STP-NU-020

VERIFICATION OF ALLOWABLE STRESSES IN ASME SECTION III SUBSECTION NH FOR ALLOY 800H

ASME STANDARDS TECHNOLOGY, LLC

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FOREWORD

This document is the result of work resulting from Cooperative Agreement DE-FC07-05ID14712 between the U.S. Department of Energy (DOE) and ASME Standards Technology, LLC (ASME ST-LLC) for the Generation IV (Gen IV) Reactor Materials Project. The objective of the project is to provide technical information necessary to update and expand appropriate ASME materials, construction and design codes for application in future Gen IV nuclear reactor systems that operate at elevated temperatures. The scope of work is divided into specific areas that are tied to the Generation IV Reactors Integrated Materials Technology Program Plan. This report is the result of work performed under Task 1 titled “Verification of Allowable Stresses in ASME Section III, Subsection NH with Emphasis on Alloy 800H and Grade 91 Steel (a.k.a., 9Cr-1Mo-V or ‘Modified 9CR-1Mo’).”

ASME ST-LLC has introduced the results of the project into the ASME volunteer standards committees developing new code rules for Generation IV nuclear reactors. The project deliverables are expected to become vital references for the committees and serve as important technical bases for new rules. These new rules will be developed under ASME’s voluntary consensus process, which requires balance of interest, openness, consensus and due process. Through the course of the project, ASME ST-LLC has involved key stakeholders from industry and government to help ensure that the technical direction of the research supports the anticipated codes and standards needs. This directed approach and early stakeholder involvement is expected to result in consensus building that will ultimately expedite the standards development process as well as commercialization of the technology.

ASME has been involved in nuclear codes and standards since 1956. The Society created Section III of the Boiler and Pressure Vessel Code, which addresses nuclear reactor technology, in 1963. ASME Standards promote safety, reliability and component interchangeability in mechanical systems.

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ABSTRACT

Part I Base Metal - Databases summarizing the creep-rupture properties of alloy 800H and its variants were reviewed and referenced. For the most part, the database was judged to be adequate to meet the needs for time-dependent properties in the extension of alloy 800H in ASME Section III Subsection NH (III-NH) to 900˚C (1650˚F) and 600,000 hours. Procedures for analyzing creep and stress-rupture data for III-NH were reviewed and compared to the current procedure endorsed by the ASME Section II on Materials. The stress-rupture database for alloy 800H in the temperature range of 750 to 1000˚C (1382 to 1832˚F) was assembled and used to estimate the average and minimum strength for times to 600,000 hours.

Part II Weldments - Databases summarizing the tensile and creep-rupture properties of deposited weld metal and weldments for alloy 800H were reviewed and referenced. Procedures for analyzing creep-rupture data for temperatures of 750˚C (1382˚F) and higher were reviewed and used to estimate the weld strength reduction factors (SRFs) as a function of time and temperature for temperatures to 900˚C (1650˚F). The database was judged to be inadequate to meet the needs for the extension of the use of filler metal for alloy 800H in ASME Section III Subsection NH to 900˚C (1650˚F). Five appendices were included that 1) listed the data used in the evaluation of the SRFs, 2) provided the values for parametric constants in the models, 3) provided an example of the calculated SRFs for alloy 82, 4) recommended supplemental creep-rupture testing to expand the database and improve the estimation of SRFs for long-time service and 5) provided a summary of a parametric Finite Element Analysis (FEA) study of cross-weld samples.

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PART I – BASE METAL

1 INTRODUCTION

A collaborative effort has been established between the Department of Energy (DOE) and the American Society of Mechanical Engineers (ASME) to address technical issues related to codes and standards applicable to the Generation IV Nuclear Energy Systems Program [1]. A number of tasks have been identified that will be managed through the ASME Standards Technology, LLC (ASME ST-LLC) and involve significant industry, university and independent consultant activities. One of the tasks the *Verification of Allowable Stresses in ASME Section III, Subsection NH with Emphasis an Alloy 800H and Grade 91 Steel*. A subtask is the assessment of the data needed to extend the ASME Section III coverage of alloy 800H to 900˚C (1650˚F). To this end a review is provided here that identifies data sources and analytical procedures that have been used in code-related work on alloy 800 over the last 30 years. This review is followed by an evaluation of the long-time stress-rupture characteristics in the temperature range of 750 to 900˚C (1382 to 1650˚F).

