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HIGH-TEMPERATURE STRUCTURAL DESIGN
METHODS FOR LMFBR COMPONENTS
QUARTERLY PROGRESS REPORT FOR
PERIOD ENDING JUNE 30, 1972

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SEPTEMBER 1972

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ABSTRACT

This is the third of a series of quarterly progress report summarizing accomplishments in the ORNL program to develop high-temperature structural design methods for LMFBR vessels, components, and core structures. This report covers the period of April through June 1972. The program has as its goal the development of a verified high-temperature design technology applicable to the long-term operating conditions expected for LMFBR systems. Although the ultimate objective is to develop verified methods and criteria for reliably designing for high temperatures and long life, near-term efforts are largely concentrated on establishing and validating methods and criteria to be used, on an interim basis, for FFTF and which are based on the current, largely unverified, state-of-the-art. The overall program is divided into a number of tasks which are being carried out with the participation and cooperation of industrial groups, universities, and design code bodies. Progress on these tasks is described herein.

SUMMARY AND STATUS

W. L. Greenstreet

Management and coordination activities continue to account for a significant portion of the effort under the overall High-Temperature Structural Design Methods Program. During this reporting period, we have participated in planning discussions for initiating a large-scale testing program to provide basic mechanical properties data, such as short-time tensile, creep, and relaxation data, as well as data to provide information regarding loading history effects on subsequent deformation response for metals of interest in LMFBR system designs. Work in this area is to be carried out under an ORNL program entitled Mechanical Properties of Structural Materials (189a 10567). In addition to participation in planning of this program, we are assisting in meeting the needs for type 304 stainless steel specimen material from the available reference heat

stockpile. The test program to provide data on low-cycle fatigue and creep-fatigue interaction behavior that was developed under this program and is to be carried out at Argonne National Laboratory (ANL) was discussed at meetings held at ORNL on April 28, 1972, and at DRDT on May 17, 1972. The purpose of these meetings was to describe the ORNL proposal and underlying bases and to discuss differences between the proposed program and alternatives proposed by ANL. The differences were clarified and a revised plan was subsequently prepared by ANL.

ORNL staff members participated in three audit committee meetings concerned with inelastic analyses to be, or that are being, performed by designers of LMFBR components and facilities. As reported last quarter, an audit committee made up of DRDT, HEDL, WARD, and ORNL personnel was established for the purpose of assessing the general capabilities, experiences, and problems associated with inelastic analysis efforts in order that a definite plan of action can be promulgated for expediting inelastic analyses throughout the FFTF project. One meeting of this committee was held during this reporting period; the meeting was at Bethesda, Maryland, on April 19, 1972. A related stress analysis audit activity was recently initiated by HEDL and has as its objective the evaluation, in detail, of the design stress analysis work being conducted by FFTF manufacturers. At the invitation of HEDL, ORNL personnel are participating in this activity. A staff member attended an audit meeting at Bechtel on June 7-8, 1972. Finally, at the request of DRDT, we assigned one member to an ad hoc team which made a detailed audit, on June 27-28, 1972, of inelastic analysis work performed by C. F. Braun for the Sodium Pump Test Facility (SPTF). In contrast to the participation in the first two activities mentioned, participation in the third is not expected to be on a continuing basis.

To help expedite inelastic structural analyses of FFTF components, we arranged a working group meeting which was held on April 17, 1972, at ORNL. The purpose of this meeting was to bring structural design analysts and code developers together to discuss computer programs (mainly finite element codes), structural analysis requirements, problems being solved, and experiences and difficulties in handling structural analysis problems. Participants included representatives of 13 vendors and the developers of principal computer codes for inelastic analysis use. The latter are:

J. A. Swanson (ANSYS), P. V. Marcal (MARC), Y. R. Rashid (CREEP-PLAST), and Z. Zudans (EPACA). The meeting provided a timely and useful exchange of information because of the status of implementation of inelastic analyses and because of recent developments related to such implementation. The latter included a restatement of guidelines for structural analyses of FFTF components, additional materials properties data to be included in the LMFBR Materials Properties Handbook, plans for incorporating the currently recommended constitutive equations in the computer programs, and plans to compile a catalog of benchmark problems and to use these problems in validating computer programs. Follow-on working group meetings will be held as the need arises.

Coordination activities with the Pressure Vessel Research Committee (PVRC) and with the ASME code committees were actively pursued. We attended and participated in PVRC meetings on April 19-20, 1972, and May 22-23, 1972; a representative attended ASME code subgroup meetings on June 20, 1972; and we participated in meetings and activities of the ASME Subcommittee on Computer Program Verification and Validation.

The LMFBR Program Office recently assigned to ORNL the responsibility for drafting the task description on high-temperature structural design, Task 2-000-1 (formerly Task 2-000-2) for the revised overall LMFBR Program Plan. The area of high temperature design is to be given significantly greater emphasis in the revised Plan. We submitted the initial draft for Task 2-000-1 in June; comments were received from the PRO coordinator; and we are in the process of revising the draft. Finally, work associated with rewriting and updating the detailed High-Temperature Design Methods Program plan, which was issued as a two-part (short- and long-range) document in 1970, was initiated.

We are continuing to work closely with WARD to provide guidance for their program entitled Validation of High-Temperature Design Methods and Criteria. This program is to supplement the ORNL High-Temperature Structural Design Methods Program by providing additional experimental data for validating high-temperature design methods. The WARD program was reviewed in a progress meeting at ORNL on April 27, 1972, and we participated in a meeting at DRDT on June 29, 1972, to discuss ratchetting tests on a model of the intermediate heat exchanger (IHX) primary inlet nozzle. These

tests have been added to the WARD program and are to be performed primarily to prove the structural adequacy of the nozzle design; the results are also expected to contribute to the development of structural design methods. The present status of the WARD program and plans and priorities for work during the next fiscal year were also discussed during this meeting. We are assisting WARD in defining and obtaining weldments of characterized materials for use in basic specimen tests to determine strain limits under biaxial conditions. Finally, we are cooperating in the procurement of material for use in test specimen fabrication and in the procurement of pipe and pipe fittings for this program.

The report:

E. P. Esztergar, "Creep-Fatigue Interaction and Cumulative Damage Evaluations for Type 304 Stainless Steel: Hold-Time Fatigue Test Program and Review of Multiaxial Fatigue," USAEC Report ORNL-4757,

was published and distributed during this quarter. The three interpretive type reports are in various stages of ORNL preparation and publication. The first of the three, by E. T. Onat, is scheduled for distribution in August. Negotiations were initiated to enter into subcontract with the Boeing Company, Aerospace Group, to conduct a high-temperature test of a shear-lag specimen using Boeing capacitance gages. The negotiations were not completed during this quarter, mainly due to questions of patent rights. It is expected that a contract will be signed in the next quarter, however.

Under the ORNL program activities, a second machine for combined stress testing under inelastic conditions was delivered and has been prepared for operation during this quarter. This second machine is intended for use in studying creep as well as elastic-plastic behaviors of thin-walled tubular specimens.

Tests to investigate initial and subsequent yield surfaces have been initiated. The specimens are made of type 304 stainless steel from the reference heat, and the first tests are to be conducted at room temperature to establish the method and technique to be used. As soon as the necessary preliminary work at room temperature is completed, high-temperature testing will be initiated. The room-temperature work involves setting up the apparatus and controls necessary to define and map yield

surfaces. The method selected, that is, defining yield through the use of tests that produce limited, carefully controlled offset, or permanent, strains, holds a significant advantage over other methods in that a surface can be mapped through the use of a single specimen. To accomplish this, precise control of deformation is required.

The analysis of results from previous radial loading tests at room temperature show that initial yield is reasonably described by the von Mises yield function, and the directions of the plastic strain trajectories are very close to the directions of the loading paths in stress space. Further, the form of the function, g , in the equation which relates the total surface translation tensor to the plastic strain tensor applies to the case of radial loadings in combined stress space as well as for uniaxial loadings.

A combined stress (combined tension and torsion loading) creep test at 1100°F was completed at Brown University this quarter. The duration was 1000 hr, and a series of short-time loading and recovery tests were subsequently carried out on the same specimen. Changes and improvements in the experimental facility were made during this period. A second combined stress test machine was made available and preparations were made for testing the first specimen in this machine. Finally, the second specimen was readied for test in the first machine.

The initial tests to be carried out under the Babcock and Wilcox Co. creep rupture studies have been defined, type 304 stainless steel reference heat material has been supplied, and several uniaxial specimens have been machined and annealed. Tests have been initiated on two of these specimens. Of the two types of uniaxial test machines, that is, creep and rupture machines, to be used, only the creep machines were fitted with DCDT's to measure deformations. The rupture machines are now being similarly instrumented to aid in the data collection process.

The effort to develop uniaxial equations to describe the creep behavior of the ORNL preliminary heat of type 304 stainless steel at 1200°F was continued. This effort was addressed to providing two correlations: a best representation of all available constant-load creep data for this material and a representation of the creep data for test times up to 1000 hours only. The studies carried out dramatically emphasized the

need for conducting precision experiments, which provide accurate strain values throughout the regions of interest, especially in the low strain ranges. Further, these accurate strain data are needed at a number of constant stress levels, with particular attention being given to stresses in the range of code allowable design values.

The finite element computer program, CREEP-PLAST, which was developed for elastic and inelastic analyses of two-dimensional solids, is now being distributed to the public. It is being supplied to requesters with the understanding that any modifications and changes made will be coordinated with ORNL. A formal request form and an agreement document are included in this report for convenience in ordering copies of the program. The constitutive equations recommended by ORNL for FFTF use are included in the version being distributed.

A completed version of the EPACA finite element program, which is for use in analysis of general thick shells, has been transmitted to ORNL. The program as received is a "raw program" in which many of the features have not been checked out. Hence considerable ORNL effort will be required to place it in the category of being ready for design analysis use. We plan to make the program available to the public this calendar year, with distribution to be made on the same basis as for CREEP-PLAST. A combined theory and user's manual document for EPACA has been written.

Several of the LMEC ratchetting tests were analyzed using the ORNL finite element computer program PLACRE. Since the detailed time history of an individual bending force application as it occurred in a test could be rather complicated and difficult to determine but can have an important effect on the experimental results, histories of loading that were intended to bound the actual histories were used in the analyses. The creep ratchetting strains at the end of each test were consistently lower than the calculated values. The latter, in turn, were less than the results given by using Bree analyses. In the case of plastic ratchetting, the calculated strains were less than the reported values for two of the three cases examined. Bree analyses gave zero ratchetting strains for these two cases. Generally, the calculated and measured ratchet strains were in reasonable agreement for only a few cases.

Additional analytical studies on pipe ratchetting were initiated to examine the influence of the rules proposed by ORNL for treating cyclic plastic and creep strains. To facilitate these studies, a finite difference heat transfer subroutine was incorporated into the program PLACRE. The results obtained are presently being compared with previously obtained results. Since a very short computer running time per ratchet cycle is realized with this modified program, it is a good candidate for conducting parametric studies.

Inelastic analyses for two of the beam specimens, B3 and B4, were carried out during this period. Agreement between calculated and experimental results for periods in which creep occurred was reasonable. Comparison studies such as these clearly show the importance of having a creep law which gives good representations of behavior in the low stress range, that is, for stresses within design stress ranges. In the case of purely elastic-plastic behavior, both bilinear and nonlinear uniaxial stress-strain representations were used, but neither predicted the cyclic behaviors very well. These analyses indicate that in the bilinear case, only strains up to the maximum strain incurred should be considered for establishing the bilinear approximation. In general, when considering comparison studies of this type, it should be remembered that, based on the available information and associated design technology as it presently exists, predictions of detailed features of inelastic behavior are not to be expected, except in very special cases.

The first test of a beam, B5, from the reference heat type 304 stainless steel is underway at 1100°F. This specimen and a companion specimen, B6, are to be subjected to the same type loading histories as were B3 and B4 from the preliminary heat, except that the test temperature is 1100°F instead of 1200°F. A first attempt to test a beam made from the reference heat material was terminated after 475 hours because of a support problem and a temperature controller failure. The specimen was replaced.

Construction of the facility for use in conducting inelastic deformation studies on circular flat plates is nearing completion. The type 304 stainless steel specimens to be tested will be 1/2-in. thick with a 20 3/4 in. O.D.; they will be simply supported at the edges and loaded through a boss at the center. Deflection measurements are to be made through the use of quartz rods connected to dial gages and DCDT's, as in the case of the beam tests.

Design work for the piping and sodium components for the pipe thermal ratchetting test facility was completed, and the structural design work is nearing completion. Design work on the high pressure argon pressurizing and cover gas system is in progress. Very close control of the pressure in the sodium source tank is required of this system both under static conditions and during the thermal transient portions of the test sequence. Rework and testing of valves for the sodium and argon systems are underway. All of the stainless steel material and fabricated items to be purchased were placed on order, and approximately 40% (by cost) was delivered this quarter. Individual tests are being run to determine requirements such as the number of separately controlled heater zones required to obtain desired temperature gradients in the pipe nest.

In order to obtain the reliability needed and the precision of measurements necessary, six independent strain-measuring systems or devices have been selected for use in the thermal ratchetting test. Development work is continuing on those devices for which such work is needed. The specific characteristics of each device, in so far as they are currently known, are listed in this report.

One of the failed specimens from the weldment creep rupture program, specimen 2A, was rebuilt and continued under test until failure occurred a second time. Failure initially occurred due to separation of the flat head from the cylindrical portion. The specimen was fitted with a second flat head for continuation of the test. The second failure occurred at 269 hours due to a longitudinal split in the cylindrical portion. Fabrication of a second generation of specimens was completed, and these specimens are being prepared for test. We will alter the conditions for this set in order to achieve longer times to rupture than for the previous set.

Calculations of strain distributions on the basis of measurements taken on the first set of specimens before, during, and after test have been initiated. The first computations were for a flat head, and the results obtained indicate that reasonably accurate strain determinations can be made.

In the case of the project to conduct a high-temperature moire strain analysis of an in-plane loaded, finite-width plate with a circular hole,

progress is being made in several areas. A special loading frame to prevent out-of-plane motion of the plate was designed; a preliminary test on the proposed specimen enclosure was conducted successfully to check the ability to maintain the 1100°F test temperature within $\pm 5^\circ\text{F}$; and work is being done to achieve the capability to use gratings having 500 lines per inch. Finally, the specific details of the specimen design were established.

The activities in the category of the material properties investigations have been associated with a number of aspects during this period. The anisotropy of plate material from the type 304 reference heat, as indicated by tensile tests, was studied, and microstructural studies were also carried out. Constant load creep tests are being continued, and two load change tests are underway. One is at 1100°F and the other is at 1200°F; the latter indicates a strong interaction between creep strains and plastic strains. Our experience with creep tests conducted under compressive loads shows that improvements are needed in the specimen design and extensometer attachment methods used. These factors account for some of the differences between tensile and compressive response that we have observed.

A cyclic creep test, that is, one in which the stress was cycled between positive and negative stress values, was conducted at 1100°F using a type 304 stainless steel specimen. The strain-time responses observed in the creep portions of the cycles were close to those obtained from virgin specimens for the same stress levels. These results lend support to the auxiliary rules established for use in creep calculations.

Relaxation testing was continued, and a program of short-time cyclic testing was done at room temperature. The data from these tests support our recommendation to use the tenth-cycle curve in accounting for permanent deformation history effects on elastic-plastic behavior. The influence of temperature can be important, however, and must be factored into this type of study.

The experimental studies on the effects of biaxial stress states on fatigue behavior of type 304 stainless steel at high temperatures are progressing from the baseline data acquisition to the biaxial testing phase. A tension-torsion testing apparatus is being used, and the specimen

is a hollow cylinder. Cyclic axial load only, strain-controlled tests and cyclic torsional load, angle-of-twist-controlled tests were completed. No-hold-time tests were conducted at room temperature and 1200°F in both cases, while axial load tests with 10 min hold-times were conducted at 1100 and 1200°F. Three additional axial load cumulative damage studies were also carried out, and biaxial testing is now in progress.

The contract with Teledyne Materials Research to conduct analytical ratchetting studies was extended. Under this extension, the computations are to be made using the new version of ANSYS with classical kinematic hardening, and a study is to be made of nozzle-to-spherical shell attachments. Completion of this new work was scheduled for this quarter, but a significant amount of downtime was experienced by the NYU computer system, causing the completion date to be delayed.

Design work at Atomics International on the facility for conducting creep collapse tests on piping elbows is about 80% complete. The requirement for using an argon cover gas for the test enclosure was deleted. The present indication is that the schedule for the tests under this project will be determined by the availability of test specimens.

At present, the development of an element to represent circularly curved pipe is being pursued under the project to develop a simplified inelastic design analysis method for piping systems. During the next six months, the effort on this project is to be increased so that the objectives outlined for the first year will be reached, and an initial version of the analysis tool will be made available.

1. INTRODUCTION

This is one of a continuing series of progress reports describing work under the LMFBF program task to develop a structural design technology and associated design criteria applicable to LMFBF vessels, components, and core structurals. This is the third quarterly progress report (previously reports were issued on a monthly or bimonthly basis) and it covers the period April through June 1972.

The high-temperature structural design methods program includes development of analytical methods for predicting structural behaviors of components whose geometries range from relatively simple to complex. These components may be subjected to a variety of environmental conditions (including temperature to about 1200°F, temperature changes, and sodium atmosphere) and to various types of loadings and loading and temperature histories. In the case of core structurals, irradiation effects must be considered in addition to the above-named items. The work is being performed with the participation and cooperation of industrial groups and code bodies.

In the near-term, emphasis of the program is being placed on establishing, evaluating, and providing information pertinent to interim design methods and criteria for FFTF and early LMFBF demonstration plants. Ultimately, however, criteria must be based on accurate knowledge of time- and history-dependent stress and deformation behaviors throughout a component. Analytical techniques and associated materials behavior descriptions will be identified or developed to permit detailed predictions of the long-term history-dependent structural behavior of high-temperature components. The validity of these materials behavior characterizations and analytical methods will be demonstrated by comparing theoretical predictions with results from a series of relatively simple structural tests. The central thrust of the long-range program consists, therefore, of (1) studies leading to analytical descriptions of elastic-plastic and time-dependent material behavior, (2) development of structural analysis techniques incorporating these descriptions, and (3) basic structure tests under elastic, elastic-plastic, and creep conditions for assessing the basic features of the analytical methods.

