III of the vessel code.

* *B31.7 uses a conservative procedure proposed by S. W. Tagart. However, Cooper and Snow stated that no data for this procedure exist in the creep range and that the procedure may be inaccurate.*

Bree's work^(6,7,8) (See Figure 1) was discussed by Slember and others. The consensus was that the latest work⁽⁷⁾ was the most relevant to LMFBR problems. A single smooth curve (similar to the farthest right curve on the figure) may bound the design region, but meager data and many variables (triaxiality, temperature, load rate, prior load history, etc.) are complicating the understanding of this phenomena.

W. Cooper stated that Professor Krempl at RPI has some ideas for research on ratchetting.⁽⁹⁾ The attendees agreed that additional work must be done quickly in the ratchetting area, and that Krempl's proposal should be given prompt consideration. Ratchetting and deformations become even more important when designs involving thick structural parts on pumps and valves are considered. The 25-cycle limit was to limit the total strain that may occur locally as a direct result of severe (emergency) transients. The 25-cycle limit also reflects the group's expectation that the simplified elastic-plastic concept of B31.7 will be invalid at higher temperatures.*

When the Braun-UNC work is available, there may be extra criteria added for evaluation of thermal ratchetting. The interim criteria for FTR piping design are expected to undergo several changes during the next 15 months. The improvements should allow a thorough evaluation of ratchetting in the straight pipe sections.

2. THERMAL STRESS AND THERMAL SHOCK

Most of the attendees agreed that the nature of thermal stress moment generation effects were the same as those from mechanical loads insofar as pipe reaction effects on equip-

* *B31.7 uses a conservative procedure proposed by S. W. Tagart. However, Cooper and Snow stated that no data for this procedure exist in the creep range and that the procedure may be inaccurate.*

ment are concerned. Pipe expansion stresses, both in and immediately adjacent to the vessel, are treated as per paragraph N-417.9 of Section III.

FIGURE 1. *Uniaxial Stress Regimes - Mean Stress Versus Thermal Stress Range for Linear Temperature Gradient (Bree Method)*

There was a temptation to classify the linear portion of the radial temperature profile in a cylinder wall as a "peak" stress. This would be consistent with the nuclear vessel code but inconsistent with the rules of B31.7. Les Alsager and others felt that the secondary stress was the appropriate classification. Even though the conservatism of the vessel code may allow a peak stress classification, the progress toward reality should not be lost...the linear portion of the radial wall gradient causes secondary stresses, just as in the E31.7 Code*. The attendees agreed that the E31.7 philosophy was most appropriate until the time when the two codes become technically equivalent in this area.**

3. PRE-LIFE EFFECTS

The effects of prior loading history on creep behavior was briefly discussed. There appears to be an absence of materials data for answering questions such as: What design procedure covers an earthquake loading which occurs after ten years of creep straining? What conditions lead to interrupted creep tests having much-increased creep-strain rates?

Testing was recommended along the lines of Coffin's ~1955 paper on cyclic stress-strain tests. At elevated temperatures tests are needed that involve high strains per cycle on cyclic stress-strain tests after prior service life has given the test specimen some prestrain. The strain rate of the cyclic tests would be an important parameter. The interrupted creep tests may be within the present scope of the GE-NSP test program -- inclusion in that program should be explored. This is discussed in the next section on fatigue criteria.

* *The ASME reactor vessel code has retained the linear radial gradient as a peak stress primarily because: (a) their $3 S_m$ secondary stress criterion is too conservative; and (b) they will not accept the present form of Tagart's procedure as part of the Code.*

** *A compromise satisfactory to both groups was apparently reached during the subsequent September, 1969 Code V Meetings.*

CREEP-FATIGUE CRITERIA

High cycle fatigue ($N > 10^4$ cycles) should be handled as in the associated elevated temperature code case for 131.7. The curves are for "no holdtime" cycling at a strain rate range of $\sim 2 \times 10^{-3} \text{ sec}^{-1}$ to $2 \times 10^{-4} \text{ sec}^{-1}$ (See Reference 10). Since faster strain rates seem to improve Type 304 SS behavior slightly, the present curve should also cover all faster strain rate design. The stated factors of safety on the nominal data are: 2 on stress or 20 on cycles, whichever gives the lowest curve value on the ϵ_t versus N plot.