2 IDENTIFICATION OF MATERIALS

Alloy 800H is one of three classes (or “grades”) of 33Ni-42Fe-21Cr alloy that are listed in ASME Section II and approved for construction of pressure boundary components. These are identified as UNS N08800, UNS N08810 and UNS N08811 for alloy 800, alloy 800H and alloy 800HT, respectively. There are other variants identified in international construction codes and databases. Often, the specifications for these variants fall within the ASME SB specifications so valuable information may be obtained from these sources. The history of the development of the three SB grades of alloy 800 has been provided by INCO alloys [2], [3]. Variants of alloy 800 were examined for both irradiation resistance [4] and steam generator requirements [5] and by 1975 several restricted chemistry versions of alloy 800 were available. Further evaluations were performed in Europe on the Sanicro 30 and Sanicro 31 alloys with emphasis on the influence of carbon, titanium and aluminum [6]. By 1989, three variants of alloy 800 were available in the German codes [7] and the German code KTA 3221.1 that was issued in 1993 provided design data for three materials: alloy 800 DE, alloy 800 Rk and alloy 800H [8].

ASME III-NH identifies the permitted SB specifications and associated product forms for alloy 800H (UNS N08810) in Table I-14.1. The ladle composition for the alloy 800H material may be compared to the other grades mentioned above in Table 1. Alloy 800 differs from alloy 800H in permitting carbon levels below 0.05%, annealing temperatures below 1121˚C (2050˚F) and finer grain size with ASTM grain size numbers above 5. Alloy 800HT requires carbon to be at least 0.06%, the aluminum plus titanium to be in the range of 0.85 to 1.2%, and the annealing temperature to be at least 1149˚C (2150˚F). The Japanese specification for alloy 800H is virtually identical to the ASME SB specification for alloy 800H. The three specifications identified in the German code KTA 3221.1 are included in Table 1. The German specifications require narrower ranges for nickel and chromium content. For grades 800 DE and 800 Rk, lower carbon is permitted and the maximum carbon is reduced relative to the ASME SB specifications. The ranges for aluminum and titanium are reduced and the maximum for both elements is reduced. The KTA 3221.1 specifications allow higher aluminum and titanium for the alloy 800 H grade. Both the minimum and maximum values are higher than for the ASME SB specification. All specifications, except for alloy 800 and alloy 800 DE, require grain sizes of ASTM No. 5 or coarser. The German specifications place additional requirements on phosphorus, nitrogen, cobalt and niobium. Additional product form chemistry requirements apply but they will not be presented here. The similarity in the chemical requirements for ASME and Japanese versions of alloy 800H suggest that data produced on materials from these sources should be interchangeable and useful in extending ASME III-NH to higher temperatures. Care is needed with respect to using data produced from material in conformance with the German specifications to assure that the material falls with the ASME SB specification for alloy 800H.

Table 1 - Comparison of Chemistries for Variants of Alloy 800

Element	ASME N08800 800	ASME N08810 800H	ASME N08811 800HT	DIN 800 DE	DIN 800 Rk	DIN 800 H	JIS-G-4904
Ni	30.0-35.0	30.0-35.0	30.0-35.0	30.0-32.5	30.0-32.5	30.0-34.0	30.0-35.0
Cr	19.0-23.0	19.0-23.0	19.0-23.0	19.0-22.0	19.0-22.0	19.0-22.0	19.0-23.0
Fe	39.5 min	39.5 min	39.5 min	bal	bal	bal	
C	0.10 max	0.05-0.10	0.06-0.10	0.03-0.06	0.03-0.08	0.05-0.10	0.05-0.10
Mn	1.50max	1.50 max	1.50 max	<1.5	<1.5	<1.5	1.50 max
S	0.015 max	0.015 max	0.015 max	<0.010	<0.010	<0.010	0.015 max
Si	1.0 max	1.0 max	1.0 max	<0.70	<0.70	<0.70	1.0 max
Cu	0.75 max	0.75 max	0.75 max	<0.15	<0.45	<0.45	0.75 max
Al	0.15-0.60	0.15-0.60	0.15-0.60	0.15-0.40	0.20-0.50	0.40-0.75	0.15-0.60
Ti	0.15-0.60	0.15-0.60	0.15-0.60	0.20-0.40	0.20-0.50	0.25-0.65	0.15-0.60
Al+Ti			0.85-1.20	<0.60	<0.70		
P				<0.015	<0.015	<0.015	
N				<0.03	<0.03	<0.03	
Co				<0.02	<0.45	<0.45	
Nb				<0.1	<0.1		
ASTM GS No.		≤5	≤5				≤5
Euronorm 103 GS				3 to 7	1 to 5	1 to 5	