In addition to the main thrust of the program, there are several related areas of study that are being pursued. These include component-type structural testing, creep-fatigue cumulative damage criteria, the behavior of weldments in critical structural areas, ratchetting criteria, and inelastic buckling of common configurations such as piping components. Specific tasks in the near-term effort were discussed briefly in the first monthly progress report.¹

2. ELASTIC-PLASTIC BEHAVIOR STUDIES

K. C. Liu

2.1. Experimental Tests

Tests designed to investigate initial and subsequent yield surfaces in tension (σ_{11})-torsion (σ_{12}) stress space were initiated for type 304 stainless steel (Ht. 9T2796). While initial tests are at room temperature, tests at elevated temperatures are being prepared and will be performed as soon as gage factors for the special high temperature weldable strain gages discussed in previous progress reports^{2,3} are calibrated. These gages will be used for tests at temperatures above 500°F, and conventional foil strain gages will be employed for tests at lower temperatures, including room temperature. Thin-walled tubular specimens identical to those used in the previously reported biaxial cyclic loading tests³ are employed in these tests to probe the yield surface. Initial elevated temperature tests will be conducted under isothermal conditions.

The mathematical theory of plasticity defines the yield surface as a hypersurface in stress space that bounds the region in which the material responds elastically to applied loads. The study of such a boundary by experimental means is difficult because a point on the yield surface can not be identified without inducing some plastic flow, with consequent altering of the surface. Therefore, the reported sizes and shapes of yield surfaces for a given material can vary quite dramatically, depending on the experimental technique, the definition of yield, or onset of plastic flow, and the precision with which the measurements are made.

The discrepancies between results from different experimenters generally are caused primarily by differences in the definitions of yielding. In order to make systematic experimental probes of yield surfaces, various methods for experimentally defining the extent of the region of elastic response must be thoroughly examined.

In general, the methods of determining yield surfaces are categorized in four different ways. Each has its own merits and weaknesses depending upon the application. Depending upon the method selected, the number of specimens required for the determination of a yield surface may vary from a single specimen to a large number. Based on experiments reported in connection with yield surface studies the four most commonly used definitions of yielding are as follows:

1. Proportional limit method. Yielding, as defined by the proportional limit, is illustrated in Fig. 2.1(1). Unless dealing with non-linear elastic materials, this method of definition represents the true yielding of material.

2. Backward extrapolation method. The intersection of the elastic line and a line tangent to the stress-strain curve in the plastic response region is defined as yielding in Fig. 2.1(2). This method of yield definition has some drawbacks; e.g., (i) the point being defined does not represent the true yield point, (ii) the point can not be determined uniquely since the stress-strain curve in the plastic region is not linear, (iii) it requires a number of identical specimens to construct the yield surface, and (iv) it also requires an excessive amount of overstrain for its application.

3. Offset strain. An offset strain definition is most often used in engineering applications and is illustrated in Fig. 2.1(3). Yielding is defined by the intersection of the stress-strain curve and an offset line drawn parallel to the elastic line. An offset strain of 0.2% is customarily used in engineering applications. This method is very simple to apply once an appropriate amount of offset has been established. However, to apply this method to multiaxial stress conditions, it is necessary to have a meaningful definition for the measure of plastic strain. In

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Fig. 2.1. Definitions of yielding.

the case of combined tension and torsion, a generalized plastic strain defined by*

$$S = \left[\frac{2}{3} (\Delta \epsilon_{11}^p)^2 + (\Delta \epsilon_{12}^p)^2 \right]^{\frac{1}{2}} = \text{constant} \quad (2.1)$$

can be used as a scale of strain offset. $\Delta \epsilon_{11}^p$ is an increment of axial plastic strain and $\Delta \epsilon_{12}^p$ is an increment of plastic shear strain.

4. Irreversible proof strain method. Here yielding is defined as occurring when the difference in strain before and after the addition and removal of a stress increment exceeds a certain amount [see Fig. 2.1(4)].

In the present test program, the objective is to study surfaces corresponding to proportional limit stress values. The small offset strain method is to be employed in experimentally defining such surfaces. Since the proportional limit can not be detected without inducing a small amount of plastic strain each time there is contact of the stress point with the surface (each probe) a small strain value, as determined by Eq. (2.1), will be used as a criterion for establishing a point on the surface. This measure of offset strain will be restricted to quite small values in order to limit distortion of the yield surface due to plastic deformation. Clearly, this method can be used to compare various surfaces, such as one defined by proportional limit stresses and one defined by the small offset strain. By tacitly assuming that the yield surfaces examined by this small offset method are not to be affected by the small plastic strains induced, the method chosen has an important practical advantage; namely, the mapping of the yield surface can be carried out through the use of only one specimen.

2.2. Theoretical Studies

A more systematic and complete study of the biaxial test data discussed in the last progress report³ was carried out during this reporting

*This definition is not unique but, through the von Mises yield condition formulation, it represents a measure consistent with that for combined stress.

period. In the series of biaxial tests, specimens were loaded such that the stress trajectories were along five radial paths in the tension (σ_{11})-torsion (σ_{12}) coordinate system shown in Fig. 2.2; the paths were evenly spaced in two quadrants of the stress space. Axial and shearing components of plastic strain were calculated from the test data and the plastic strain trajectories corresponding to the five loading paths were plotted in a strain space whose coordinates are ϵ_{12}^p and $\sqrt{3}/2 \epsilon_{11}^p$ (Fig. 2.3). Here ϵ_{11}^p is the axial plastic strain and ϵ_{12}^p is the plastic shear strain. The von Mises surface formulation was used as the basis for selecting the coordinates for both the stress-space and the strain-space. A comparison between the experimentally determined yield points and the von Mises initial yield surface is shown in Fig. 2.2, where it may be seen that the experimental data fall reasonably close to the theoretical yield condition of von Mises.

A comparison between Figs. 2.2 and 2.3 shows that the direction of each plastic strain trajectory closely follows the direction of the corresponding loading path. Because of this colinearity of trajectories the following relationship between stresses and plastic strain increments may be written:

$$d\epsilon_{ij}^p = d\lambda(S_{ij} - \alpha_{ij}) . \quad (2.2)$$

Here S_{ij} are the deviatoric stress components, α_{ij} is a tensor representing the total translation of the initial yield surface governed by the classical kinematic hardening rule, and λ is a function of ϵ_{ij}^p , in this special case of combined radial loadings. The applicability of Eq. (2.2) for other than combined radial loadings has not been verified for this material and, hence, requires study.

As noted above, the initial yield surface is reasonably well described by the von Mises yield function, that is,

$$f = \frac{1}{2} (S_{ij} - \alpha_{ij})(S_{ij} - \alpha_{ij}) = k_o^2 \quad (2.3)$$

where k_o is a constant. The yield function (2.3) is simplified to the familiar expression

Fig. 2.2. Cyclic radial loading paths.

Fig. 2.3. Plastic strain trajectories.

$$f = \frac{1}{2} S_{ij} S_{ij} = k_o^2 \quad (2.4)$$

for initial yielding since $\alpha_{ij} = 0$ for this case. By adjusting the von Mises formulation for use in the description of the initial and subsequent yield surfaces, some interesting and relevant observations can be made. It may be seen, that on the basis of the appropriateness of Eq. (2.3) for describing yield surfaces, Eq. (2.2) supports the normality postulate, which is given by

$$d\epsilon_{ij}^p = d\lambda \frac{\partial f}{\partial S_{ij}}, \quad (2.5)$$

where f is the yield, or loading, function.

When the inner product of Eq. (2.5) is taken, we have

$$d\epsilon_{ij}^p d\epsilon_{ij}^p = (d\lambda)^2 \frac{\partial f}{\partial S_{ij}} \frac{\partial f}{\partial S_{ij}} \quad (2.6)$$

or we may write

$$(d\epsilon_{ij}^p d\epsilon_{ij}^p)^{\frac{1}{2}} = d\lambda [(S_{ij} - \alpha_{ij})(S_{ij} - \alpha_{ij})]^{\frac{1}{2}} \quad (2.7)$$

and

$$= \sqrt{2} k_o d\lambda \quad (2.8)$$

in which the relation (2.3) has been used. In the case of combined tension and torsion, Eq. (2.8) reduces to

$$\left[\frac{3}{4} (d\epsilon_{11}^p)^2 + (d\epsilon_{12}^p)^2 \right]^{\frac{1}{2}} = k_o d\lambda \quad (2.9)$$

since

$$d\epsilon_{ij}^p = \begin{vmatrix} d\epsilon_{11}^p & d\epsilon_{12}^p & 0 \\ d\epsilon_{12}^p & -\frac{1}{2}d\epsilon_{11}^p & 0 \\ 0 & 0 & -\frac{1}{2}d\epsilon_{11}^p \end{vmatrix}$$

in which incompressibility and isotropy of the material are assumed. The left hand side of Eq. (2.9) represents an incremental distance, $d\bar{\epsilon}^p$,

measured in the radial direction of the strain space considered in Fig. 2.3. Substituting these quantities into Eq. (2.9) gives

$$d\lambda = \frac{1}{k_0} d\bar{\epsilon}^P . \quad (2.10)$$

For a work-hardening material, the kinematic hardening rule assumes that

$$d\alpha_{ij} = g d\epsilon_{ij}^P , \quad (2.11)$$

where g is a function of plastic strains ϵ_{ij}^P . If linear work-hardening is assumed, g is a constant. Substituting Eqs. (2.6) and (2.9) into the above equation, we have

$$d\alpha_{ij} = g \frac{d\epsilon^P}{k_0} \frac{\partial f}{\partial S_{ij}} ; \quad (2.12)$$

again, taking the inner product of Eq. (2.12) we obtain

$$d\bar{\alpha} = g d\bar{\epsilon}^P , \quad (2.13)$$

where $\bar{\alpha}$ is defined as

$$d\bar{\alpha} = \left[\frac{1}{2} d\alpha_{ij} d\alpha_{ij} \right]^{\frac{1}{2}} . \quad (2.14)$$

In a previous progress report⁴ the function g is assumed as a function of the second invariant of the plastic strain tensor, that is,

$$g = g(II) = g(\bar{\epsilon}^P) , \quad (2.15)$$

where

$$II = \frac{1}{2} \epsilon_{ki}^P \epsilon_{ki}^P = \frac{3}{4} \left[\int d\epsilon^P \right]^2 = \frac{3}{4} (\bar{\epsilon}^P)^2 \quad (2.16)$$

for these special combined radial loadings. Upon substituting Eq. (2.15) into Eq. (2.13), we obtain

$$g(\bar{\epsilon}^P) = \frac{d\bar{\alpha}}{d\bar{\epsilon}^P} . \quad (2.17)$$

The form of function g can be deduced if the relationship between $\bar{\alpha}$ and $\bar{\epsilon}^P$ can be found.

In the case of combined radial loadings, if the subsequent yield surface translates in accord with the classical kinematic hardening rule the following relation

$$d\alpha_{ij} = dS_{ij} \quad (2.18)$$

should hold. Consequently, we obtain that

$$d\bar{\alpha} = \left[\frac{1}{3} (d\sigma_{11})^2 + (d\sigma_{12})^2 \right]^{\frac{1}{2}} \quad (2.19)$$

since

$$dS_{ij} = \begin{vmatrix} \frac{2}{3} d\sigma_{11} & d\sigma_{12} & 0 \\ d\sigma_{12} & -\frac{1}{3} d\sigma_{11} & 0 \\ 0 & 0 & -\frac{1}{3} d\sigma_{11} \end{vmatrix}. \quad (2.20)$$

The right hand side of Eq. (2.19) is equivalent to the radial stress increment measured in the stress space (Fig. 2.2).

Plots of $\bar{\alpha}$ versus $\bar{\epsilon}^p$ for each of the radial paths are shown in Fig. 2.4, where it is seen that the fundamental relationship (2.17) for a given segment of loading does exist for all radial loading paths. Furthermore, it is observed from the results shown in Fig. 2.4 that the form of function g initially proposed in Ref. 4 applies in this case.

3. TIME-DEPENDENT BEHAVIOR STUDIES

C. E. Pugh

3.1. Multiaxial Creep Studies

Professor W. N. Findley
Brown University

3.1.1. Work completed during the month of April

1. Specimen number 1 of 304 stainless steel (Ht. 9T2796) was mounted and testing under combined tension and torsion was started at a combined stress of $\sigma = 10,628$ psi and $\tau = 6111$ psi at a temperature of 1103°F . The von Mises effective stress value for this stress state is 15 ksi.

Fig. 2.4. $\bar{P}$ vs $\bar{\epsilon}^P$ relation.

2. New difficulties were encountered in this experiment which were not observed in preliminary work. The previous setup resulted in averaging the temperature from one side to the other of the specimen. In this test, the temperatures at particular points were measured. It was possible to obtain nearly uniform temperature distribution along one side of the specimen, but not both. Thus the variation over the specimen was $\pm 8^{\circ}\text{F}$. The extreme of time-wise fluctuation of temperature was $\pm 5^{\circ}\text{F}$. It was found that the positioning of the thermal shielding (aluminum foil) affected the temperature distribution considerably. The problem results in part from low thermal conductivity of the material, together with the fact that we are heating from the inside and have thermocouples on the outside of the specimen. In the next setup we will attempt to place the control couple on the inside and provide adjustable shielding on the outside.
3. A phaser used in temperature control failed just before testing and a replacement was ordered. At the high loads involved, an oil leak in the thrust bearing leads to a dangerous situation. The problem presented was partially solved by providing a smoke detector to cause automatic shut down of the equipment in the event of trouble. A second safety measure, that is a means for shutting down the oil pump in the event of lamp failure, was considered necessary and a device for this purpose is being installed.
4. A new simplified temperature control system was devised which minimizes the cost of equipment for temperature control.
5. The following equipment has been ordered:
 - a. Digital temperature indicator.
 - b. Two phasers (received).
 - c. Two variacs (received).
6. Work on specimen manufacture has continued.
7. Some early data from the experiment mentioned in item 1 above are:

<u>Time</u> <u>hr.</u>	<u>Tensile Strain ϵ_{11}</u> <u>%</u>	<u>Shearing Strain ϵ_{12}</u> <u>%</u>
0.1	0.3137	0.2518
1.0	0.3532	0.2863

During these early test times, the shape of the ϵ_{11} and ϵ_{12} plots are nearly identical. This means that the strain increment vector lies between the normal to the Mises ellipse and the normal to the Tresca ellipse under this state of stress. This agrees with the observation for test C1 of an aluminum alloy under the same state of stress as shown in Fig. 15 of Reference 5.

3.1.2. Work completed during the month of May

1. Specimen number 1 of 304 stainless steel has continued under test without difficulty, except that the temperature distribution has apparently become worse (see item 2 below). Completion of 1000 hours of testing will occur about June 15. A few additional strain readings from this test are as follows:

Time hr.	Tensile Strain ϵ_{11} %	Shearing Strain ϵ_{12} %
497	0.6936	0.6070
810	0.7373	0.6380

2. Investigations of another setup showed that considerable improvement in time-wise temperature variation could be achieved by welding the control thermocouple inside the test specimen. It was also found that the separated-junction thermocouples being used on specimen number 1 do not give as consistent readings as welded-junction couples for the stainless steel at 1100°F. It has also been found that welded-junction couples placed close together do not always give consistent readings. This problem will be explored further.
3. A different type thermal shielding is being investigated, together with the possibility of mounting the circumferential extensometer on the shielding.
4. A safety device to shut down the oil pump in the event of lamp failure has been installed.
5. Ten additional specimens of 304 stainless steel have been rough machined and are being heat treated.
6. A second tension-torsion machine will be available for an undetermined period. Another specimen and associated apparatus are being prepared for a 1000 hr creep test under combined tension and torsion in this machine.

3.1.3. Work during the month of June

1. The combined tension-torsion test of specimen number 1 has been completed. The final time and strain readings are:

Time hr.	Tensile Strain ϵ_{11} %	Shearing Strain ϵ_{12} %
1002.5	.7534	.6571

Strain versus log (time) diagrams for this test are shown in Figs.

3.1 and 3.2. The shear strain (ϵ_{12}) is plotted vs the tensile strain (ϵ_{11}) in Fig. 3.3 to explore the possible existence of a creep surface for the material. The slope of this line gives the direction of the strain rate vector. It was used to derive the elliptic equation:

$$\sigma^2 + 3.2\tau^2 = (15246)^2 \quad (3.1)$$

passing through the test point. Eq. (3.1) can be compared (see Fig. 3.4) with the von Mises and Tresca relations for tension-torsion, passing through the test point, $\sigma = 10,628$ psi and $\tau = 6,111$ psi, as follows:

$$\sigma^2 + 3\tau^2 = (15000)^2 \quad \text{Mises} \quad (3.2)$$

$$\sigma^2 + 4\tau^2 = (16197)^2 \quad \text{Tresca} \quad (3.3)$$

This result suggests that the creep surface lies between Mises and Tresca but is closer to Mises.

2. Following the 1000-hr test on specimen number 1 several short-time experiments were conducted, and these are listed in chronological order in Table 3.1. The amount of recovery observed was quite small. Analysis and interpretation of these results are not complete, but available plots of strain versus log (time) are shown in Figs. 3.5 - 3.10.
3. Ten additional specimens have been heat treated. Of these, two have been finish-machined and are ready for testing.
4. A second tension-torsion machine has been prepared and a specimen is being mounted for a test under combined tension (13,975 psi) and torsion (3,407 psi). This stress state corresponds to a point on the curve given by Eq. (3.1) and is shown in Fig. 3.4.

Fig. 3.1. Tensile creep strain vs time under combined tension and torsion. ($\sigma = 10,628$ psi, $\tau = 6,111$ psi). Test segment 1.

Fig. 3.2 Torsional creep strain vs time under combined tension and torsion ($\sigma = 10,628$ psi, $\tau = 6,111$ psi). Test segment 1.

Fig. 3.3. Torsional creep strain vs tensile creep strain under combined tension and torsion. ($\sigma = 10,628$ psi, $\tau = 6,111$ psi). Test duration = 1000 hours. Test segment 1.