The area of low cycle fatigue with holdtimes and at creep temperatures provides one of the significant challenges to safe LMFBR piping design. The present work at GF-NSP (Cincinnati, Ohio) was recognized as significant research in this area. Jakub and Snow briefly discussed the GE-NSP results with Type 304 SS at 1200 °F:

- Fatigue cycling with tension holdtimes correlates with stress-to-rupture data where the hold-time is summed and the stress level is the average of peak and relaxed values.
- Other types of cycles compare with zero holdtime results.
- Other types of data fall along a line of slope -1 on a curve of log (stress amplitude) versus log (elapsed time). Elapsed time is (N_F/freq) for zero-holdtime tests.

The obvious usefulness of their results is limited to considering the tension-hold-only fatigue cycle, which is the most severe, as the model for reactor cycling. This would then allow a separation of fatigue and creep phenomena.

The GE-NSP test results were not considered as conclusive... although additional tests planned for next year may be helpful. Tests with longer hold times will help resolve which stress level must be used as a reference on a tension-hold fatigue specimen. Figure 2 shows a typical relaxation curve for a

FIGURE 2. *Stress Relaxation During Holdtime*

hold time portion of a cycle. Wherein σ_0 is defined as the original stress; σ_2 , the final relaxed stress; and σ_1 , the arithmetic average of σ_2 and σ_0 . For holdtimes up to 200 minutes, GE-NSP found good correlation using σ_1 as a reference stress. Yet, the time of major stress relaxation, perhaps up to t_0 , is expected to involve only about 5% of the damage. While some attendees felt that longer holdtime tests will show that σ_2 is the best correlation stress, Ev Rodabaugh and M. Jakub still were concerned about incurring too much plastic strain from the relaxation process on each cycle.

The attendees concluded that the linear damage equation(11) was believed to be a conservative method for design until GE-NSP results (or others) adequately proved other alternatives (See Figure 3).

$$\sum_{i=1}^K \left(\frac{t_i}{t_{ri}} + \frac{N_i}{N_{fi}} \right) \leq 1$$

FIGURE 3. *Failure Diagram for Creep-Fatigue of Type 304 SS*

- t_i = time spent at maximum tensile stress under i^{th} loading
- t_{ri} = minimized time-to-rupture under a reference stress of i^{th} load. At present use peak stress as the reference stress (a_0 on Figure 2) and use Figure A.3 in Appendix A for t_{ri}
- N_i = number of cycles under i^{th} loading
- N_{fi}^i = number of cycles-to-failure if no holdtimes occurred in the i^{th} loading. In order to minimize the value the code values shall be used as per the elevated temperature code case. [Average data curve is reduced by 1/2 on stress or 1/20 on cycles -- whichever gives the lowest curve.]

In calculating the values for the equation, the material is assumed to be notch insensitive. Thus only the primary

stress intensities are included in the t/t_r term -- secondary and creep stress intensities are assumed to yield out. This assumption is consistent with the behavior of notched creep specimens, which show no loss of life when compared with unnotched specimens. Ev Rodabaugh pointed out that this still begs the question of how much total plastic and creep strain can be taken in a localized area.

The last term, N/N_f , must account for all stress intensification factors. N_f uses the code values with their factors of safety to adjust for the worst possible mean stress effects and to adjust the data for the "lesser-quality-but-acceptable" material.

CREEP BUCKLING

A brief discussion of creep buckling from axial loads was held, but no criteria were recommended for this area. The basic problem, which is a lack of analytical tools, keeps most of the research efforts concentrated on straight cylindrical pipe buckling. Yet experience has shown that the elbows take up most of the actual plastic deformation.