3 AVAILABLE SOURCES FOR CREEP AND STRESS-RUPTURE DATA

Although sufficient tensile and creep-rupture data existed in the 1960s to gain ASME Boiler and Pressure Vessel (BPV) Code acceptance, Huntington Alloys Inc. (HAI) assembled an expanded database for alloy 800 from U.S. and European sources for a reevaluation of strength needed for further BVP code action in 1974. This information was intended for use in nuclear programs in [2], [9]. At that time, the European data provided to HAI included 302 creep-rupture tests. It is known that there were three specifications involved. In two of these specifications, the maximum carbon content was 0.030% and in the third the carbon range was 0.035 to 0.060%. Also, different limits were set for the titanium and aluminum contents. These data, provided by HAI for use by General Atomic Co. (GA), Westinghouse-Tampa (W-T), and ORNL, were retained at ORNL and included both Grade 1 (alloy 800) and Grade 2 (alloy 800H) materials. Some creep data were provided by HAI in the ASTM McBee card format. Other listings were in tables and hand plots. The temperatures for approximately 130 creep tests on alloy 800H ranged from 538 to 1093˚C (1000 to 2000˚F). The creep data were used by Sterling at GA to develop at creep law needed for construction of isochronous stress-strain curves [10].

To further assist in expanding the data base, ORNL placed a subcontract with Sandvik in 1976 to supply stress-rupture data and technical papers describing development work on Sanrico 30 and Sanrico 31 alloys [11]. Over 600 rupture tests were listed for a variety of chemistries, melting practices, fabrication practices, product forms and heat treatments. The Sanrico 30 heats were too low in carbon to qualify as alloy 800H but 19 of the 39 lots of Sanrico 31 exhibited chemistries that conformed to alloy 800H. Most lots of Sanrico 31 met the alloy 800H heat treating requirements. Testing temperatures ranged from 550 to 700˚C (1022 to 1296˚F). The emphasis of the research was for usage around 600˚C [11]-[14].

In 1978, three reports produced by W-T were combined in a review of the status of alloy 800 for steam generators [15]. The stress-rupture compilation included 162 results from tests in the range of 482 to 982˚C (900 to 1800˚F). Although the emphasis was on the properties of Grade 1 material (N08800), an interesting discussion of tertiary creep limit was included that bears on the tertiary creep limit of ASME III-NH. Much of this material was presented at Petten International Conference in 1978 [16], [17].

Also in 1978, Booker, Baylor and Booker re-assembled and analyzed the creep-rupture database for alloy 800H (N08810) [18]. They examined creep behavior, tertiary creep characteristics and stress-rupture. They reported creep data for eight lots tested in the range of 538 to 871˚C (1000 to 1600˚F). These included two product forms of a single heat (plate and tubing) and one lot whose chemistry did not conform to alloy 800H due to low carbon content. The creep data included the time to end "primary creep," the minimum creep rate and the time to tertiary creep as defined by the 0.2% offset strain from the minimum creep rate projection. They showed creep curves for 72 tests. Many of the creep data compiled were taken from the HAI data package [2], [9]. In their report, Booker, et. al. listed 485 stress-rupture data supplied by Sandvik for Sanicro 31 [11]. Included were 156 stress-rupture data for lots that conformed to the alloy 800H specification. Booker, et. al. performed extensive analyses of the creep data and proposed formulations to describe the temperature-stress dependencies of creep, rupture and tertiary limits.

A revised data compilation of creep, rupture and tensile data for alloy 800 (N08800) was issued by HAI in 1980 [19]. This compilation included the European test results that were accumulated in 1974. The listing of tensile data included results for 71 lots of cold drawn (CD) tubes, 2 lots of cold drawn (CD) rounds, and 10 lots of hot rolled (HR) plates. Creep-rupture data were included for the same product forms. A total of 228 test data covered the temperature range of 450 to 982˚C (842 to 1800˚F).

The accumulation of creep and stress-rupture data on variants of alloy 800 continued during the early 1980s. Andersson reported data on effects of composition, heat treatment and cold work on the tensile and stress-rupture of alloy 800H at 600˚C (1112˚F) [6], while Milička reported data on effects of prestraining on creep behavior of alloy 800H near 700˚C (1292˚F) [20]. The data in both papers were provided in graphical rather than tabular form.

In 1982, stress-rupture data were added to the data base accumulated by GA for a reevaluation of the strength of alloy 800H. These included 40 data from five lots of tubing produced by Sumitomo Ltd. and 39 data from Babcock & Wilcox Co. on bar and tubing. Data were restricted to the temperature range of 538 to 816˚C (1000 to 1500˚F). Analysis of the data was undertaken by ORNL, Mar-Test Inc and GA and led to the revision of allowable stress intensities for ASME Section III Code Case N-47 [21]. The data and results of the analysis were summarized in a report by Booker [22].