Fig. 3.4. Creep surfaces in two-dimensional stress space.

Table 3.1. Short-time experiments conducted on type 304 stainless steel (Heat 9T2796)
specimen No. 1 subsequent to 1000 hours of test under sustained multiaxial stresses

Test segment	Reference figure	Stress state, (σ, τ) psi	Duration hr.	Final tensile strain, ϵ_{11} , %	Final shear strain, ϵ_{12} , %
2-Recovery	3.5	(0,0)	119.50	.6969	.6094
3-Combined Ten.-Tor.	3.6	(10628, 6111)	5.35	.7519	.6548
4-Recovery	3.7	(0,0)	43.05	.6969	.6114
5-Tension	3.8	(15584, 0)	22.00	.8482	.6052
6-Recovery	Not plotted	(0,0)	3.20	.7741	.6059
7-Torsion	3.9	(0, 8356)	21.50	.7577	.6958
8-Torsion	3.9	(0, 9190)	96.40	-	.7836
9-Recovery	3.10	(0,0)	1.00	.7607	.7216

Fig. 3.5. Recovery of creep strains upon removal of multiaxial loadings ($\sigma = 10,628$ psi and $\tau = 6,111$ psi) which had been sustained for 1002 hours. Test segment 2.

Fig. 3.6. Tensile and torsional creep strains upon reapplying combined tension and torsion loads ($\sigma = 10,628$ psi and $\tau = 6,111$ psi). Test segment 3.

Fig. 3.7. Recovery of creep strains upon second removal of multiaxial loadings. Test segment 4.

Fig. 3.8. Tensile and torsional creep strains under applied tensile load ($\sigma = 15,584$ psi). Test segment 5. (Test segment 6, recovery of creep strains upon removal of tensile load, not plotted.)

Fig. 3.9. Torsional creep and strains for two successively applied pure torsional loads ($\tau = 8356$ psi and $\tau = 9190$ psi). Test segments 7 and 8.

Fig. 3.10. Recovery of torsional creep strains upon removal of torsional loads. Test segment 9.

5. Another specimen is being prepared for testing in the first machine at combined tension (5570 psi) and torsion (7934 psi). This stress value, like the one given above, was selected to lie on the curve indicated by the first test, Eq. (3.1).
6. Additional experiments have been performed to improve temperature distribution. It is planned to calibrate all thermocouples at the test temperature against a Bureau of Standards thermocouple which we have borrowed.

3.2. Investigations of Creep Failure Under Uniaxial and Multiaxial Conditions

C. C. Schultz
Babcock and Wilcox

3.2.1. Overall program

The last progress report⁶ stated that a subcontract had been awarded to the Babcock and Wilcox Company for studies of failures under uniaxial and multiaxial creep condictions, and these were to be conducted by the Research and Development Division of B & W. A more specific description of the activity is given in the following paragraphs. The types of experimental tests planned are discussed, and a test matrix is shown for initial uniaxial tests. Testing of multiaxial specimens will begin approximately three months after initiation of uniaxial tests.

(1) a. Uniaxial Rupture Tests

Constant load, step load and interrupted tests are to be conducted. The primary, general objective of these tests is to characterize the uniaxial rupture behavior. A secondary objective is to characterize the long-time deformation behavior.

Both uniaxial creep machines and uniaxial rupture machines are to be used for these tests. Fabrication of specimens for the creep machines requires 5/8-in. diam by 6-in. long bar stock. Direct Current Differential Transformer (DCDT) axial strain measurements will be made over the 3-in. gage length, with digital output. The maximum time span between measurements will be 8 hours, and the temperature control is to be within plus or minus 1.5°F.

The rupture machine specimen geometry requires a 5/8-in. diam by 4-1/2-in. long rough bar stock. Monitoring of deformation will be limited to daily (except weekends) dial gage readings. Temperature control (over the 2-in. test section) is to be within plus or minus 3°F.

A total of five creep machines are to be used. During the first year of testing up to 11 rupture machines would be used. In subsequent years up to eight rupture machines would be used. All tests are to be at a constant temperature, which during the first year is to be 1100°F.

b. Constant Load Tests

The specific objective of this test series is to establish the 1100°F rupture curve. The rupture curve is then the basis for planning tests and evaluating the data in the other test series. All tests will go to rupture with periodic deformation readings as outlined above. The test matrix is shown in Table 3.2.

The temperature level of 1100°F has been selected on the basis of minimizing both test time and short-time inelastic (plastic) strains.

c. Interrupted Rupture Tests

The specific objective of this test series is to attempt to define the contributions of necking, grain boundary cracking, and loss of strength to tertiary creep and rupture.

The concept is to run several tests at a single temperature and stress level. The tests are then terminated at various stages of life for destructive examination. It is proposed to terminate two specimens at each of the following approximate life usages: 50 percent, 65 percent, and 80 percent. At each usage level one specimen would be used for micro-structure examination and one for short-time tensile testing to failure. In addition, a single specimen at near end of life is to be used for micro-examination. All specimens terminated prior to rupture will be measured to define shape changes due to the prior straining.

After approximately a year of testing, the results of this initial series of interrupted tests can be reviewed to determine future interrupted tests.

d. Life Fraction Tests

The specific objective of this test series is to evaluate the life fraction rules. Also of interest is to determine the effect of load changes

Table 3.2. Initial uniaxial test matrix -
uniaxial rupture - type 304 stainless steel -
T = 1100°F

<u>CREEP MACHINES</u>					
<u>#1C</u>	<u>#2C</u>	<u>#3C</u>	<u>#4C</u>	<u>#5C</u>	
BWC-1	BWC-2	BWC-3	BWC-4	BWC-5	
30 ksi	28 ksi	25 ksi	22 ksi	20 ksi	
BWC-6 ⁽¹⁾	BWC-7 ⁽¹⁾	BWC-8 ⁽¹⁾	BWC-9 ⁽²⁾	BWC-10 ⁽³⁾	
16 ksi	17 ksi	18 ksi	4.5 khrs		
<u>RUPTURE MACHINES</u>					
<u>#1R</u>	<u>#2R</u>	<u>#3R</u>	<u>#4R</u>	<u>#5R</u>	<u>#6R</u>
BWR-1	BWR-2	BWR-3	BWR-4	BWR-5	BWR-6
30 ksi	28 ksi	25 ksi	22 ksi	20 ksi	19 ksi
BWR-7	BWR-8	BWR-9	BWR-15 ⁽²⁾	BWR-16 ⁽²⁾	BWR-17 ⁽²⁾
16 ksi	17 ksi	18 ksi	6 khrs	6 khrs	4.5 khrs
<u>#7R</u>	<u>#8R</u>	<u>#9R</u>	<u>#10R</u>	<u>#11R</u>	
BWR-10 ⁽¹⁾	BWR-11 ⁽¹⁾	BWR-12 ⁽¹⁾	BWR-13 ⁽²⁾	BWR-14 ⁽²⁾	
16 ksi	17 ksi	18 ksi	7.5 khrs	7.5 khrs	

1. All but one stress level will become life fraction specimens.

2. Interrupted rupture test series.

3. Life fraction test.

on the initiation of tertiary creep. In addition, time-deformation data, useful in the evaluation of the effects of load changes on the creep curve, will be obtained.

Positive and negative load changes will both be considered, although only tensile loading will be used.

(2) a. Prelife Examination

This workphase involves the destructive testing of the specimens terminated in the interrupted rupture test series.

b. Microstructure Examination

These tests are aimed at defining loss of load-supporting area due to grain boundary cracking. The objectives are to determine if crack growth or coalescence predominates; if the predominate feature accelerates with time; if the process is continuous or does it initiate, say during third stage creep. The equipment to be used is an optical microscope and/or a scanning electron microscope.

c. Short-Time Tensile Tests

These tests are to determine if the short-time strength and ductility can be related to the prior creep exposure. The tests are to be at 1100°F with monotonic loading to failure.

(3) a. Multiaxial Tubular Rupture Tests

The general objective of these tests is to develop methods for extending uniaxial rupture data to multiaxial applications. The specimen geometry is basically a hollow circular cylinder and fabrication requires a 1-3/4-in. diam by 26-in. long rough bar stock. Monitoring of axial deformation will be limited to daily (except weekends) dial gage readings. All tests will be conducted at constant temperature. Temperature control will be within plus or minus 2°F. A total of four machines will be used.

b. Standard

The specific objective of these tests is to evaluate the effects of multiaxial states of stress and stress gradients. All tests are to be at constant load and constant temperature. During approximately the first year, 1100°F will be the test temperature. The test parameters, ratio of axial membrane stress to circumferential membrane stress and diameter to thickness ratio will be varied. A test matrix will be established in conjunction with ORNL. Testing will continue for the term of the subcontract.

c. Nonproportional

The specific objective of these tests is to evaluate material degradation in a direction other than the direction of maximum stress.

Essentially thin-walled specimens will be used. The OD is limited by furnace dimensions to approximately 1-1/4 in. and machining capability limits the wall thickness. The ratio of axial to circumferential stresses will be step changed during the tests to alter the direction of the maximum stress.

(4) a. Laboratory Anneal

Subsequent to machining all test specimens are to have the anneal being used throughout the ORNL program. The anneal consists of holding the specimen at 2000°F for 30 min in an argon atmosphere and then rapid cooling the specimen.

(5) a. Data Reduction and Analysis

The objective of this task is to reduce data from the previous tasks to a suitable form for transmittal and to evaluate methods of application of the data.

b. Data System and Statistics

The data processing system will be modified to allow storage of data on magnetic tape in order to facilitate accessibility to related data. Statistical support will be provided for both test design and evaluation of the significance of test results.

c. Rupture Analysis

This work phase is concerned with the analysis and use of the data in evaluating methods of predicting multiaxial rupture. The initial activity will be a literature review, followed by uniaxial modeling and correlations. The end result to be sought is the prediction of expended life (on an incremental volume basis) as a function of the stress and strain histories as predicted by inelastic analysis methods.

(6) a. Material Requirements

The material to be used is bar stock from the ORNL reference heat (No. 9T2796) of type 304 stainless steel. The uniaxial tests will use specimens from nominal 2 1/32-in.-diam bars, and the multiaxial specimens will be machined from nominal 2-in.-diam bar.

3.2.2. Monthly report for April 1972

A partial shipment of material, consisting of 4 bars (48') nominally 2 1/32 in. in diameter and 3 bars (36') nominally 2 in. in diameter, was received by the Alliance Research Center (ARC) in March. Preparation of the test specimens has been initiated and furnace modifications for the laboratory anneal are proceeding. The initial program discussed in Subsection 3.2.1, reflects agreements on the work scope arrived at through discussion between ARC and ORNL. A draft of the program, as revised from the initial B & W proposal, was prepared for forwarding to ORNL. Mr. W. E. Leyda of the Metallurgy Section of ARC will be responsible for data acquisition for this program.

3.2.3. Monthly report for May 1972

Twenty-seven threaded (rupture machine) and twenty-eight button head (creep machine) specimens have been machined and are awaiting laboratory anneal.

Our intended procedure for obtaining the laboratory anneal is to encapsulate specimens in an argon atmosphere through the use of commercially available containers (SEN/PAK). After 30 min at 2000°F the whole package will be water quenched.

This procedure is selected because of our doubts about having sufficient cooling capability (in our vacuum furnace) to obtain fast enough cooling rates with the large tubular specimens. With the procedure outlined above the uniaxial specimens could be annealed with better control as well as minimize cost. Possible distortion of the tubular specimens during the anneal is still being considered.

3.2.4. Monthly report for June 1972

As stated in the monthly report for May (see Subsection 3.2.3), two bars (#2 and #7) have been machined into uniaxial specimens. The specimens are identified with respect to both bar number and location within the bar. Thus specimens 2-1 through 2-14 are the threaded specimens from bar #2, and 2-15 through 2-28 are the button-head specimens from the same bar.

Table 3.3 shows the specimen assignments made thus far for the initial test series. The remaining assignments will be made after review of initial results.

Thus far the following specimens have had the required laboratory anneal: Specimens 7-1 through 7-8, 7-11, 7-14 through 7-28, 2-1, 2-4, 2-5, 2-14 and 2-22. During the anneal of specimens 7-1 through 7-7 a furnace control failure occurred. While repairs were made these specimens were held for approximately 45 minutes at a temperature which varied between 1920 and 1980°F. Following repair, the 30 minute hold-time at 2000°F was obtained. The "heat-tint" on these specimens is slightly heavier than on our other specimens, but the probability of damage is thought to be very low. Nevertheless, the assignment of specimens has been made such that the adequacy of these specimens can best be determined early in the program.

Tests BWC-1 and BWR-1 (both at 30,000 psi) are in progress. A DCDT was added to rupture machine R1 to provide more frequent displacement measurements than possible with the dial gage (the dial gage is still being used as backup). This DCDT is measuring grip displacement (as with dial gage) rather than actual specimen strain as in the modified creep machines. Similar DCDT setups are being installed on five more of the rupture machines. This DCDT arrangement will not increase accuracy, but will provide for more frequent readings during primary creep and throughout the life of the high stress tests.

3.3. ORNL Studies of Time-Dependent Behavior

C. E. Pugh

It was stated in the last quarterly progress report⁶ that efforts were continuing to establish a creep law from the available information on the ORNL preliminary heat (No. 8043813) of type 304 stainless steel at 1200°F. These efforts have been along two lines. The first is based on providing a best representation of all available constant-load creep data for this material. The second considers only the creep data for test times up to 1000 hours. The intent of this second approach is to provide a representation over this limited time range. Specific examples of potential use

Table 3.3. Initial specimen assignment for tests at 1100°F

<u>CREEP MACHINES</u>					
Machine No.	<u>#1C</u>	<u>#2C</u>	<u>#3C</u>	<u>#4C</u>	<u>#5C</u>
Test No.	BWC-1	BWC-2	BWC-3	BWC-4	BWC-5
Stress	30 ksi	28 ksi	25 ksi	22 ksi	20 ksi
Specimen No.	(7-15)	(2-22)	(7-28)	(2-28)	(2-15)
	BWC-6	BWC-7	BWC-8		
	16 ksi	17 ksi	18 ksi		
	(2-16)	(2-18)	(7-16)		

<u>RUPTURE MACHINES</u>						
Machine No.	<u>#1R</u>	<u>#2R</u>	<u>#3R</u>	<u>#4R</u>	<u>#5R</u>	<u>#6R</u>
Test No.	BWR-1	BWR-2	BWR-3	BWR-4	BWR-5	BWR-6
Stress	30 ksi	28 ksi	25 ksi	22 ksi	20 ksi	19 ksi
Specimen No.	(7-1)	(2-1)	(7-8)	(7-7)	(2-14)	(7-14)
	BWR-7	BWR-8	BWR-9			
	16 ksi	17 ksi	18 ksi			
	(2-4)	(2-5)	(7-11)			
	<u>#7R</u>	<u>#8R</u>	<u>#9R</u>			
	BWR-10	BWR-11	BWR-12			
	16 ksi	17 ksi	18 ksi			
	(2-11)	(2-12)	(7-12)			

of the second creep law are the studies of beams B3 and B4 discussed in Chapter 4 of this report. Since the studies of structural analyses and tests discussed in Chapter 4 are examining overall capabilities of analysis methods, it is important that the creep law used be one which represents material behavior data for the appropriate ranges of stress and time. In this case, 1000 hours is chosen as an appropriate time span.

Preliminary versions of creep laws for this material and temperature were presented and discussed in two previous progress reports.^{6,7} Since those earlier considerations, some additional data have become available and have now been factored into considerations. Specifically, the test numbers, stress levels and test durations for the experiments now available for use in creep law development at 1200°F are shown in the first three columns of Table 3.4. The form of the creep law considered is

$$\epsilon(\sigma, t) = A(\sigma) [1 - e^{-r(\sigma)t}] + K(\sigma)t, \quad (3.4)$$

where $A(\sigma)$ is the magnitude of the primary creep, $r(\sigma)$ the reciprocal retardation time and $K(\sigma)$ the secondary creep rate.

Since the last report,⁶ one point has been added to the available secondary creep rate, $K(\sigma)$, values, and all the points used in developing a correlation for $K(\sigma)$ are shown in Table 3.4. The expression developed is

$$K(\sigma) = \dot{\epsilon}_{\min} = 7.91 \times 10^{-11} [\sinh(0.1932 \sigma)]^{4.0}, \quad (3.5)$$

where $\dot{\epsilon}$ is in (in./in./hour) and σ in (ksi).

In efforts to determine a representation of the primary creep, Eq. (3.4) was used to establish a fit, in the sense of least squares, to each individual creep curve. The secondary creep rate given in Eq. (3.5) was used in this process. Table 3.4 shows the values of $A(\sigma)$ and $r(\sigma)$ that were determined simultaneously in this manner from consideration of all data and from consideration only of data up to 1000 hours. Note that when there is more than one test for the same stress value, the data were combined to provide only one A and one r value for that particular stress.

Test P-68, a 6 ksi creep test, is listed in Table 3.4 but was not used directly in creep law determinations because of the uncertainty associated with establishing nominal creep behavior at this low stress level. Limited

Table 3.4. Constant load creep tests - Equation (3.4) coefficients from least squares fits to creep curves - type 304 stainless steel (Ht. 8043813) at 1200°F

Test number	Stress ksi	Duration hours	All data		Data for $t \leq 1000$ hours		
			A (in./in.)	r (hours) ⁻¹	A (in./in.)	r (hours) ⁻¹	
P-68 ^a	6	1000	3.752×10^{-4}	8.742×10^{-3}	3.752×10^{-4}	8.742×10^{-3}	
P-32	8	2010	1.397×10^{-3}	3.509×10^{-3}	1.239×10^{-3}	4.878×10^{-3}	
P-37	8	505					
P-17	10	2003	3.464×10^{-3}	3.693×10^{-3}	3.112×10^{-3}	4.815×10^{-3}	
P-33	10	503					
P-34	10	502					
P-16	12.5	2010	5.111×10^{-3}	9.562×10^{-3}	4.798×10^{-3}	1.198×10^{-3}	
P-39	12.5	504					
P-15	15	1995	7.380×10^{-3}	1.684×10^{-2}	7.240×10^{-3}	1.803×10^{-2}	4.0×10^{-7}
P-40	15	505					
P-5	17	5033	1.529×10^{-2}	6.207×10^{-3}	1.329×10^{-2}	1.225×10^{-2}	2.2×10^{-6}
P-3 ^b	20	684	2.553×10^{-2}	1.456×10^{-2}	2.553×10^{-2}	1.456×10^{-2}	4.15×10^{-5}
P-2 ^b	22	298	3.087×10^{-2}	4.219×10^{-2}	3.087×10^{-2}	4.219×10^{-2}	1.25×10^{-4}
P-1 ^b	25	70	6.023×10^{-2}	2.517×10^{-2}	6.023×10^{-2}	2.517×10^{-2}	1.79×10^{-3}
P-4 ^{b,c}	28	17					7.64×10^{-3}

^aTest P-68 has not been used directly in creep law determinations, but it has been used to compare with predictions of creep laws.