Professor J. Brock (at the Naval Postgraduate School and a part-time affiliate John F. Wax, Inc.) is currently studying (analytically) the phenomena on buckling at low temperature (<300 °F). The program was initiated through ORNL. The complexity of bending buckling of elbows is illustrated by Figure 4. Should M_2 cause a buckling collapse, then M_1 can be approximately three times larger before the elbow buckles.

FIGURE 4. *Buckling Moments on Elbows*

With a lack of data on elbow buckling, the group suggested that the pipe designer follow two general guidelines: (a) reduce loads which can induce buckling; and (b) keep unsupported pipe lengths at a minimum in regions of axial loads.

APPLYING UNIAXIAL CRITERIA TO MULTI-AXIAL STRESS FIELDS

In present nuclear codes, the flow rule of Tresca (maximum shear) determines how the multi-axial stress fields are related to the uniaxial stress fields of tensile and creep specimens." Recently, the usefulness and validity of the Tresca rule has been questioned. After a long discussion, the attendees concluded that the Tresca or modified Tresca flow rule should be used. In creep analysis, either the Tresca or Von Mises (distortion energy) method are acceptable.

D. Slember, in stating his observations on how the various flow rules correlate with material phenomena, pointed out that creep-rupture of austenitic stainless steel best correlates with the maximum principal stress.** To best estimate equivalent strain, either the shear or distortion energy theories can be used. Ev Rodabaugh stated that crack propagation also seems to correlate best with maximum principal stress. Bill Cooper pointed out that the plot of yield surfaces (See Figure 5) is only 15% different for the two theories of shear and distortion energy. He believes that the use of a Modified Tresca rule can reduce the error further. In his opinion, the Tresca (or Modified Tresca) is conservative and is far easier to work with for the bulk of design applications. D. Slember pointed out that under an increasing stress loading, the strain errors from the Tresca flow rule can be as high as 20 to 50%. For these long term loadings, attendees agreed that the use of shear distortion theory is certainly acceptable.

* *Note that the tri-axiality of necking is glossed over by the statement.*

** *This is true for temperatures of ~ 1100 °F and above where the material fails by intergranular cracking. At temperatures below ~ 1000 °F, the transgranular failure mode correlates better to the Von Mises criterion.*

FIGURE 5. *Failure Surfaces on Biaxial Stress Field*

RECOMMENDED TESTS

This section briefly reviews the tests recommended by the meeting participants.

1. Low-and-constant strain rate tensile tests must be performed on Type 304 SS to define, for the designer, the allowable design curves for short-term loads. Initial test data in the 1200 to 1300 °F range should be followed by tests at temperatures down to the 800 to 900 °F range. These tests are not expected to be difficult even though the strain rates are lower than standard Instron values. The maximum test time is ten hours for the total strain of 0.27%.
2. Mid-life, cyclic stress-strain tests must be performed on Type 304 SS to determine what, if any, reduction of strength occurs after partial service life. Pre-strain from short-term effects or long-term creep effects may lower the capability to withstand

- transients significantly (e.g., earthquake loads). The tests would be performed initially at the design temperature of 1200 °F - then at lower temperatures down to the 800 to 900 °F range.
3. Ratchetting and Shakedown research should be undertaken immediately by Krempf and perhaps others. These phenomena were the least understood of all areas discussed at the meeting.
 4. Low-Cycle fatigue and creep-fatigue testing of Type 304 SS still has not resolved the following questions:
 - With t/t_r and N/N_f maintained at about the same value during a tension holdtime test, what will the ratios equal at failure?
 - Do holdtime effects saturate out if holdtimes longer than 200 minutes are used? What reference stress must be used for t_r on such a test?
 - If the load strain is changed during a tension holdtime fatigue test, what is the appropriate accumulation criterion?
 5. Long-term creep-rupture testing of Type 304 SS must be done on both notched and un-notched specimens to satisfy the requirements of the elevated temperature code cases (e.g., see paragraph (1) (b) of Code Case 1331-4).

BNW will attempt to incorporate all of the above tests into either RDT or BNW-FFTF tasks for FY-70.