^bOnly data up to the onset of third stage creep are considered.

^cTest P-4 was not used in determining $A(\sigma)$ and $r(\sigma)$ values due to the abnormally low value of primary creep.

results are available from a total of three tests at 6 ksi and are illustrated in Figs. (3.11) and (3.12). Although Fig. (3.12) shows the behavior of test P-68 to be reasonably nominal over the indicated time span, the two tests in Fig. (3.11) show that essentially zero creep strain occurred at 6 ksi. Test P-68 has, therefore, been used only to compare with predictions of creep laws established on the basis of results from tests at other stress levels. Some of these comparisons are presented in following paragraphs.

With this as a starting point, consideration has been given to several possible representations for $A(\sigma)$ and $r(\sigma)$. For each possibility that has been explored, two creep equations have been developed; one on the basis of all the data (labelled henceforth with a letter "A") and one on the basis of data up to 1000 hours (labelled henceforth with a letter "B"). Three of the sets of creep laws that have been examined are discussed in the following paragraphs.

3.3.1. Creep laws 1-A and 1-B

Least squares fitting of power law expressions to the $A(\sigma)$ and $r(\sigma)$ values given in Table 3.4 appears reasonable and gives

Creep Law 1-A

$$\begin{aligned} A(\sigma) &= 1.98 \times 10^{-6} \sigma^{3.15} \\ r(\sigma) &= 5.52 \times 10^{-5} \sigma^{1.94} \\ K(\sigma) &= \dot{\epsilon}_{\min} = 7.91 \times 10^{-11} [\sinh (0.1932\sigma)]^{4.0} \end{aligned} \quad (3.6)$$

Creep Law 1-B (uses 1000 hour data)

$$\begin{aligned} A(\sigma) &= 1.39 \times 10^{-6} \sigma^{3.26} \\ r(\sigma) &= 3.41 \times 10^{-5} \sigma^{2.09} \\ K(\sigma) &= 7.91 \times 10^{-11} [\sinh (0.1932\sigma)]^{4.0} \end{aligned} \quad (3.7)$$

In Eqs. (3.6) and (3.7), as with all the creep laws to be discussed, ϵ is in (in./in.), σ in (ksi), and time in (hours).

Figures (3.13) through (3.16) show plots of each of the tests listed in Table 3.4. The points in these figures represent data, the solid lines are least squares fits to the particular creep curves [assuming a secondary

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Fig. 3.11 Creep data for two variable stress tests for type 304 stainless steel (Heat 8043813) at 1200°F. Initial test segments use $\sigma = 6$ ksi. Solid lines represent nominal behavior for constant stresses of 8 and 10 ksi.

Fig. 3.12. Measured axial creep strain for type 304 stainless steel (ORNL preliminary heat) specimen tested at 6 ksi and 1200°F.

Fig. 3.13a. Measured and predicted creep strains for type 304 stainless steel (ORNL preliminary heat) specimens tested at 6 and 10 ksi and 1200°F.

Fig. 3.13b. Measured and predicted creep strains for type 304 stainless steel (ORNL preliminary heat) specimens tested at 8 ksi and 1200°F.

Fig. 3.14a. Measured and predicted creep strains for type 304 stainless steel (ORNL preliminary heat) specimens tested at 10 ksi and 1200°F.

Fig. 3.14b. Measured and predicted creep strains for type 304 stainless steel (ORNL preliminary heat) specimens tested at 12.5 ksi and 1200°F.

Fig. 3.15. Measured and predicted creep strains for type 304 stainless steel (ORNL preliminary heat) specimens tested at 15 and 17 ksi and 1200°F.

Fig. 3.16. Measured and predicted creep strains for type 304 stainless steel (ORNL preliminary heat) specimens tested at 20, 22, and 25 ksi and 1200°F.

creep rate given by Eq. (3.5)], the long-dashed lines are the predictions of creep law 1-A, and the short-dashed lines are the predictions of creep law 1-B.

3.3.2. Creep laws 2-A and 2-B

One alternate consideration was to maintain $r(\sigma)$ fixed by the expressions given in Eqs. (3.6) and (3.7) and then obtain new values for $A(\sigma)$ by again obtaining least squares fits to the creep curves. This process gives rise to the $A(\sigma)$ values shown in the second column of Table 3.5 when all data are considered and to those values in the third column when only 1000 hour data are considered. Power law representations of $A(\sigma)$ then give rise to the following creep laws.

Creep Law 2-A

Same as 1-A [see Eq. (3.6)] except

$$A(\sigma) = 2.05 \times 10^{-6} \sigma^{3.136} \quad (3.8)$$

Creep Law 2-B (uses 1000 hour data)

Same as 1-B [see Eq. (3.7)] except

$$A(\sigma) = 2.29 \times 10^{-6} \sigma^{3.092} \quad (3.9)$$

Figures (3.17) - (3.20) are plots of the predictions of creep laws 2-A (long-dashed lines) and 2-B (short-dashed lines) for the stress levels listed in Table 3.4. Again, the points are experimental values.

3.3.3. Creep laws 3-A and 3-B

Another alternate that has been examined also considers $r(\sigma)$ fixed, but by an exponential fit to the values given in Table 3.4. Corresponding values for $A(\sigma)$ were developed by again obtaining least squares fits to the individual creep curves. Creep laws 3-A and 3-B were then completed by obtaining power law representations for $A(\sigma)$.

Fig. 3.17a. Measured and predicted creep strains for type 304 stainless steel (ORNL preliminary heat) specimens tested at 6 and 10 ksi and 1200°F.

Fig. 3.17b. Measured and predicted creep strains for type 304 stainless steel (ORNL preliminary heat) specimens tested at 8 ksi and 1200°F.

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Fig. 3.18a. Measured and predicted creep strains for type 304 stainless steel (ORNL preliminary heat) specimens tested at 10 ksi and 1200°F.

Fig. 3.18b. Measured and predicted creep strains for type 304 stainless steel (ORNL preliminary heat) specimens tested at 12.5 ksi and 1200°F.

Fig. 3.19. Measured and predicted creep strains for type 304 stainless steel (ORNL preliminary heat) specimens tested at 15 and 17 ksi and 1200°F.

Fig. 3.20. Measured and predicted creep strains for type 304 stainless steel (ORNL preliminary heat) specimens tested at 20, 22, and 25 ksi and 1200°F.

Table 3.5. Primary creep values established from least squares fitting of creep curves while maintaining $r(\sigma)$ and $K(\sigma)^a$ fixed

Stress (ksi)	$r(\sigma) = 5.52 \times 10^{-6} \sigma^{1.94}$ A (in./in.)	$r(\sigma) = 3.41 \times 10^{-6} \sigma^{2.09}$ A (in./in.)
8	1.4252×10^{-3}	1.5240×10^{-3}
10	3.2952×10^{-3}	3.2511×10^{-3}
12.5	5.2636×10^{-3}	5.3177×10^{-3}
15	7.5602×10^{-3}	7.6667×10^{-3}
17	1.5013×10^{-2}	1.3262×10^{-2}
20	2.5015×10^{-2}	2.5113×10^{-2}
22	3.2582×10^{-2}	3.2730×10^{-2}
25	5.6579×10^{-2}	5.7090×10^{-2}

$$^a K(\sigma) = \dot{\epsilon}_m = 7.91 \times 10^{-11} [\sinh (0.1932\sigma)]^{4.0}$$

Creep Law 3-A

$$A(\sigma) = 2.33 \times 10^{-6} \sigma^{3.083}$$

$$r(\sigma) = 1.354 \times 10^{-3} \exp (0.129\sigma) \quad (3.10)$$

$$K(\sigma) = \text{same as in law 1-A}$$

Creep Law 3-B (uses 1000 hour data)

$$A(\sigma) = 2.37 \times 10^{-6} \sigma^{3.076}$$

$$r(\sigma) = 9.623 \times 10^{-4} \exp (0.1443\sigma) \quad (3.11)$$

$$K(\sigma) = \text{same as in law 1-B.}$$

Plots of the predictions of laws 3-A and 3-B are not included here, because they do not differ substantially from the plots shown in Figs. (3.17) - (3.20) for laws 2-A and 2-B. A characteristic which distinguishes laws 3-A and 3-B from the others is that the duration of primary creep, as measured by $1/r(\sigma)$, remains bounded for arbitrarily small stress values. A power law representation of $r(\sigma)$ allows the duration of primary creep to become unbounded as stress values approach zero. Therefore, creep laws 3-A and 3-B predict a more rapid occurrence of primary creep strains at

very low stress levels than do the other laws. Data do not exist for sufficiently low stress levels or for sufficiently long times to permit an assessment of primary creep behavior in these regimes.

3.3.4. Summary discussions of creep laws for the ORNL preliminary heat of type 304 stainless steel at 1200°F

The overall ability of the creep laws to represent the available data can be ascertained by studying Figs. (3.13) - (3.20). It is again pointed out that the solid lines in Figs. (3.13) - (3.16) are least squares fits of the individual creep curves through the use of Eqs. (3.4) and (3.5). In general, these least squares fits to the individual curves represent the creep curves quite well. Deviations such as that observed in test P-3, 20 ksi, are attributed to the fact that a smooth correlation of the secondary creep rate dependence upon stress was used in obtaining these least squares curves. Therefore, the scatter in $K(\sigma)$ values results in these deviations. The complete creep laws result when smooth function representations are also obtained for the other coefficients. As can be seen from Figs. (3.13) - (3.20), these creep laws provide representations with significant deviations from some of the actual creep curves. Thus, the scatter of the experimental results over the range of stresses examined can be significant. Therefore, it is felt that when establishing a creep law, it may be more important to place emphasis on the careful establishment of a proper stress dependence than on a detailed refinement in the representation of the time dependence. Of course, added caution is necessary when the creep law is to be extrapolated for use with stress or time values outside the ranges for which data are available. These observations also illustrate the need for highly accurate data, not only for research programs, but also for providing data for use in inelastic design.

Of the creep laws considered, it is thought that laws 2-A and 2-B provided the best overall representation of the available data ($6 \leq \sigma \leq 25$ ksi). However, as noted earlier, the test results shown in Figs. (3.11) and (3.12) do not give a good quantitative indication of the manner in which the creep law should extrapolate to low stress values. They only indicate that the creep strains should be very small. That is to say, the

test results shown in Fig. (3.11) indicate that the creep strains shown in Fig. (3.12) may be uncharacteristically large for a stress value of 6 ksi. Therefore, it is recommended that creep laws 1-A and 1-B be used in analysis of problems, such as those associated with the beam studies discussed in Chapter 4 of this report. This recommendation is made, in part, on the basis that when extrapolated to lower stress values, lower creep strains are predicted by this set of laws than by the others.

Specifically, it is recommended that creep law 1-B, which is based on 1000 hour creep data, be used in connection with analysis of beams B3 and B4 discussed in Chapter 4 of this report and in Chapter 4 and 5 of the previous progress report.⁸ For analysis of other situations involving times greater than 1000 hours, creep law 1-A is recommended. However, for situations involving higher stress ranges (say for $\sigma > 10$ ksi), only small differences exist between the various creep laws discussed.

4. STRUCTURAL ANALYSIS

J. M. Corum

4.1. Development of Elastic-Plastic-Creep Finite Element Computer Programs

Two inelastic structural analysis computer programs, CREEP-PLAST and EPACA, are being developed by the High-Temperature Structural Design Methods Program. An important objective of these computer programs is to provide FFTF and LMFBR component vendors with special purpose, nonproprietary, inelastic analysis programs for use on their in-house computing facilities. Neither CREEP-PLAST nor EPACA is a general purpose program in the sense that the well known ANSYS and MARC programs are. Both treat restricted geometries and are, in general, restricted to elastic-plastic-creep analyses of quasi-static systems. Together, however, the two programs will handle many of the geometries for which inelastic analyses are required by FFTF/LMFBR vendors.

4.1.1. CREEP-PLAST (J. A. Clinard, J. M. Corum, J. S. Crowell)

The CREEP-PLAST computer program, which was developed by General Electric for elastic-plastic-creep analysis of two-dimensional solids, is

now being distributed to the public. The program is being released with the agreement and understanding that any changes and modifications will be coordinated with ORNL. In this way everyone will benefit from any corrections, modifications, or extensions made to the program, and a current updated version will always be available. To promote a sharing of experience, information request forms will be mailed to each program recipient on a quarterly basis, and changes made in the master program at ORNL will be briefly outlined in this series of quarterly progress reports.

A formal request form and agreement document are provided on the following two pages for the reader's convenience. If you desire a copy of the CREEP-PLAST program, remove the forms, fill them out, and send both to the ORNL Computer Librarian:

Computer Librarian
Room A228, Building 4500N
Oak Ridge National Laboratory
P. O. Box X
Oak Ridge, Tennessee 37830

The agreement document must be signed by a person in your organization with appropriate authority. Since AEC regulations require that we recover the costs of disseminating computer software, we are charging approximately \$25.00 for materials, handling, and mailing. The signed agreement document serves as our authority to bill your organization.

Upon receipt of the signed forms, we will mail the requester a copy of the program on a magnetic mini-tape, which may be kept, together with copies of the original General Electric user's manuals (Parts I and II) and of a revised input and program implementation manual prepared by ORNL for use with the version of the program that is being distributed.

The version of the program being sent out includes the constitutive equations that were recommended by ORNL for interim FFTF use.^{9,10} These equations include the shift from the initial loading bilinear stress-strain curve to the tenth-cycle bilinear curve to approximately account for cyclic hardening and/or hardening due to prior creep strains. The special auxiliary creep strain hardening rules are also included.

AGREEMENT FOR
USE OF STRUCTURAL ANALYSIS
COMPUTER PROGRAM CREEP-PLAST

WHEREAS, Union Carbide Corporation, acting under its Prime Contract W-7405-ENG-26 with the United States of America (Government) as represented by the United States Atomic Energy Commission (Commission), in operating and managing the Government-owned facility identified as the Oak Ridge National Laboratory entered into Subcontract No. 3511 with the General Electric Company for the development of a special elastic-plastic-creep structural analysis computer program identified as CREEP-PLAST; and

WHEREAS, it is one of the objectives of the Commission to provide the liquid-metal fast-breeder reactor component manufacturers and other interested Commission approved parties with a well-documented, non-proprietary structural analysis computer program to be used by said recipients on their own computers; and

WHEREAS, it is a further objective that all corrections, changes, and modifications should be incorporated in an up-to-date master computer program to be made available by the Oak Ridge National Laboratory to all interested aforementioned manufacturers and other Commission approved parties;

NOW, THEREFORE, in consideration of Union Carbide Corporation making the Computer Program CREEP-PLAST available to _____ (Recipient) on a non-exclusive, royalty-free basis, the recipient agrees to furnish the Oak Ridge National Laboratory with any changes, corrections, modifications or updating the recipient makes to the Computer Program, for incorporation in a current master computer program to be made available by the Oak Ridge National Laboratory to all Commission approved parties.

The recipient further agrees not to withhold or classify as proprietary any of said corrections, changes, modifications and alterations to the Computer Program and hereby grants to the Government an irrevocable, non-exclusive, royalty-free license to use the same for any purpose whatsoever.

It is understood that there is a handling and mailing charge of approximately Twenty Five Dollars (exact cost to be determined by Commission cost recovery guidelines applicable at time of request) due Union Carbide Corporation for the account of the Government upon receipt of the Computer Program.

Date _____ Name _____
Title _____
Representing _____
Address _____

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REQUEST FORM FOR CREEP-PLAST COMPUTER PROGRAM

Return form to: Computer Librarian
 Room A228, Building 4500N
 Oak Ridge National Laboratory
 P. O. Box X
 Oak Ridge, Tennessee 37830

The source program, in card image form, will be sent to you on a magnetic tape. The tape will be a 600 ft. mini-tape which you may keep.

Please check the option below which comes closest to meeting your system's requirements as to tape format. Tapes will be prepared in advance in these two standard formats. Your request for the program can be met more quickly if you can accept one of these standard formats.

_____ Option 1: 7-track tape
 BCD card code
 556 bpi density
 Unblocked records (84 characters per
 physical record, with last four characters
 blank)

_____ Option 2: 9-track tape
 EBCDIC card code
 800 bpi density
 Blocked records (800 characters, or ten
 cards per physical record)

If you require modifications to the option checked above, please indicate them below.

character code _____ BCD _____ EBCDIC
 density _____ 200 _____ 556 _____ 800
 physical record size _____

Note: Our 9-track tape drives are all 800 bpi density.

What type of computer will be used to read the tape? _____

On what type of computer will the program be executed? _____

Name and address of requester: _____

Phone number of requester: _____

Signed: _____ Date: _____

cut out here

The "I/O bound" situation that was described in the previous progress report has been improved. This was done, without appreciably increasing the fast memory region, by eliminating unnecessary I/O. Two I/O units have been completely eliminated.

4.1.2. EPACA (Z. Zudans, The Franklin Institute Research Laboratories; J. M. Corum, ORNL)

EPACA treats arbitrary thick, or thin, shell structures such as piping elbows and tees, nozzle attachments, etc. In addition to doing elastic-plastic-creep analyses for quasi-static conditions, EPACA has the capability to compute natural frequencies and mode shapes, with and without prestress, to compute dynamic response time histories by direct integration, and to compute buckling loads for linear elastic structures.

The completed EPACA program has been transferred from the Franklin Institute to ORNL. It should be emphasized, however, that EPACA is a large and complex program, and many of the features have not been adequately checked out. Thus the program at present is a "raw program," and we anticipate that a very significant ORNL effort will be required to evaluate, debug, and verify the program to produce a reliable tool for design analysis use. We plan to make EPACA available to the public late this calendar year. Distribution will be on the same basis as CREEP-PLAST.

4.2. Inelastic Ratchetting Analyses

W. K. Sartory

4.2.1. Analysis of LMEC ratchetting tests

Several of the ratchetting tests conducted by W. F. Anderson at LMEC¹¹⁻¹³ have been analyzed with the finite element program PLACRE¹⁴ using the cyclic creep and plasticity laws proposed in ORNL-TM-3602.⁹ The LMEC tests were conducted by applying a longitudinal load to a metal strip heated to 1200°F or 1100°F, and bending the strip back and forth repeatedly around cylinders of 80-in., 40-in., or 20-in.-diam. The tests were approximated in the calculations by using an axisymmetric cylinder with a wall thickness equal to the strip thickness, an axial length equal to the strip width, and a very large (1800 in.) fixed diameter. The mechanical bending strain of the tests was approximated by a thermal strain of

the same magnitude by applying a linear temperature gradient across the wall of the cylinder. A special anisotropic thermal expansion law giving expansion only in the circumferential direction was used for this purpose. The longitudinal load in the tests was obtained by applying an internal pressure to the cylinder. (Only a few psi are required.) No axial load was applied to the cylinder. A single row of 22 rectangular elements through the thickness of the cylinder was used. The stress and strain were therefore taken to be uniform along the length and width of the strip and were permitted to vary only through the thickness.

The detailed time history of an individual bending application as it occurred in the tests could be rather complicated and difficult to determine, and might have an important effect on the results of an analysis. The time required to bend a specimen is approximately 2 1/2 minutes, and at high stress levels, a significant amount of creep can occur in that time. In most of the present analyses, the bending history has been approximated by a ramp loading of 2 1/2 minute duration. If a strip were very flexible, however, we would expect very rapid bending to occur at a given longitudinal location on the strip, just as that point makes or breaks contact with a cylinder. Some additional calculations have therefore been made by assuming an instantaneous bending followed by a 2 1/2 minute hold time before any further load changes are made. Note that the cases considered for analysis represent opposite extremes of perfect distribution or perfect concentration of the bending in the allotted time.

Creep data were available for the LMEC 304 stainless steel at 1200°F at five stress levels from 8600 psi to 20,000 psi. The data were fit by the equation

$$\epsilon^c = \epsilon_x (1 - e^{-st}) + \epsilon_t (1 - e^{-rt}) + \dot{\epsilon}_m t ,$$

where

$$\epsilon_x = C_1 + C_2 \sigma$$

$$s = C_3 \left[\sinh \left(\frac{C_4 \sigma}{3.5} \right) \right]^{3.5} \exp \left(\frac{467,000}{1.987 T} \right)$$

$$r = 0.1 s$$

$$\dot{\epsilon}_m = C_6 \left[\sinh \left(\frac{C_7 \sigma}{6.0} \right) \right]^{6.0} \exp \left(\frac{467,000}{1.987 T} \right)$$

$$\epsilon_t = C_5 \frac{\dot{\epsilon}_m}{r}$$

σ is the stress in psi

ϵ^c is the creep strain in percent

$T = 923^\circ K$

$C_1, \dots, C_7$ are constants.

The secondary creep was separated out graphically and used to obtain the constants C_3, C_7

$$C_6 = 5.5515 \times 10^6$$

$$C_7 = 0.0007982$$

The remaining constants were then fit by the method of least squares to the remaining primary creep giving

$$C_1 = 0.0004685$$

$$C_2 = 0.10877 \times 10^7$$

$$C_3 = 0.3424 \times 10^{12}$$

$$C_4 = 0.0006255$$

$$C_5 = 9.6278$$

The overall fit to the data is not very good. At 20,000 psi the equation overestimates the creep by predicting 2.5% strain at 160 hours where the data indicate 2.0%. At 17,000 psi the equation underestimates the creep by predicting 0.82% strain at 330 hours when the data indicate 1.01%. Inspection of the data suggests that the measured primary creep strain at 20,000 psi is rather out-of-line with the measurement at lower stresses.

A few calculations were also made at 1100°F. No creep data were available, so the creep rate was approximated very crudely by substituting $T = 867^\circ K$ into the exponential terms of the creep equation, with no other changes.

From the average of three tensile tests, an elastic modulus of 21.5×10^6 psi, a plastic tangent modulus of 0.447×10^6 psi, and a virgin yield stress of 23,300 psi were obtained at 1200°F. No cyclic plasticity data were available. On the basis of results from other tests on 304 stainless steel, the 10-cycle yield stress was taken to be twice the virgin yield stress or 46,600 psi. The plastic and elastic properties were not changed for calculations at 1100°F.

Table 4.1 shows a comparison, for some of the LMEC creep ratchetting tests, of the experimental longitudinal percent ratchetting strain at the end of the test with results of the present analyses for both ramp bending and instantaneous bending. Also shown are the corresponding results of a Bree analysis. (The Bree values given are the per-cycle values reported by Anderson¹¹ multiplied by the total number of cycles.) Results obtained by the present analysis are consistently higher than the experimental results, but lower than the Bree results. The effect of using an instantaneous bending instead of a ramp bending is to raise the predicted values and to give poorer agreement with the experiments.

Table 4.1. Percent creep ratchetting strains

LMEC case	Experimental result	Analysis (ramp)	Analysis (instantaneous)	Analysis (Bree)
1	0.74	0.846	—	1.12
2	0.49	0.986	—	1.56
3	0.38	0.713	—	0.696
4	0.29	0.431	—	0.54
5	0.63	0.930	—	1.23
6	0.50	0.940	—	1.34
8	0.42	0.539	0.612	0.728
20	0.556	0.850	1.003	—
29	0.883	0.952	1.163	—
31	0.65	0.911	1.172	—

Table 4.2 shows the same comparison for some of the LMEC plastic ratchetting tests. (The plastic ratchetting tests were made with a minimum hold time between bending applications. In the present analysis a 2 1/2 minute minimum bending time has been used as described earlier, so

creep can occur. The Bree results in the table are those reported by Anderson and do not include creep.) Tests 11 and 19 are borderline cases according to the Bree plastic ratchetting analysis so that no strain is predicted. The present analysis using ramp bending gives a predicted ratchetting strain which is lower than the experimental results by almost a factor of 1/3 for Cases 11 and 19. The present analysis using an instantaneous bending application gives much closer agreement with experiment for these two cases. Case 9, which involves loading well into the plastic ratchetting regime, is more like the earlier creep ratchetting cases. The present analysis predicts too high a strain even with ramp bending.

Table 4.2. Percent plastic ratchetting strains

LMEC case	Experimental result	Analysis (ramp)	Analysis (instantaneous)	Analysis (Bree)
9	0.98	1.181	1.483	1.36
11	0.346	0.110	0.233	0.00
19	0.175	0.056	0.183	0.00

Figures 4.1 and 4.2 present comparisons of the cycle-by-cycle longitudinal ratchetting strain. The experimental strains were obtained by correcting the measured strain at the end of the tests using dial gage readings giving the overall elongation of the strip on a cycle-by-cycle basis. The analytical strains reported were obtained by multiplying the strain with the specimen unloaded at the end of the calculation by the ratio of the strain (under load) at the end of the nth cycle to the strain (under load) at the end of the calculation.

Table 4.3 shows a few comparisons of analyses made using the cyclic plasticity and creep hardening rules of ORNL-3602,⁹ with results obtained with ordinary kinematic hardening plasticity and ordinary strain hardening creep. Including the cyclic hardening rules appears to increase the calculated strain. The effect of the cyclic rules in these cases is rather small, however, compared to the deviation between the tests and calculations, and is also smaller than the effect of changing from ramp to instantaneous bending in the calculations.

- - experimental
- ▲ - calculated for 2 1/2 minute ramp bending application
- + - calculated for instantaneous bending + 2 1/2 minute hold

Fig. 4.1. Ratchetting strain against cycle number for LMEC case 9.

- - experimental
- ▲ - calculated for 2 1/2 minute ramp bending application
- + - calculated for instantaneous bending + 2 1/2 minute hold

Fig. 4.2. Ratchetting strain against cycle number for LMEC case 11.

Table 4.3. Comparison of hardening rules

LMEC case	Experimental result	ORNL-TM-3602 plastic & creep	Non-cyclic plasticity	Non-cyclic plastic & creep
8	0.42	0.539	0.516	—
8*	0.42	0.612	—	0.580
11	0.346	0.110	0.109	—
11*	0.346	0.268	—	0.233
31*	0.65	1.172	—	1.164
1**	0.49	0.542	0.542	0.537

* Instantaneous bending. All others are ramp bending.

** Ramp bending; results after five cycles only.

In spite of the small effect of the cyclic rules on the overall ratchetting strain, the effect on the creep strain, plastic strain, and stress in an individual element can be very pronounced. In the case of a ramp bending application the cyclic plastic hardening rules raise the yield stress of a hardened element to a value so high that the element never yields again. This occurs because the creep rate at 1200°F and an effective stress on the order of 27,000 psi equals the rate of applied mechanical strain during bending. In such a case, the incremental plastic strain is replaced by creep strain after an element undergoes cyclic plastic hardening.

In the case of an instantaneous bending application, no creep occurs during bending, and elements which have undergone cyclic plastic hardening reach stress levels nearly twice as high as unhardened elements.

Although the cyclic hardening rules have a small effect on the total ratchetting strain, it is possible that they would have a substantial effect on the life of a specimen based on calculated damage. No damage calculations have been performed, however.

4.2.2. Pipe ratchetting analysis

Since the cyclic plasticity and creep rules were proposed in ORNL-TM-3602,⁹ there has been some interest in the possible effect of those rules

on pipe ratchetting analyses. To aid in determining the effect, subroutines to carry out pipe wall thermal transient calculations using finite difference methods have been incorporated into the inelastic finite element program PLACRE.¹⁴ This program has been tested on the pipe ratchetting problem of Minami and Roberts,¹⁵ for which it gives substantially the same thermal and mechanical results as were obtained by Minami and Roberts and by Teledyne.¹⁶ The program has also been tried on the Teledyne ratchetting case TMR-20¹⁶ using the inelastic rules of ORNL-TM-3602, for which it predicts substantially more ratchetting strain than obtained by Teledyne. The results will not be presented here because the program is still in the testing stage.

Using 80 time increments for the thermal transient, the program requires about 1/2 minute of IBM/360-91 CPU time per ratchetting cycle.

This program will be subjected to further testing and modification and, if satisfactory, will be used for extensive pipe ratchetting studies aimed at extending the parametric studies of Teledyne.¹⁷

4.3. Elastic-Plastic-Creep Analyses of Beams B3 and B4

J. A. Clinard
J. M. Corum

Preliminary analyses of the initial elastic-plastic loading of beam B3 (Ref. 18) to 1900 lb and the 312-hr creep period at that load level were reported in the previous progress report.¹⁹ Both the CREEP-PLAST computer program and the ORNL in-house program PLACRE were used, and the agreement between the two analyses was very close. However, the agreement between predictions and test results for the creep portion of the test were poor because the creep equation used did not adequately predict the creep at the low stress levels and relatively short times existing in the test. A revised creep law has been developed, as described in Section 3.3 of this report, and the analyses have been redone using the CREEP-PLAST program. Comparisons of these latest predictions with experimental results are briefly presented in this section. Also, cyclic load analyses have been performed for the initial short-time cyclic loading of beam B4, and comparisons of these predictions with test results are given. Calculations

based on both bilinear and nonlinear stress-strain representations have been made, and the interim constitutive equations recommended by ORNL for FFTF use^{9,10} have been used.

The ORNL constitutive equation recommendations require the use of a bilinear representation of the initial loading curve and of the tenth-cycle cyclic stress-strain curve. After the initial plastic loading or after 0.2% creep strain has been accumulated, a switch is made from the initial curve to the tenth-cycle curve to approximately account for hardening. The recommendations also include auxiliary creep strain-hardening rules to be used in cases involving load reversals.

The bilinear stress-strain curve is required because it results in a theoretically consistent kinematic hardening plasticity formulation for multiaxial, nonproportionate loading situations whereas the nonlinear stress-strain curve currently does not. In the case of simple beams, the stress state is largely uniaxial and we would expect the nonlinear formulation to be adequate. This is the basis for performing both bilinear and nonlinear analyses.

4.3.1. Property data for preliminary heat of 304 stainless steel

The creep law recommended in Section 3.3 for beam analyses is rewritten here for completeness. This equation results in theoretical predictions which are in reasonable agreement with the test results from beams B3 and B4, and it should be used in all analyses of these two beams. The equation is:

$$\epsilon^c = A(\sigma) [1 - e^{-r(\sigma)t}] + K(\sigma)t ,$$

where

$$A(\sigma) = 1.39 \times 10^{-6} \sigma^{3.28}$$

$$r(\sigma) = 3.41 \times 10^{-5} \sigma^{2.09}$$

$$K(\sigma) = 7.91 \times 10^{-11} [\sinh(0.1932\sigma)]^{4.0} .$$

Here, ϵ^c is creep strain measured in in./in., σ is stress measured in ksi, and t is time measured in hours. This equation is for the preliminary heat of type 304 stainless steel at a temperature of 1200°F.

Cyclic stress-strain curves for the preliminary heat, obtained at a temperature of 1200°F, and the equivalent bilinear representations, were

given in the previous progress report.²⁰ For the bilinear equivalent of the initial loading curve, the elastic modulus is $E = 18.93 \times 10^6$ psi, the plastic modulus is $E_p = 0.417 \times 10^6$ psi, and the yield point is 13.8 ksi. The yield point for the tenth-cycle curve is 33.5 ksi, so that $\kappa_1 = 5.89 \kappa_0$. The modulus values remain unchanged for the tenth-cycle curve.

For analyses based on the nonlinear stress-strain curve, the following set of equations was used to represent the initial loading curve:

$$\epsilon(\%) = \begin{cases} \sigma/189.3 & 0 \leq \sigma \leq 8.0 \\ 0.005492\sigma^2 - 0.08258\sigma + 0.3515 & 8.0 \leq \sigma \leq 12.9 \\ 0.0172\sigma^2 - 0.3847\sigma + 2.3, & 12.9 \leq \sigma \leq 18.154 \\ 0.985 + (\sigma - 18.154)/4.17 & 18.154 \leq \sigma \approx 20.0 \end{cases}$$

The strain, ϵ , in these equations is in % and the stress, σ , is in ksi. The stress-strain curve given by these relations, as well as the slope of the curve, is continuous, and the predicted curve matches the actual curve closely. For cyclic nonlinear analyses, the tenth-cycle curve was assumed to be adequately represented by the initial loading curve with the proportional limit appropriately increased. The increase in proportional limit was chosen to give the best fit of the initial curve to the tenth-cycle curve. On this basis the proportional limit for the tenth-cycle curve was taken to be 30.4 ksi, compared to 8.0 ksi for the initial curve. Consequently, for the nonlinear analyses that were performed, $\kappa_1 = 14.44 \kappa_0$.

4.3.2. Elastic-plastic-creep analysis of beam B3

Both the bilinear analysis and the nonlinear analysis have been carried through the entire 934-hr creep test period for beam B3. The results of these analyses are typified in Figs. 4.3 and 4.4 where the predicted center-deflection behavior is compared to the actual measured behavior. These predictions were obtained with the finite element model described previously.¹⁹

The measured center deflection versus load for the initial loading of beam B3 to 1900 lb, as well as for the other "instantaneous" load changes

Fig. 4.3. Predicted and measured load versus center deflection for short-time load changes associated with creep portions of beam B3 test.

Fig. 4.4. Predicted and measured creep deflection versus time for center of beam B3.

associated with the creep portion of the test, is shown in Fig. 4.3. The creep deflections that occurred at 1900, 2250, and -1900 lb have been subtracted, so that the deflection shown is essentially elastic-plastic only. Specifically, the four curve segments in Fig. 4.3 represent the load-deflection response during the loading from 0 to 1900 lb, from 1900 to 2250 lb, from 2250 lb to -1900 lb, and from -1900 lb to 0. Note that due to strain hardening that occurred during prior creep, the measured behavior during the load change from 1900 to 2250 lb is elastic only. Also shown in Fig. 4.3 is the measured response of the virgin beam B4 during the initial loading to 1900 lb. Note that the initial elastic response of beam B3 is peculiar and does not agree with the subsequent elastic response of beam B3 nor with the elastic response of beam B4. The peculiar behavior is believed to be associated with dimensional imperfections of the beam at the loading points. A lack of parallel surfaces resulted in some initial torsion of the beam in addition to the predominant bending.

The nonlinear analysis predictions agree reasonably well with the measured initial loading response to 1900 lb, particularly for beam B4. The bilinear analysis does not do nearly as well. This is mainly because the bilinear representation considerably overapproximates the actual curve for the relatively small strains involved. For example, the maximum total strain at 1900 lb predicted by the nonlinear analysis was only 0.2%, while the bilinear representation was based on a maximum strain of 1%. A bilinear representation based on 0.2% would give much improved predictions. Note that due to the assumed hardening, the "instantaneous" responses predicted by both analyses are elastic following the initial loading to 1900 lb.

The measured and predicted creep deflections that occurred as a function of time are shown in Fig. 4.4. Here, the predictions based on the nonlinear stress-strain representation are only slightly different from those based on the bilinear representation. Overall, the agreement between the measured and predicted creep deflections is reasonable.*

*The disagreement is little more than that generally observed between the results of two uniaxial creep tests at the same condition or between the results of some of the individual creep tests and the predictions of the creep law.

4.3.3. Elastic-plastic cyclic analysis of beam B4

The initial precreep short-time cyclic loading of beam B4 was analyzed using both nonlinear and bilinear stress-strain representations. In both cases the switch to the tenth-cycle curve was made after the initial loading. The results are typified in Figs. 4.5 and 4.6 for the nonlinear and bilinear cases, respectively. In these plots, the predicted load versus center-deflection behavior is compared with the measured behavior. Only the first cycle is shown since both analyses predicted repeated elastic behavior on subsequent cycling. The measured response to subsequent cycling was presented in the previous progress report.²⁰

The nonlinear analysis prediction shown in Fig. 4.5 agrees reasonably well with the measured behavior for the initial loading. The agreement upon reversed loading is not too good since the analysis predicts elastic behavior whereas the actual behavior was elastic-plastic. However, by the tenth cycle of loading, the measured loop (see Ref. 20) is only about 0.015 in. wide so that the stable cyclic behavior is reasonably well predicted; this was the basic objective of using the initial loading curve and the tenth-cycle curve — to predict the initial response and the stable cyclic behavior.