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APPENDIX A

TYPES 304 AND 304H SS PROPERTIES AND DESIGN CURVES

APPENDIX A

TYPES 304 AND 304H SS PROPERTIES AND DESIGN CURVES

R. A. Moen

Figure A.1, S_m , was constructed by passing a curve through the minimum of the following:

- 1/3 of the specified minimum tensile strength at room temperature
- 1/3 of the tensile strength at temperature
- 2/3 of the specified minimum yield strength at room temperature
- 90% of the yield strength at temperature but not to exceed 2/3 of the specified minimum yield strength
- 100% of the average stress to produce 0.01% creep in 10^3 hours
- 80% of the minimum stress to produce rupture in 10^5 hours
- ⁶⁷60% of the average stress to produce rupture in 10^5 hours

The data used in the above criteria were obtained from the corrected version of G. V. Smith's October 9, 1968, memo on "Trend Curves for Austenitic Stainless Steels." The ultimate tensile strength of Type 304-304H was formulated by: (1) multiplying the trend ratio values for each temperature from Smith's memo times the minimum tensile strength expected for that material at room temperature, 75 ksi; (2) adding a 10% increment to the minimum expected tensile strength at temperature, as employed in Section III; and (3) multiplying the resultant number by one-third. The yield strength values were obtained by multiplying the trend ratio values by the minimum yield strength expected for that material at room temperature, 30 ksi; then multiplying this resultant number by the 90% or two-thirds as stated above. The average stress to produce rupture in 100,000 hours was that adjusted "by the percentage required to adjust the value at 1200 °F, from

Fig. A.1, The 60% curve should be redrawn slightly to the right and labelled as "67% ADJ. AVG. STRESS TO PRODUCE RUPTURE IN 10^5 hr." NWL-1236

FIGURE A.1. S_m for Type 304 and 304H SS

regression analysis, to equal the average of the values at 1200 °F developed by visual extrapolation of individual lots." The minimum stress to produce rupture in 10^5 hours was "computed by multiplying the average trend curve by the ratio of statistically defined minimum (average less 1.65 times the standard deviation) at 1200 °F to the average at 1200 °F." The creep values were again taken from Smith's memo and are reported as average stress for a secondary creep rate of 0.01% per 1000 hours as determined by linear regression of log strength versus temperature.

Figure A.2, S_{mt} , is tentative as noted since some simplifying assumptions were required to construct the curves. The assumptions were:

- (1) The yield strength corresponding to 0.27% total strain is approximately equal to the 0.2% offset yield strength.*
- (2) The strain rate of tests comprising G. V. Smith's trend curves was $0.002 (\text{min})^{-1}$.
- (3) Johnson and Murray's relationship reported in ISI-P97 holds true, that a ten-fold increase in strain rate produces about 1300 psi increase in yield strength, or, a ten-fold decrease produces about 1300 psi decrease in yield strength, regardless of the steel and regardless of the temperature.

This allows Smith's yield strength values for 800 to 1300 °F to be placed on a yield strength versus log time plot at the time required to produce a strain of 0.27% at a strain rate of $0.002 (\text{min})^{-1}$, that is, 1.5 minutes. Using Johnson and Murray's relationship, a strain rate increase to $0.02 (\text{min})^{-1}$ increases each yield strength value by 1300 psi, etc. The S_{mt} curves were obtained by using 90% of the values described

* Elastic strain at 0.2% offset strain is $\sim 0.07\%$ for temperatures of interest as shown in Figure A.7.

Fig. A.2, The reference scale along the ordinate is incorrectly labelled. Values range from 5 psi to 30 psi with small marks occurring at every 5 psi interval.

FIGURE A. 2. S_{mt} for Types 304 and 304H Austenitic SS

above. Also noted on the left side of the graph are the actual yield strength values for a strain rate of $0.002(\text{min})^{-1}$.

Figure A.3 is a minimum stress-to-rupture plot of log stress versus log time-to-rupture for 1000, 1100, and 1200 °F. The 10^5 hour rupture values correspond to G. V. Smith's numbers. The slopes of the curves are the same as those reported in KNWL-891, FFTF Materials Design Data.