The bilinear predictions, shown in Fig. 4.6, again do not agree well with the measured behavior. As explained in the previous subsection, this is because the bilinear representation considerably overapproximates the actual curve for the relatively small strains involved. A bilinear representation based on 0.2% maximum strain would be expected to give much better results.

In future analyses, additional bilinear approximations will be examined. Also, the effects of including, or not including, the specific features of the ORNL recommended constitutive equations will be evaluated, and possible alterations to the recommendations will be examined.

Fig. 4.5. Comparison of measured center deflection of beam B₄ for first cycle of reversed loading with predictions based on nonlinear stress-strain representation.

Fig. 4.6. Comparison of measured center deflection of beam B4 for first cycle of reversed loading with predictions based on bilinear stress-strain representation.

5. STRUCTURAL TESTING

J. M. Corum

5.1. Elastic-Plastic-Creep Beam Tests

J. M. Corum
M. Richardson

Testing of beam B5, which is the first beam from the reference heat of type 304 stainless steel, is currently in progress. Previous beams (B1 through B4) were from the preliminary heat of 304. Beam test B5 and the following beam test, B6, are companion tests and are very similar to B3 and B4. Whereas beams B3 and B4 were tested at 1200°F, beams B5 and B6 are being tested at 1100°F.

The loading history for beam B5 consists of a constant load of 2000 lb for 312 hr, a step increase to 2250 lb for a second creep period of 312 hr, and a step decrease to -2000 lb for a final 312 hr creep period. Following the creep test period, beam B5 will be subjected to ten short-time load cycles between ± 2000 lb. The history for beam B6 differs from that of B5 only in that the creep period will be preceded by ten short-time load cycles between ± 2000 lb.

The test currently underway is designated B5B. A previous test, B5A, was terminated after approximately 475 hr because of what was apparently a structural support problem and also because of a temperature controller failure which resulted in a loss of temperature. Several things, which have now been corrected, contributed to the support problem, but the net result was a couple of instantaneous jumps in the measured creep deflections during the first 312 hr of testing. Since these jumps could not be traced to a single specific cause, they cast an element of uncertainty on the remaining data. It turns out, however, that if the instantaneous increases are subtracted out, the measured behavior of beam B5A is practically identical to that of B5B, thus the remaining data were probably valid.

The general behavior of these reference heat beams is similar to that of the preliminary heat beams except for one significant feature. Recall that in the case of beam B3, the load change from 1900 to 2250 lb after the initial creep period resulted in elastic behavior (see Fig. 4.3). This

was due to strain hardening that occurred during prior creep. In the case of beam B5, the increase from 2000 to 2250 lb after the initial creep period resulted in elastic-plastic behavior that indicated that very little hardening had taken place. This behavior is apparently dependent on the test temperature. The same sort of behavior difference has been observed in step-load uniaxial creep tests of the two materials. That is, significant hardening due to prior creep occurs in both the preliminary heat and the reference heat at 1200°F but not in the reference heat at 1100°F.*

The test results from beams B5A and B5B will be presented in the next progress report. Creep data for the reference heat at 1100°F are available, and cyclic stress-strain tests at 1100°F have been performed for us by Southern Research Institute. These latter curves will be presented in the next report along with the beam test results.

5.2. Elastic-Plastic-Creep Circular Plate Tests

J. M. Corum
M. Richardson

Construction of our elevated temperature circular plate test facility²¹ is nearing completion. This facility, which will be a companion to the beam test facility, is to be used for elastic-plastic-creep tests of simply-supported circular plates laterally loaded at the center. The basic specimens, which are being machined from the reference heat of type 304 stainless steel, are 1/2 in. thick and 20 3/4 in. in outside diameter. The support circle is 20 in. in diameter.

The partially assembled facility is pictured in Fig. 5.1, and a close-up view of the plate support arrangement is shown in Fig. 5.2. Simple-support conditions are achieved by using 1/2-in.-diam ball bearings around the periphery of the specimen. The bearings are guided by races which are machine fitted to heavy upper and lower flanges. The lower flange is supported from the loading frame through a slotted

*It should be pointed out that in the case of the beam tests the amount of creep in the reference material beam at 1100°F is considerably less than that in the preliminary heat beam at 1200°F. This perhaps explains at least part of the observed difference in hardening due to prior creep.

PHOTO 79853

Fig. 5.1. Partially assembled elevated temperature circular plate test facility.

Fig. 5.2. Close-up view of simple support arrangement for circular plate specimen.

stainless steel skirt, which can be partially seen in Fig. 5.2. Spacers are used between the upper and lower flanges to maintain the slight gap necessary to insure that the specimen does not bind between the upper and lower supports as it undergoes cyclic deformation.

The center load is applied through a loading arm and dual bridge load cell. The loading arm attaches rigidly to the specimen through a small center boss which is an integral part of the specimen. A guide rod that is an integral part of the loading arm extends below the specimen and through a guide bushing in the lower support flange assembly. This arrangement assures proper alignment.

The actual loads are applied in the same manner as in the beam test facility. Short-time loadings are applied through a servo-controlled hydraulic system, and dead weights are used for the long-term creep loadings.

The furnace consists of sectioned, 5-in.-thick, canned insulation. The primary heaters are calrods that are serpentine on the inner walls of the furnace around the periphery of the specimen. These primary heaters are automatically controlled. Several smaller calrod heaters are located in hot plate fashion above and below the specimen. These smaller heaters are zone controlled individually and manually to maintain the required $\pm 5^{\circ}\text{F}$ temperature tolerance. Suitable high-low temperature alarms are incorporated into the heater control circuit.

Instrumentation for the plates will be basically the same as for the beams, and will consist of quartz rods located along one diameter and connected to dial gages and DCDT's. A support frame, not shown in Fig. 5.1, is located above the furnace to hold the dial gages and DCDT's.

5.3. Pipe Thermal Ratchetting Tests

J. M. Corum
H. C. Young
A. G. Grindell

The design work for the piping and sodium components in the Thermal Transient Tests Facility²² (TTT) was completed, and the structural design is nearly complete. The flow diagram for instruments and controls and the layout of the gas system were completed, and some of the gas flow control valves were obtained. The electrical design of the heating

system was initiated. Orders were placed for all the stainless steel requirements for the facility and some material was received.

The arrangement of the principal facility components is shown in Fig. 5.3. The assembly of the test piece²³ is shown in Fig. 5.4. The test piece is a 30-in.-long section of 8-in. pipe from the ORNL reference heat of stainless steel and has a wall thickness of 0.375 in. (the specimen is machined from sch. 80 pipe). It is welded to adjacent sections of 8-in. stainless steel pipe which are welded to concentric pipe reducers. The test piece will be provided with 5 thermocouples²⁴ in the sodium annulus, 5 thermocouples embedded in the specimen wall, and 13 thermocouples and several strain measurement devices on the outside surface. There is a relatively thin walled bladder mounted inside the test piece to reduce the amount of sodium required for the thermal transient. The internal gas volume of the bladder is connected to the gas volume in the sodium source tank with a gas pressure equalizing line. Equalization of these gas pressures will tend to reduce the differential pressure across the bladder wall to near zero during the 500 hr creep testing and to minimize it during the thermal transient testing when sodium is flowing.

The design of the controls for the high pressure argon system is progressing. Our goal is to control the gas pressure in the sodium source tank during static testing to within $\pm 1\%$, that is ± 7 psi, and during thermal transient testing to within ± 25 psi of the 700 psig normal operating pressure. This close control requires two different sizes of valves for both the gas supply and vent for the source tank. When delivery time for gas valves of this type was quoted by manufacturers to be as long as 9 months, valves were found in-house that will be suitable after the valve seat and plug trim are replaced. These valves are currently being reworked and bench tested. We plan to equip these valves with air-actuated operators and also to trace-heat them to prevent the formation of sodium plugs which could interfere with proper functioning. The pressure relief for the facility called for by the ASME Boiler and Pressure Vessel Code will be provided by connecting in series a rupture disk and a relief valve. This arrangement reduces the requirement for the extremely low leak rate across the valve seat that arises from depending on the relief valve alone and also permits us to use an available relief valve.

Fig. 5.3. Schematic of the thermal transient test facility. Shows arrangement of principal facility components and connecting piping. Note high-pressure gas system, sodium source tank, pipe nest, test piece, orifice pipe run, thermal capacitance tank, sodium shutoff valve, and low pressure drain tank. (From ORNL Dwg. E10596-RE-504E.)

Fig. 5.4. Cross section of the test piece for the thermal transient test facility. Note thermocouples in sodium annulus, bladder, and other details of construction. (From ORNL Dwg. M10596-RM-004E.)

A heating test was made with a 2 1/2-in. drain line to determine heater length, control, and thermocouple requirements²⁵ for the pipe nest in which sodium will be temperature graded for use during the thermal transient operation. The gradients that were established during the tests indicated the 2 to 7°F per ft gradients that were calculated for thermal transient operation could be achieved by providing the pipe nest with about 40 separately controlled heater zones. The pipe nest heaters are being designed on this basis.

All of the stainless steel pipe, tubing, fittings, bar, and plate requirements²⁵ for the facility were determined and the items placed on purchase order. Attempts made to procure some sizes of pipe and plate from RDT materials in LMFBR stockpiles were fruitless. The possibility of procuring welding fittings to RDT standards, based on the groundwork already laid by LMEC and HEDL, was investigated to no avail. In an attempt to maintain fabrication and construction schedules for the facility most of these materials were ordered to ASME SA standards, and inspections to upgrade the materials to the more significant quality requirements of the RDT standards will be performed and documented by the ORNL Inspection Engineering Department. Approximately 40% (by costs) of the material was delivered before the end of June. One piece of 12-in. stainless steel pipe for the thermal capacitance tank is being purchased to meet the requirements of the appropriate RDT standard and is scheduled for delivery in September. Most of the component fabrication and facility construction activities can be performed prior to the receipt of this long delivery item.

The flanges that retain the bonnet to the body of the 2 1/2-in. sodium shutoff valve were redesigned for 800 psig operation. Seat leakage tests are being conducted with 800°F argon. If the valve seat leakage during the 500 hr creep portion of the test cycle is larger than can be accommodated without resorting to refill, then a sodium freeze valve will be employed. Provisions have been made to freeze the sodium at the lower orifice plate, the one nearest to the inlet of the thermal capacitance tank.

As previously mentioned,²⁶ we have given particular attention to the absolute necessity of obtaining accurate and reliable strain and dimensional

change data, and we are now planning to use six independent strain measurement systems:

1. Boeing high-temperature capacitive strain-gage system (purchased from the Boeing Co. - Aerospace Group);
2. Microdot, electrical resistance, weldable strain gages (commercially available);
3. mechanical extensometer with remote monitoring (being designed and built by ORNL);
4. strain-gage-based cooled extensometer (to be designed and built by ORNL on basis of design used successfully by Boeing at temperatures to 1500°F);
5. special scratch gages with remote indexing (to be purchased from Prewitt Associates);
6. Demec mechanical gages (available at ORNL).

Each of these strain-measuring devices, except for the strain-gage-based extensometer, was described in more detail in Ref. 26.

The six independent systems offer not only a cross-check through redundancy, but they also augment one another by being particularly applicable and reliable in certain regions of the test sequence. As a minimum we must accurately measure the incremental strains accumulated during each ratchetting cycle. An accurate determination of the strain-time history during the thermal shock and during the 500 hr hold periods is almost equally important. The expected regions of applicability for the six devices are briefly indicated in Table 5.1.

The specific characteristics of each device, in so far as they are currently known, are listed in Table 5.2. Development work is continuing on some of the devices, so that certain characteristics will not be accurately known until tests are carried out on the final gages.

Table 5.1. Strain-measuring devices for pipe thermal ratchetting test and regions of applicability and reliability

Device	Incremental strain per cycle	Strain-time history		Expected to be reliable through- out test
		Transient	Long-term	
1. Boeing capacitive strain gage	X	X	X	X
2. Microdot strain gage	(possibly for first cycle)	X	(possibly for first cycle)	
3. Mechanical extensometer with remote monitoring	X		X	X
4. Strain-gage-based cooled extensometer	X	X	X	X
5. Scratch gages with remote indexing	X		X	X
6. Demec mechanical gages (before and after measurements only) at room temperature				X

Table 5.2. Summary of characteristics of strain-measuring devices for pipe thermal ratchetting tests

Device ^a	Strain sensitivity	Strain rate capability	Resolution	Strain range	Drift	Creep	Comments
1. Boeing high-temperature capacitive strain gage. Used with Boeing-designed signal conditioning electronics. Two gages, one axial and one circumferential, will be used on each ratchetting specimen.	0.5 mv/ $\mu\epsilon$ with less than 4% change from room temperature to 1500°F.	31,180 $\mu\epsilon$ /sec to 1500 $\mu\epsilon$. This is the highest level checked, not the limiting value.	5 $\mu\epsilon$ at 500 $\mu\epsilon$	$\pm 20,000 \mu\epsilon$	$\pm 55 \mu\epsilon$ at 1500°F in 1 hr (this probably was due to reference used; gage should not drift after drying; more study is required).	Less than 3% at temperatures to 1500°F	We feel that the Boeing gage has the best potential for measuring strain vs time histories throughout the test of any current state-of-the-art device. The one unknown is its long-term durability (not stability). No problems are foreseen, however.
2. Microdot electrical resistance weldable strain gage. Six of these commercial gages will be used on each specimen.	Gage factor ≈ 4.0 at 1100°F.	Not known but sufficient for application.	$\pm 5 \mu\epsilon$ (depends on readout equipment).	$\pm 5000 \mu\epsilon$ at 1100°F (based on ORNL experience).	Less than 20 $\mu\epsilon$ /hr at 950°F. Stabilizes at 1100°F with precycling.	Not known	Requires considerable effort prior to use (precycling to temperature and calibration at temperature). Life expectancy uncertain although ORNL beam test experience indicates some gages will last through at least one cycle.
3. Mechanical extensometer with remote monitoring. These are being designed and developed by ORNL, and they use a mechanical multiplication concept. Reading of a needle position on a scale, which will be outside the test piece furnace, will be by a remotely operated camera. An axial gage, with 20-in. gage length, and a diametrical gage will be used.	~ 1000 to 1 for axial gage and slightly more for diametrical gage.	Not applicable. Gage will follow test piece but output must be read remotely on a periodic basis.	$\sim 100 \mu\epsilon$ (to be determined).	To be determined (based partially on requirements).	None	None	To be used mainly as a redundant backup for checking other devices during hold periods and at stable points in thermal cycle.
4. Strain-gage-based cooled extensometer. These devices will be patterned after very successful water-cooled devices used by Boeing. We will probably use air cooling. An axial and one circumferential gage will be used.	4760 $\mu\epsilon$ /ohm	Maximum not known, but sufficient for application.	20 $\mu\epsilon$	$\pm 50,000 \mu\epsilon$	Variable, depending on installation. Expected to be comparable to good room temperature gages.	Should stabilize	Should give good data, particularly during transients.
5. Special high-temperature scratch gage with remote indexing. One 20-in. axial gage will be used and one 6-in. circumferential gage (with special standoffs).	20 to 1 direct measurement for 20-in. gage	Not applicable. Measurements are peak to peak (time-dependent) for each index position.	10 $\mu\epsilon$ (20-in. gage) 35 $\mu\epsilon$ (6-in. gage)	$\pm 50,000 \mu\epsilon$ (20 in.) $\pm 150,000 \mu\epsilon$ (6 in.)	None	None	These gages are very good backup to electronic gages during hold periods.
6. Demec mechanical gages. These precision gages will be used to measure permanent strains between preattached gage contact points at the end of each test. The same system will be used to monitor strains in critical areas of the test facility.	~ 5000 to 1 direct measurement (8-in. gage).	Not applicable.	10 $\mu\epsilon$ (8-in. gage)	$\pm 12,000 \mu\epsilon$ (8-in. gage)	None	None	Good cross-check of other gages.

^aAll electronic devices will be calibrated in place at room temperature with room temperature foil strain gages.

5.4. Weldment Creep-Rupture Studies

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5.4.1. Test program

In order to make maximum use of weldment creep-rupture (WCR) specimens, specimen no. 2A, which had failed by a flat head to cylinder separation (see Ref. 27) was rebuilt and continued in test. After preparing the open end of the specimen, a flat circular plate was welded to the specimen. Since the machining and subsequent welding of the part would alter the material properties only in a narrow circumferential region around the end of the specimen, the assumption was made that the remaining portion of the specimen would maintain the material history that existed at the time of rupture (neglecting recovery) and the test could be continued as if it had not been interrupted. With precautions on specimen handling, there is evidence to support this assumption.²⁸

This rebuilt specimen, designated specimen no. 2B, was continued in test until failure occurred. The resulting rupture was a longitudinal split in the lower half of the cylindrical portion of the specimen, that is, in the section between the flat head and the circumferential weld at the specimen center. Failure was in the base metal, and the total time to rupture was 269 hours. The corresponding data point is shown in Fig. 5.5 in comparison to data from other WCR specimens and uniaxial creep-rupture data for bar and plate material from the same heat. Fig. 5.5 is an updated version of Fig. 5.12 of Ref. 27. The uniaxial creep-rupture data were taken from Ref. 29.

In addition, preparation of the second generation of specimens is complete, and the specimens are being prepared for testing.

5.4.2. Test data reduction

The deformation data obtained from the WCR specimens consist of length and radius measurements at 60° increments around circumferential scribe lines on the cylinder and head sections of the specimen. These 60° increments, though not scribed on the test section of the specimen, are marked and referenced on the specimen closure cap. Thus, the necessary information for reproduction of the grid is a permanent part of the specimen.

ORNL DWG 72-10069

Fig. 5.5. Comparison of creep rupture data from uniaxial tests and WCR tests.