Figure A.4 was produced in the same manner as Figure A.3, using Smith's data to anchor the 10^5 hour values and using the curves in BNWL-891* to establish the slopes.

Figure A.5, S_{ms} , was constructed by first drawing in the yield strength portion of the S_m curve, shown previously in Figure A.1, from room temperature out to 1200 °F. Then a tabulation of stress rupture and creep strength versus time for temperatures of 1000, 1100, and 1200 °F was used to obtain the controlling property for an S_m -like curve. These values were used to draw in a series of time-dependent stress versus temperature curves.

Figure A.6 is the design fatigue curve for austenitic stainless steels as it appears in Code Case 1331 of the Nuclear Vessel Code (Section III).

* R. A. Moen. *FFTF Materials Design Data*, BNWL-891.
Battelle-Northwest, Richland, Washington, October 1968.

FIGURE A.3 Minimum Stress to Rupture for Type 304 and 304H SS
(G. V. Smith's Table 10, Col 2)

FIGURE A.4 Creep Rates for Types 304 and 304H Austenitic SS

FIGURE A.5 S_{ms} for Types 304 and 304H Austenitic SS

FIGURE A.6 Design Fatigue Strength S_a for High-Alloy Steels and Nickel-Chrome-Iron Alloy up to 1200 °F. Code Case 1331-1

FIGURE A.7 *Sy/E Versus Temperature for Type 304 SS*

APPENDIX B

LEYDA COMMUNICATION AND LITERATURE
SURVEY OF THE LIFE FRACTION CONCEPT

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SURVEY OF THE LIFE FRACTION CONCEPT

Letter communication from W. E. Leyda, Metallurgy and Welding, Babcock and Wilcox to B. E. Bolton also of B&W. L. R. 69:2146-01:1, presented by the recipient to those attending the ASME Meeting, New York City, 3-3-69.

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"In answer to your request for an analysis of the "Life Fraction Concept" principle, the following literature survey has been prepared. The only references contained in this summary are those which actually contained test data.

From the work reported in these references, one can only conclude that the "Life Fraction Concept" principle is a useful tool. Not only does it appear to be useful but it appears to be the only tool available at the present time.

There are some indications that the principle is not applicable and gives poor correlation when used with unstable materials. Also, in its present form it appears that it cannot accommodate large amounts of strain hardening or relaxation, however, under design conditions these problems should not exist.

- - - - -

Whether it be for the evaluation of creep damage resulting from service or for studying new concepts for advanced design the engineer frequently requires the use of the life fractions concept. Since it was initially proposed in 1952 by E. L. Robinson, many investigators²⁻⁷ have tried to improve on it or come up with something better but to no avail. It has been recognized that the concept cannot fully account for large amounts of transient creep, relaxation, or certain metallurgical changes. However, because of the time, difficulty, and cost of conducting the tests, which would be required to further validate or to refine the process, the concept continues to be used and accepted as originally presented.

The life fraction concept is based on the premise that the expenditure of each individual rupture life fraction of the total life at elevated temperature is independent of and without influence upon the expenditure of all other fractions of the life-to-rupture. Laboratory evaluations of the life fraction concept have in general been of short time duration (rupture tests) employing high stresses resulting in high creep rates. In addition the evaluations generally employed either step increases or decreases in stress and temperature or cycling of these two parameters. Because of the short time nature of the tests, the changes or steps were generally large in order to create measurable changes. These factors often led to a significant amount of relaxation on unloading and large amounts of transient creep on loading. In addition, some of the tests were conducted in the yield range of the test material which introduced the additional factor of strain hardening. Tests were also conducted in which cycling was carried on in the tertiary stage of creep which helped to promote early fracture.

Regardless of poor planning and the introduction of questionable conditions, most all of the test results substantiate the life fractions concept (the life fractions add up to unity). The following quotes represent conclusions drawn by engineers who sought to verify the concept:

1. H. R. Voorhees, C. M. Sliepcevich, and J. W. Freeman²

"In view of the consistent results for this entire mass of data, it appears safe to conclude that the observed addibility of life fractions under variable tension stresses should hold to a close degree for all alloys under conditions where they have reasonable structural stability."

2. R. F. Decker, J. V. Gluck and J. W. Freeman³

"Damage accumulates on a percentage basis in structurally stable materials (sometimes called the 'life-fraction' rule).

For instance, if a material is exposed for 25 percent of its life at one set of conditions, it will continue to creep under another set of conditions as if 25 percent of its life had been expended under the new conditions."

"The life-fraction rule would be expected to hold best when the changing conditions do not involve too wide changes in temperature and creep rate."

3. Avraham Rerkovitz

"Although considerable study is necessary in order to explain the recovery phenomenon as it affects cyclic-load creep and to modify the creep relations accordingly, it appears that when many load cycles are applied, as in aircraft structural applications, the existing hypothesis produce fair agreement between calculated and experimental creep strain if the loads within the cycles are in ascending order. Poorer correlation is obtained when the loads within the cycles are in descending order. Rupture times computed using the life-fraction hypothesis were found to be in good agreement with experimental data."

4. R. Montandon and F. Kirchner⁵

"According to the measurements obtained, however, the assumption that the life of a specimen during test or in service expires to the same extent as the time till fracture is quite permissible."

5. P. N. Randall⁶

"The life-fraction rule predicted the cumulative damage reasonably well for these tests. There was appreciable creep strain under each set of test conditions, so the cumulation of life fractions may be regarded as the gradual exhaustion of ductility of the steel under these conditions.

However, the test conditions did not include a period of high initial temperature (1100 to 1200 °F) followed by service at low temperatures (800 to 1000 °F). Work in progress has shown that this situation may produce severe damage not predicted by the life-fraction rule."

6. R. M. Goldhoff⁷

"The methods tested were (a) time hardening, (b) strain hardening, (c) life-fraction, and (d) strain fraction. The latter is possible because all tests were run to failure and terminal elongation measured. It is basically like the life-fraction method except the cumulative result is based on exhaustion of strain rather than time in each successive step.

Each of these methods is illustrated graphically in Figure 6 for the step-down on Steel 1. Predictions of rupture time for each specimen by each method are shown in Table 4.(7) This table shows that the strain-fraction method is the best overall procedure and is remarkably accurate in all but the step-down test on Steel 1. Excluding time-hardening, which is unworkable, all the remaining methods give reasonable predictions."

7. Unpublished results obtained by The Babcock and Wilcox Company substantiate the general applicability of the life-fraction rule. These results also point up the same thoughts expressed by Berkovits⁵ and Randall⁷ regarding loads in descending order. However, there is an indication that the lower the stresses the less is the effect and it would be hoped that this effect would disappear or become negligible under Code stresses.

In light of these statements, which were by each engineer that actually generated test data, and since normal changes in the operating conditions of a pressure vessel would involve

only small changes in stress and/or temperature or both, the life fraction concept appears to be a useful tool partially dependent on interpolations and extrapolations of rupture data for its accuracy."

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APPENDIX C

INTERIM STRUCTURAL DESIGN CRITERIA FOR FFTF REACTOR VESSEL

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INTERIM STRUCTURAL DESIGN CRITERIA FOR FFTF REACTOR VESSEL

At Washington, D.C. on 2/28/69, the following criteria were adopted as interim design criteria for the FFTF reactor vessel. These criteria are valid for Type 304 SS at temperatures up to 1000 °F and for normal and upset loading conditions.

- Use current Section III rules along with Code Case 1331-4 except that S_m and $3S_m$ values shall be obtained from the curves of Figure C.1, Cooper's Compromise Curves.
- The linear damage equation, $\frac{t}{t_r} + \frac{N}{N_f} \leq 1$, shall be used for creep fatigue design.
- Assume no deleterious material effects from sodium coolant or from irradiation as long as total tensile test ductility is at least 10%.

FIGURE C.1. Allowable Stress Values for Type 304 SS FTR Vessel

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