Using this scheme it is tacitly assumed that there will be no circumferential components of displacement. This is a reasonable assumption except in a rupture region where there will be gross distortion of the specimen surface.

To determine total strains from the original measurement data requires first of all a conversion of these data to a displacement field. When this has been done, strains can be calculated using the appropriate strain-displacement relations. Since in most cases the specimen deformation is finite, large deformation theory is required. Due to the polar symmetry of the original specimen, it was decided to use large strain-displacements equations in cylindrical form. The complete equations for normal strains are

$$\begin{aligned} \epsilon_{rr} &= \left(\frac{\partial u_r}{\partial r} \right) + \frac{1}{2} \left\{ \left(\frac{\partial u_r}{\partial r} \right)^2 + \left(\frac{\partial u_\theta}{\partial r} \right)^2 + \left(\frac{\partial u_z}{\partial r} \right)^2 \right\}, \\ \epsilon_{\theta\theta} &= \left(\frac{1}{r} \frac{\partial u_\theta}{\partial \theta} + \frac{u_r}{r} \right) + \frac{1}{2} \left\{ \frac{1}{r^2} \left(\frac{\partial u_r}{\partial \theta} - u_\theta \right)^2 + \left(\frac{1}{r} \frac{\partial u_\theta}{\partial \theta} + \frac{u_r}{r} \right)^2 + \frac{1}{r^2} \left(\frac{\partial u_z}{\partial \theta} \right)^2 \right\}, \quad (5.1) \\ \epsilon_{zz} &= \left(\frac{\partial u_z}{\partial z} \right) + \frac{1}{2} \left\{ \left(\frac{\partial u_r}{\partial z} \right)^2 + \left(\frac{\partial u_\theta}{\partial z} \right)^2 + \left(\frac{\partial u_z}{\partial z} \right)^2 \right\}. \end{aligned}$$

Here ϵ_{rr} , $\epsilon_{\theta\theta}$, and ϵ_{zz} are the strain components in the r , θ , and z directions, respectively, and u_r , u_θ , and u_z are the corresponding displacements. For the assumption listed above, i.e., $u_\theta = 0$, these equations become

$$\begin{aligned} \epsilon_{rr} &= \frac{\partial u_r}{\partial r} + \frac{1}{2} \left\{ \left(\frac{\partial u_r}{\partial r} \right)^2 + \left(\frac{\partial u_z}{\partial r} \right)^2 \right\}, \\ \epsilon_{\theta\theta} &= \frac{u_r}{r} + \frac{1}{2} \left\{ \frac{1}{r^2} \left(\frac{\partial u_r}{\partial \theta} \right)^2 + \left(\frac{u_r}{r} \right)^2 + \frac{1}{r^2} \left(\frac{\partial u_z}{\partial \theta} \right)^2 \right\}, \quad (5.2) \\ \epsilon_{zz} &= \frac{\partial u_z}{\partial z} + \frac{1}{2} \left\{ \left(\frac{\partial u_r}{\partial z} \right)^2 + \left(\frac{\partial u_z}{\partial z} \right)^2 \right\}. \end{aligned}$$

This approach was used to analyze the relatively complex deformation of the head of WCR specimen no. 1. The specimen, due to the rupture opening in one side, did not exhibit an axisymmetric deformation pattern, even though the head deformations were essentially symmetric. Due to the procedure by which the radius and length measurements were taken, this asymmetric curvature in the cylinder introduced a rigid body movement

component of deformation into the head data. It was determined that the center of the specimen head was offset with respect to the center about which the radial measurements were taken and that the head was tilted with respect to the plane of the reference end of the specimen. By assuming that each circumferential scribe line on the head of the specimen was still essentially a circle, the true center of the head and the corresponding true radii were determined by a non-linear least squares approach. The tilt component was removed by choosing as a new datum plane a plane parallel to the base of the specimen head.

Least squares techniques were applied to the resulting radii and lengths in an attempt to find a functional relationship to describe the deformation of the head. An analytical function would provide an easy way of evaluating the terms in Eq. (5.2). However, the success of this approach has been marginal to date.

By a combination of curve fitting with analytical, graphical, and numerical differentiation, Eq. (5.2) was used to convert the deformation data for the head of WCR specimen no. 1 to total strain components. The results of these calculations are shown in Fig. 5.6. The radial and circumferential components of strain versus radial distance are shown for each of three head diameters. For comparative purposes, the average strain (based on all three diameters) is also shown. The results indicate a central region of almost uniform strain.

Although the radial and circumferential strains should be the same at the center of the head the calculations did not show this equality. However, for small radii, i.e., near the center of the head, the errors involved approach the same magnitude as the quantities being measured and may be the major factor involved in this discrepancy.

The sharp fluctuation in radial strain and its value near the edge of the head is interesting since preliminary inelastic analyses predicted a more uniform distribution. No measurements were made of the outside edge of the head so that the strain calculations stop at the first increment from the edge as indicated in Fig. 5.6.

Specific conclusions regarding these results must await reduction of additional data. Reduction of the remaining data on this specimen as well as other specimens is continuing.

Fig. 5.6. Results of outer surface strain calculations for head of WCR specimen No. 1. (a) Diameters of head of WCR specimen No. 1 along which strains components were calculated. (b) Strains along diameter A-D. (c) Strains along diameter B-E. (d) Strains for diameter C-F.

5.5 High-Temperature Moire Strain Analysis
of an In-Plane Loaded Finite-Width Plate with a Circular Hole

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5.5.1. Introduction

As described previously,³⁰ the object of the research reported here is to strain analyze a finite-width stainless steel plate with a circular hole when it is subjected, at 1100°F, to an in-plane, unidimensional, reverse loading that produces elastic-plastic and creep deformations. In the investigation an attempt will be made to provide high temperature structural test data for comparison with independently obtained theoretical predictions and also to demonstrate the applicability of moire to analyze, at elevated temperatures, structures exhibiting elastic-plastic-creep behavior.

To conduct the test, a special loading frame, a special furnace and special measuring equipment are required. In the first three months, preliminary work has been conducted to build the equipment and prepare for application of the measuring technique.

5.5.2. The loading frame

The specimen is to be subjected to tension and compression loadings, and the reversal of loading must be applied to the specimen without any out-of-plane motion since this may introduce error in the photographic measurements required. Since the usually available testing machines are not suitable for meeting these requirements, a special loading frame was designed in an attempt to satisfy these conditions. A wooden mock-up was built prior to building the actual steel frame. It is planned to apply the load hydraulically with an intermediate reservoir for keeping the load constant.

5.5.3. Furnace

It is believed that the easiest, least expensive, and most reliable method for obtaining the desired high temperature is to use infrared lamps inside an asbestos box covered with metal foil. The temperature will be

measured and controlled with thermocouples. Preliminary testing of a mock-up of this system has shown that the required 1100°F can be reached. Preliminary results indicate that the variation of temperature can be maintained with $\pm 5^\circ\text{F}$.

5.5.4. Method of measurement

It is planned to photograph gratings on both sides of the plate. The gratings will be etched, and preliminary tests indicate that 500 lines per inch (lpi) can be etched successfully. An attempt is being made to obtain a higher density grating using the same technique. Also, electrolytic etching, which may be more effective, is being tried.

Preliminary tests have permitted photographing the 500 lpi at 1100°F, but thermal effects interfere with good photography of gratings of higher density. Changes in the furnace design are being introduced at present to try to overcome this difficulty.

5.5.5. Test specimen

The final specimen dimensions have been established. The test plate will be 7 in. wide by 1/2 in. thick, with a 1-1/2-in.-diam hole in the center. ORNL will provide three machined and heat treated test specimens. One of these, which will be used in preliminary tests, will be of an ordinary "off the shelf" 304 stainless steel material, while two will be from the ORNL reference heat of 304 stainless steel.

6. MATERIAL INVESTIGATIONS

R. W. Swindeman

6.1. Material Characterization

We have reported previously³¹ that the reference heat (No. 9T2796) of type 304 stainless steel being used for exploratory tests in the Structural Design Methods Program is weak compared to most other heats of type 304 stainless steel when the comparison is on the basis of short-time properties - i.e., up to 1000 hr in duration. Longer time creep

tests suggest that the material behavior alters after 1000 hr, and it shows properties more typical of type 304 stainless steel. We also observe anisotropy in the 1 in. plate product form. Specifically, higher strains occur in the thickness direction than in either the longitudinal or transverse. To see if this behavior is typical of plate product forms, we made post-test diametric measurements on a number of tensile specimens taken from other heats and product forms.³² Comparisons are shown in Table 6.1. These data represent the maximum and minimum diametric strains at a location about mid-way between the fracture and one shoulder of the tensile specimens. All plate specimens were tested parallel to the plate rolling direction. Heat (9T2796) is the only one which shows pronounced anisotropy. This has been found to occur from room temperature to 649°C (1200°F) and after times as long as 5000 hours under creep conditions. The reasons for this type of behavior are under study.

In other work, metallography has been obtained on specimens from the ORNL preliminary heat (heat 8043813) which had been creep tested to rupture at temperatures ranging from 538 to 649°C (1000 to 1200°F). This heat (No. 8043813) exhibited very good creep and rupture strengths and also excellent ductility. Electron transmission micrographs, reported elsewhere,³¹ have not been fully evaluated, but the cursory comparisons we have made with structures obtained for heat 9T2796 revealed very little difference between the two heats for comparable stress levels. A study of the microstructure by light microscopy (see Figs. 6.1 to 6.5) revealed that heat 8043813 is fairly resistant to microcrack propagation. (Microcracking in heat 9T2796 is being examined by Moteff.³³) Indeed, specimen P5 which ruptured after 7240 hr at 649°C (1200°F) showed a reasonable amount of necking in the failure region. Low cycle fatigue studies by Brinkman³⁴ at 593°C (1100°F) have also shown this material to resist microcracking even for tensile hold time conditions. Brinkman also reports the occurrence of a very fine $M_{23}C_6$ carbides in this material. Another feature which this heat exhibits, in contrast to 9T2796, is a greater magnetic response both before and after long-time creep testing. We attempted to evaluate the amount of ferrite by magnegage but could only conclude that it is less than 1%. Ferrite has been identified metallographically in heat 9T2796, but it amounts to even less than that found in 8043813.

Table 6.1. Comparison of anisotropy measurements
obtained from type 304 stainless steel specimens
tensile tested at 1100°F

Heat number	Specimen number	Product form	Minimum diametric strain* (%)	Maximum diametric strain* (%)	Ratio <u>minimum</u> <u>maximum</u>
9T2796	RP 15	Plate	11.8	15.0	.79
8043813	P 77	Plate	15.8	15.9	.99
600414	414-29	Plate	14.3	14.5	.98
300380	380-26	Plate	14.9	15.3	.97
K44086	086-20	Plate	14.5	15.5	.94
337330	330-14	Plate	12.5	13.2	.95
337187	187-13	Plate	14.4	14.7	.98
X22926	925-12	Plate	15.3	16.3	.94
9T2796	796-6	Bar	14.8	16.0	.92
55697	697-13	Bar	13.6	15.0	.91

*Minimum and maximum diametric strains
were obtained at the same section midway between
the fracture and one shoulder of a 0.250 diam
tensile bar. Specimens from plate were tested
in the rolling direction.

ORNL-Photo-2913-72

Fig. 6.1. Cross section of the rupture of type 304 stainless steel specimen P4 (Ht. 8043813) tested at 28 ksi and 649°C (1200°F). Failure occurred in 35.4 hours with 64.3 percent reduction in area. Notice the degree of grain distortion and fibrous tearing at the rupture surface. Etchant: Lactic acid, HNO_3 , HCl . Magnification: 50X.

ORNL-Photo-2914-72

Fig. 6.2. Cross section of the rupture of type 304 stainless steel specimen P3 (Ht. 8043813) tested at 20 ksi and 649°C (1200°F). Failure occurred in 1642 hours with 40.7 percent reduction in area. A significant amount of grain distortion and tearing is present at the fracture surface. The density of grain boundary fissures has increased. Etchant: Lactic acid, HNO_3 , HCl . Magnification: 50X.

ORNL-Photo-2915-72

Fig. 6.3. Cross section of the rupture of type 304 stainless steel specimen P5 (Ht. 8043813) tested at 17 ksi and 649°C (1200°F) for 5033 hours and then at 20 ksi for 2207 hours. Specimen ruptured with 28 percent reduction in area. Grain fissures are present but the actual failure is mixed, showing appreciable grain distortion and tearing. Etchant: Lactic acid, HNO_3 , HCl . Magnification: 50X.

ORNL-Photo-2916-72

Fig. 6.4. Cross section of the rupture of type 304 stainless steel specimen P6 (Ht. 8043813) tested at 35 ksi and 593°C (1100°F). Failure occurred in 287 hours with 42.6 percent reduction in area. Failure is mixed with grain boundary fissures and ductile tearing of grains. What appears to be a second phase (or recrystallization) in this photo and previous ones is actually Martensite which was introduced during metallographic preparation. This Martensite follows the same banding pattern as the slags and ferrite present in the structure. Etchant: Lactic acid, HNO_3 , HCl . Magnification: 50X.

ORNL-Photo-2917-72

Fig. 6.5. Cross section of the rupture of type 304 stainless steel specimen P13 (Ht. 8043813) tested at 45 ksi and 538°C (1000°F). Failure occurred in 837 hours with 28.8 percent reduction in area. This specimen exhibited a higher percentage of grain boundary failures than any of the previous specimens. Etchant: lactic acid, HNO₃, HCl. Magnification: 50X.

6.2. Creep Testing

We presently have eight creep machines allocated to exploratory testing in the Structural Design Methods program. Four of these machines are being used for uniaxial constant load tests which are carried to 5000 hr. Tests are now running at 15 and 25 ksi at 538°C (1000°F), 15 ksi at 593°C (1100°F), and 12.5 ksi at 649°C (1200°F). Two machines are being used for load change tests. One test, at 538°C (1000°F), is about half-way to termination while the other, at 649°C (1200°F), is nearing completion. The creep curve for the 649°C (1200°F) test is compared in Fig. 6.6 to constant load data and to predictions based on time- and strain-hardening models. In contrast to the behavior at 593°C (1100°F),³¹ no plastic flow was observed when the load was incremented upward at 500-hr intervals. This response is identical to that of the preliminary heat (8043813) at 649°C (1200°F) and again demonstrates an interaction between creep strain and plastic strain in the temperature range 538 to 649°C (1000 to 1200°F).

Two creep machines have been adapted for performing compressive creep tests. One machine, running at 593°C (1100°F) has accumulated over 2000 hr on a specimen at 15 ksi. A comparison of the tensile and compressive curves is shown in Fig. 6.7. We feel that the compressive curve is below the tensile partly because the shoulders and the extensometer (attached on the gage length) are hindering the diametric expansion. The second compressive creep machine is running at 649°C (1200°F) and 10 ksi. This test, with only 200 hr, exhibits a creep rate about 85% of the tensile rate.

We have modified the compressive creep specimen to reduce shoulder and extensometer restraints and also to allow cyclic tension-compression creep testing. We hope to have our cyclic creep testing facilities prepared in the next quarter.

A cyclic creep test was attempted in an electrohydraulic testing machine. The purpose of the test was to see whether cyclic hardening or weakening occurs in type 304 stainless steel at 593°C (1100°F) under cyclic load conditions. Such information is important to verify the auxiliary creep hardening rules proposed by ORNL⁹ for use in design analysis. The tests, which we intended to be ± 15 ksi with a 27-hr cycle, inadvertently started at +12.5/-17.5 ksi with a 3-hr cycle. After 24 hr we changed the

Fig. 6.6. Comparison of constant and step-load creep curves at 1200°F for type 304 stainless steel plate (Ht. 9T2796 re-annealed at 1093°C).

Fig. 6.7. Comparison of tensile and compressive creep curves at 1100°F and 15 ksi for type 304 stainless steel pipe (Ht. 9T2796 re-annealed at 1093°C).

stress ratio to +20/-17.5 ksi and again 24 hr later to +25/-20 ksi. Figure 6.8 shows a typical cycle at +25/-20 ksi. The creep portions of the cycle are quite close to the curves we obtained on virgin specimens at the same stress levels. The large total strain, about 4.8%, resulted from tensile yielding rather than creep.

6.3. Relaxation Testing

One short time relaxation test was performed this quarter in the electrohydraulic testing machine. This test was on a specimen (RR52) machined from 5/8-in.-diam bar (HRAP) and in the reannealed condition. The relaxation curve obtained from this specimen is compared in Fig. 6.9 to the curve for specimen (RR34) tested in the A479 condition. Both tests were performed at 0.5% total strain and 593°C (1100°F). The re-annealed specimen had a much lower initial stress and consequently its rate of relaxation is initially much less. At 100 hr the rates are about comparable.

A relaxation test was started in a pneumatic-hydraulic testing machine also. The specimen was machined from a section of 1-in. plate after reannealing and water quenching. The test, which was started at 13 ksi and 593°C (1100°F), relaxed to about 9.5 ksi in 350 hr. Both the initial plastic strain and relaxation rate indicated greater strength than we obtained for specimens which were annealed after machining.

6.4. Cyclic Testing

A number of room temperature cyclic tests were performed this quarter. Before discussing our results, a description of our testing method may be helpful, since our methods represent a significant departure from those used elsewhere, such as at ANL, ANC, and BMI.

For example, we use finite gage length specimens (1/2 in.) with the extensometer in the axial orientation. Tests are started in the tensile mode rather than compressive. For elevated temperatures, we use resistance furnaces rather than induction heating techniques. We can do these things because our cycling tests are aimed at generating stress-strain relationships rather than fatigue-life data. On the other hand, we have two

Fig. 6.8. Short time cyclic creep of type 304 stainless steel at 1100°F (Ht. 9T2796 re-annealed at 1093°C). History included 48 hr of creep cycling at lower stress amplitudes: +12,500/-17,500 and +20,000/-17,500 psi.

Fig. 6.9. Relaxation curves obtained on 5/8 in.-diam bar (HRAP) of type 304 stainless steel (Ht. 9T2796) at 1100°F and 0.5% strain.

particular problems which must be solved. The first is the elimination of buckling under high compressive loads. The second is the establishment of a reliable method for attaching an axial extensometer to the gage length and defining the gage length. A photo of our experimental set-up is shown in Fig. 6.10. Notice that although our columns are sturdy, we have not been using a die set, as employed elsewhere, to provide additional rigidity. We intend to do this in the future.

Our specimen has a 0.5-in. uniform gage length with a 0.25-in. diam. It possesses a rather sharp radius ($1/4$ in.) and hence we might expect severe strain concentrations at the ends of the gage length. We checked specimens of similar geometry some time ago by cementing $1/4$ -in. strain gages at the specimen midsection and comparing the hysteresis loops with those generated by clip-on extensometers mounted near the shoulders. The agreement was fairly good.^{35,36} Although we still lack complete confidence in this specimen design, we have no reason to question the validity of the stress strain data currently being generated. The exception, of course, is the high strain data at ranges in excess of 0.04 where pronounced buckling occurs. Further development of finite gage length specimens is anticipated and we will include finite element analyses of specimen designs along with experimental work.

The start of three cyclic tests is shown in Fig. 6.11 corresponding to strain ranges of 0.0042, 0.0096, and 0.021. In addition to showing the general shape of the first cycle for each range, Fig. 6.11 defines the meaning of the cyclic strain range, $\Delta\epsilon$, and the cyclic stress range, $\Delta\sigma$. The first quarter cycle falls, as expected, on top of the flow curves generated by monotonic tensile tests. The stress range, $\Delta\sigma$, is shown plotted against the cycle number in Fig. 6.12. All tests were carried for at least ten cycles since this number of cycles is recommended by ORNL⁹ for establishing the cyclic stress-strain relationship. At the lower strain ranges the tests were carried beyond ten cycles and terminated only when the number of cycles surpassed the allowed value in the ASME Code Case 1331-5 for each particular strain range. The purpose was to provide an estimate as to how much of an increase in $\Delta\sigma$ might be expected between the tenth cycle and the allowed design life. The data clearly show that only at very high strain levels is the hardening beyond ten cycles

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Fig. 6.10. Photograph of the equipment being used to perform strain-cycling tests on type 304 stainless steel at temperatures ranging from room to 649°C (1200°F).

Fig. 6.11. Tracings of the stress strain behavior during the first cycle for three tests at room temperature on type 304 stainless steel (Ht. 9T2796 re-annealed at 1093°C).

Fig. 6.12. Stress range vs cycles for type 304 stainless steel plate at room temperature (Ht. 9T2796 re-annealed at 1093°C).

appreciable. We expect that the picture will change at high temperatures, especially under continuous cycling conditions. Berling and Slot,³⁷ for example, show that significant hardening occurs in type 316 stainless steel between 10 and 100 cycles at 430°C (806°F). Our own data, although scanty, suggest this is also true for type 304 stainless steel at 427°C (800°F). A comparison of the monotonic hardening curve to cyclic hardening curves is shown in Fig. 6.13. Curves of this type have been developed by Berling and Slot,³⁷ and workers at BMI³⁸ for type 304 stainless steel, but published data have been for either stabilized conditions or half-life conditions rather than at ten cycles. It is, of course, important to recognize that the cyclic hardening curve is critically dependent on the strain ranges. For example, having subjected the specimen to several high strain cycles, we must continue to use the highest strain cyclic hardening curve for subsequent cyclic strain levels. On the other hand, time or cycle dependent recovery is possible, especially at elevated temperature. A great deal of effort is needed to establish the response at elevated temperature to changing strain ranges.

7. STUDY OF THE EFFECT OF BIAXIALITY IN CREEP-FATIGUE AT ELEVATED TEMPERATURES

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7.1. General Scope of Work

The primary objective of this program is to evaluate the influence of biaxial stress states on low-cycle fatigue behavior of type 304 stainless steel at high temperatures. Biaxial stresses are imposed on specimens as shown in Fig. 7.1; an axial stress through push-pull loading and a shear stress in a transverse direction through torsional loading. Fatigue behavior under the combination of the two is assessed on the basis of control data obtained from separate axial and torsional strain cycling. All tests are conducted at 1200°F except for some selected tests which are at 1050 and 1100°F. The tests at the lower temperatures are to determine whether or not a lower temperature has any significant effect on the

Fig. 6.13. Comparison of monotonic stress-strain curve with cyclic stress-strain behavior at 10 cycles. Type 304 stainless steel tested at room temperature (Ht. 9T2796 re-annealed at 1093°C).

Fig. 7.1. Tubular specimen and surface element — biaxial loading system.

characteristics of fatigue behavior observed at 1200°F. In addition to the primary objective, design parameters such as mean load, hold time, cumulative damage, and their effect on biaxial fatigue will be investigated.

7.2. Experimental Procedure

Combined biaxial strains are imposed through an electric-hydraulic closed loop testing machine, shown in Fig. 7.2(a). The specimen is inserted between the two loading components as shown in Fig. 7.2(b). The temperature environment is achieved through a silicon carbide element where electrical leads are connected to the aluminum metallized terminals. In order that a uniform temperature be controlled throughout the test section, two thermocouples are spot welded 3/4 inches apart on the specimen surface.

Two types of strain measurements are carried out — one in the longitudinal direction and the other in the diametral, where in the latter case the specimen test section is a modified hour-glass type. A significant objective of these two types of measurement is the determination of error that may exist in converting diametral fatigue data into longitudinal data and vice-versa through the use of Poisson's ratio values. A diametral extensometer is used to control the diametral strain. The extensometer with two types of knife edges is shown in Fig. 7.3. Figure 7.4 shows the extensometer mounted on a specimen.

7.3. Experimental Results

The basic axial and torsional strain control data for room temperature and 1200°F have been completed. Table 7.1 lists the pure torsion tests that have been conducted and Fig. 7.5 shows the data in terms of total angular range in degrees. The angular twist was performed in a completely reversed manner. The data are being converted to shear strains. The observed fatigue failure under torsional strain cycling consists of parallel cracks developing on 45° planes. Figure 7.6 shows torsional specimens and their mode of failure.

Table 7.2 shows axial strain-cycling data obtained from uniaxially loaded specimens tested at room temperature while Fig. 7.7 contains a plot of these data, as well as data from tests conducted at 1200°F. In

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ORNL-Photo-2920-72

Fig. 7.2. Biaxial strain cycling fatigue machine. (a) General layout of equipment. (b) Specimen attachment and measuring devices.

ORNL-Photo-2921-72

Fig. 7.3. Diametral extensometer with different knife edges.

ORNL-Photo 2922-72

Fig. 7.4. Diametral extensometer mounted on specimen.

Fig. 7.5. Torsional strain cycling data - type 304 stainless steel (Ht. 9T2796), specimens from 1-in. rods.

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ORNL-Photo-2924-72

(a)

(b)

Fig. 7.6. Fatigue failures under torsional strain cycling at room temperature. Type 304 stainless steel (HT. 9T2796), specimens from 1-in. rods. (a) Angle of twist = $\pm 6^\circ$. (b) Angle of twist = $\pm 15^\circ$.

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Fig. 7.7. Axial strain cycling data - type 304 stainless steel (Ht. 9T2796), specimens from 1-in. rods.

Table 7.1. Experimental results of pure torsion fatigue tests

Type 304 stainless steel at room temperature and 1200°F

Specimen no.	Outside diameter (in.)	Inside diameter (in.)	Temperature (°F)	Frequency (cpm)	Degree	No. of cycles
G ^a -6	0.460	0.340	1200	25	±6	585
G-7	0.460	0.340	1200	25	±3	2,060
G-9	0.470	0.350	1200	25	±10	230
G-10	0.465	0.345	1200	25	±2	5,234
G-5	0.460	0.340	Room	25	±3	12,655
G-3	0.468	0.348	Room	25	±6	3,130
G-8	0.460	0.340	Room	25	±15	425

^aG refers to specimens from bar no. 24.Table 7.2. Results from axial strain cycling tests
at room temperatureType 304 stainless steel (Ht. 9T2796),
specimens from 1" rods

Specimen no.	Strain range (%)	Cycles to failure
E 18	0.6	4,765
E 14	0.9	3,728
E 12	3.7	230
E 15	2.0	1,268
E 18	2.8	800
F ^a 1	1.4	2,000
F 3	0.5	12,050
F 14	1.0	3,170

^aF refers to specimens from
bar no. 15.

addition to continuous cycling, axial tests have been conducted at 1100 and 1200°F with "10 minute" hold times. The hold time, which occurs one time during a test, is introduced at the maximum tensile strain value and after n_1 cycles have been carried out. Subsequent to the hold time, the specimen is cycled to failure. The tests that have been performed are listed in Table 7.3 and the results for the 1200°F tests are also shown in Fig. 7.7.

Table 7.3. Results of hold-time fatigue tests at 1100 and 1200°F

Type 304 stainless steel (Ht. 9T2796),
specimens from 1" rods

Specimen no.	Temperature (°F)	n_1 ^a	n_1/N_f ^b (%)	Total strain (%)	Hold time (min)	Cycles to failure
F 11	1200	900	10	0.4	10	2800
E 16	1200	240	10	0.6	10	670
F 12	1200	240	10	0.6	10	560
F 15	1200	60	10	1.0	10	255
F 9	1100	900	10	0.4	10	4534
E 17	1100	900	10	0.4	10	4940
F 18	1100	900	10	0.4	15	3473

^a
 n_1 is the number of cycles completed
before the hold time is applied.

^b
 N_f is the number of cycles to failure
under continuous strain cycling.

Uniaxially loaded cumulative damage tests, in addition to those reported earlier,³⁹ were carried out at 1200°F. The results of these cumulative damage tests are presented in Table 7.4. The numbers and dimensions of the specimens taken from 1-in.-diam bar no. 15 are shown in Table 7.5. Table 7.5 includes the specimens listed in Table 7.4.

The strains at room temperature are measured through high elongation strain gages in a rosette configuration. The measured strains are calibrated against axial and torsional LVDT extensometers. The amplitudes of the calibrated strains are then generated through MTS control units.

Biaxial tests are in progress and will be reported in the next progress report. The specimen design being used for biaxial testing is shown in Fig. 7.8.

Table 7.4. Data from additional cumulative damage tests at 1200°F
Type 304 stainless steel (Ht. 9T2796), tubular specimens from 1" rods

Specimen no.	$\Delta \epsilon_1$ (%)	$\Delta \epsilon_2$ (%)	$\phi = \frac{\Delta \epsilon_2}{\Delta \epsilon_1}$	n_1	N_1	n_1/N_1	n_2	N_2	n_2/N_2	$R = 1 - \frac{n_2}{N_2}$	$\Sigma \frac{n_i}{N_i}$
F 7	0.7	2.4	3.43	300	1500	0.200	50	130	0.385	0.615	0.815
F 4	0.7	0.4	0.57	300	1500	0.200	2500	9000	0.278	0.722	0.478
F 6	0.4	0.7	1.75	900	9000	0.100	713	1500	0.486	0.514	0.586

Table 7.5. Dimensions and numbering of specimens
from 1" bar 15, type 304 stainless steel (Ht. 9T2796)

Specimen no.	Inside diameter (in.)	Outside diameter (in.)	Specimen no.	Inside diameter (in.)	Outside diameter (in.)
F 1	0.350	0.470	F 11	0.338	0.458
F 3	0.338	0.458	F 12	0.342	0.462
F 4	0.338	0.458	F 13	0.336	0.456
F 5	0.339	0.459	F 14	0.338	0.458
F 6	0.336	0.456	F 15	0.338	0.458
F 7	0.342	0.462	F 16	0.342	0.462
F 8	0.338	0.458	F 17	0.342	0.462
F 9	0.342	0.462	F 18	0.339	0.459
F 10	0.344	0.464	F 19	0.338	0.458
			F 20	0.342	0.462

ORNL-Photo-2925-72

Fig. 7.8. Fatigue specimen for testing under biaxial stress states.

8. ANALYTICAL RATCHETTING STUDIES

T. R. Branca
J. L. McLean

Teledyne Materials Research Co.

The Teledyne analytical ratchetting subcontract has been extended to include the following additional effort:

Verify that the latest version of the computer program ANSYS, which has a corrected kinematic hardening plasticity solution scheme, produces ratchetting results for the straight pipe that agree with those obtained in the initial effort.

Perform a complete ratchetting analyses of a representative axisymmetric nozzle in a spherical vessel and compare the results with the findings of the initial effort. The corrected version of ANSYS will be used for this analysis.

As with the previous ratchetting work, the biggest problem of the current study is getting the newest version of the ANSYS program up and running on the AEC computer at New York University. However, as with the previous ratchetting study, once these problems are resolved, the project should progress at a rapid pace.

8.1. NYU Computer - New ANSYS Tape

At our request Dr. Swanson made up a tape of the new version of ANSYS with classical kinematic hardening. However, the tape was written at the Westinghouse computer center, and as we have found out, tapes from the Westinghouse computer are not completely compatible with the NYU system. Therefore, it was necessary for Dr. Swanson to provide a small program to help interface between the systems.

With the assistance of Dr. Swanson, we have been able to read the tape, and we have completed analysis of a pipe ratchetting problem (TMR Report E-1414, Case 17).¹⁶

There still remains some question about the compatibility of plotting routines of the new ANSYS tape with the NYU system. However, we do not anticipate too much difficulty in resolving the problems if they occur.

To hamper our efforts at analyzing a nozzle configuration, the NYU system has been plagued recently with long periods of down time. For example, the computer was down from May 24 to May 31. This kind of interruption is delaying our progress somewhat.

8.2. Verification of ANSYS and Comparison of New Version with ANSYS Version Used for Pipe Ratchetting Study

As mentioned above, we have re-analyzed one of our previous pipe ratchet cases with the new version of ANSYS. Although we are currently using a bilinear stress-strain representation as opposed to the nonlinear representation used previously, the present results are reasonably close to those obtained previously with the old version of ANSYS.

Dr. Swanson has provided us with the new ANSYS solutions to the problems considered by Foster Wheeler (contract no. AT(45-1)-2171, Task No. 1, May 5, 1972) to help us verify the new ANSYS version. The results seem to compare very favorably. Consequently we feel confident in proceeding with the nozzle analysis.

8.3. Model Geometry

We have settled on the nozzle geometry that is shown in Fig. 8.1. The nozzle attachment consists of 414 elements. This number of elements is our compromise between a reasonably accurate analytical description and a manageable number of unknowns. With this model, we estimate the run time on the CDC 6600 to be approximately 2 hr per ratchet cycle.

8.4. Material Properties for Classical Kinematic Hardening

The stress-strain curves for the nozzle problem were obtained by applying the ORNL procedure (ORNL-TM-3736, March 1972) to the stress-strain curves used in the previous ratchetting study. The slope of the plastic portion of the stress-strain curve was determined from the total strains at 0.5% and 1%. The resulting yield points and slopes are given in Table 8.1.

Fig. 8.1. Finite element model to be used in nozzle attachment ratchetting analysis.

Table 8.1. Stress-strain input for ANSYS classical kinematic hardening

Temperature	σ at 0.5% strain	σ at 1.0% strain	$E \times 10^{-6}$	σ_y	Slope of plastic portion of σ - ϵ curve divided by E
70°F	43,500	47,800	28.3	40428	0.03039
600°F	24,500	26,700	25.4	22693	0.01732
800°F	22,700	24,900	24.1	20881	0.01826
1000°F	21,000	23,200	22.5	19175	0.01956
1100°F	20,200	22,400	21.7	18373	0.02028
1200°F	19,300	21,400	20.9	17553	0.02105

All other material properties are identical to those used in the previous ratchetting study.

8.5. Evaluation of the Elastic Thermal Stress Solution

A number of sections through the pipe and sphere wall will be examined to determine the maximum primary and secondary stress intensities occurring during the thermal down-transient. The formulas used to compute the stress intensities are generalizations of the formulas used for the straight pipe case. We have, for this study, written a program to read the ANSYS output tape and calculate the stress intensities at the different sections of interest. This program will automate our elastic evaluation and allow us to pin-point critical areas and times very easily.

8.6. Loading Conditions

There is still some question as to the particular down-transient to consider. We want to be sure that the stress intensities produced by the transient and pressure are well within the regions investigated in the pipe ratchetting study.

We have decided to consider the heat transfer coefficient for all the interior surface to be 50 Btu/hr.-in.²-°F. Also the hold temperature will be 1100°F.

9. HIGH-TEMPERATURE CREEP COLLAPSE TESTS OF PIPING ELBOWS

R. I. Jetter
J. Moldenhauer

Atomics International

Design work for the high temperature elbow creep test described in the previous progress report⁴⁰ is now about 80% complete. Current design efforts are being focused on a mechanism for loading and displacing the test assemblies, and on instrumentation.

Two changes have been made in the test as previously described. First, the location of the temperature controlling thermocouple has been shifted from the inside to the outside of the elbow. Second, the test enclosure argon cover gas requirement has also been deleted. It was found that the metal enclosure structure itself satisfied the test safety requirements.

A test specification has been completed and will be available for review in July.

10. DEVELOPMENT OF A SIMPLIFIED INELASTIC DESIGN ANALYSIS FOR PIPING SYSTEMS

G. H. Workman
Battelle-Columbus Laboratories

10.1. Progress During April 1972

During this month's effort the formulation of the uncoupled circularly curved pipe element was initiated. This element will utilize the same basic assumptions as reported earlier for the present uncoupled straight element. However, now both the inplane and out-of-plane moments vary along the bend in a more complex manner. These moments and torque are now represented by transcendental functions.

The basic moment equations along the length of the pipe have been generated and solved to obtain a closed-form solution for the elastic stiffness matrix of the element. Next month, the generation of the corresponding deflection patterns along the length of the pipe will be

initiated. Once the deflection patterns are known then the method of initial strains can be applied to complete the generation of this element.

10.2. Progress During May 1972

During this month's effort, the basic in-plane deflection pattern for the curved pipe element has been generated. Next month, the generation of the basic out-of-plane deflection pattern will be initiated. When the total deflection pattern for the curved element has been generated, a numerical integration technique will have to be created to finalize the element formulation.

It is anticipated that three or four more months will be required to complete the formulation of the circularly curved element.

The effort expended over the first five months on this program has been less than originally planned. The present rate of effort will continue through June, with an increased rate of effort (over originally planned) for the remaining six months of the program. It is presently anticipated that the overall objectives of this first year's effort will be met on schedule.

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