Creep-rupture of alloys 800 and 800H in air and helium were reported by Trester, et. al. in 1982 for temperatures in the range of 649 to 900˚C (1200 to 1650˚F) [23]. This work addressed such issues as the effect of carburization and aging on the yield and ultimate strengths, ductility and toughness and creep-rupture behavior. The report included a review of other work on helium effects and provided 45 references. Stress-rupture data from tests in “wet” helium were reported from four sources over the temperature range 649 to 760˚C (1200 to 1400˚F). Stress-rupture data from tests in “dry” helium were reported from three sources over the temperature range 649 to 816˚C (1200 to 1500˚F). Control data from tests in air were included. Creep curves were provided for 14 tests performed in air and helium at temperatures from 649 to 900˚C (1200 to 1650˚F).

Testing (tensile and stress-rupture) of alloy 800H forging at 649˚C (1200˚F) were begun at GA [24], [25]. In the mid-1980s, LSO, a program supported by GA Technologies Inc., was undertaken by ERA Technology Ltd. to explore the effect of compositional and fabrication factors on the tensile and creep-rupture behavior of alloy 800 [26]. The efforts were concerned primarily with low carbon and low aluminum plus titanium variants, but one series addressed alloy 800H. Creep-rupture tests on alloy 800H were performed on tubes from four casts and bars from two casts. The test temperatures ranged from 800 to 1000˚C (1482 to 1832˚F) for times to beyond 10,000 hours. Creep strains were determined by interruption of the tests for room temperature measurements. Data for 77 tests were provided in graphs and tables.

In the mid 1980s, a number of papers addressing HTGR materials technology were provided in a special issue of Nuclear Technology [27]. Materials included alloys 800H, 617, X and other candidates. Papers covered the status of the materials development work, the selection of metallic materials, microstructural characterization, creep properties, fatigue properties, tensile properties, fracture mechanics, gas/metal reactions, friction and wear, hydrogen permeation, irradiation behavior, design codes and nondestructive evaluation. Several papers included evaluations of alloy 800H. In particular, Sainfort, et. al. included stress-rupture curves for alloy 800H in helium and air to 750˚C (1382˚F) [28], Lee provided summary data for stress-rupture, minimum creep rate and time to tertiary creep in air and helium at 649 and 760˚C (1200 to 1400˚F) [29] and Schubert, et. al. provided summary data for stress-rupture and time to 1 percent creep for temperature to 950˚C (1742˚F) [30]. Data were provided as plots.

In the 1980s there was interest in using alloy 800H for advanced fossil energy applications. Here, alloy 800H was used in process heaters and heat recovery systems. Smolik and Flinn, for example, examined the stress-rupture of pressurized tubes in air, inert environments and oxidizing/sulfidizing environments at 871˚C (1600˚F) [31]. Over 40 tests ranging to beyond 3400 hours were included in the work and data were provided in a tabular form. About the same time, Taylor, Guttmann and Hurst reported results of stress-rupture testing of solution annealed, aged and carburized alloy 800H at 800˚C (1472˚F) [32]. Degischer, et. al. described the effect of solution temperature and aging on the creep behavior of two heats of alloy 800H at 800˚C (1472˚F) [33]. Creep data were provided as log creep rate versus log creep strain.

The very-high temperature gas cooled (VHTGR) reactor program undertook an extensive environmental creep testing effort in the 1980s at the General Electric Co. [34]. The activity examined two heats of alloy 800H. One heat was tested in both air and HTGR helium and the other heat in only air. Temperatures for 40 tests ranged from 750 to 1050˚C (1382 to 1922˚F) and times extended to beyond 10,000 hours. The reported data included the time to 1% total strain, the minimum creep rate, the time to the onset of tertiary creep, the time to 0.2% offset tertiary creep strain and rupture life. Notched-bar stress rupture testing was undertaken. The authors included an assessment of the data availability for alloy 800H as a function of temperature to determine the data requirements for code qualification to 954˚C (1750˚F).

The MHTGR-NPR program rekindled interest in restricted chemistry versions of alloy 800H in the U.S. [35]. In particular, there was interest in a version of alloy 800H with carbon near the minimum requirement of the specification (0.05%) and aluminum plus titanium at 0.5% or greater. As part of the program, efforts were made to reassemble the database and reevaluate compositional effects. Sources included the HAI compilations [2], [9], the ERA Technology Ltd. work [25], the Sandvik tests [11] and the Petten database [36]. The Petten database was quite extensive and covered several variants of alloy 800, cold work effects and environmental effects mostly derived from European research efforts. No tabular data were provided. Papers by Diehl and Bodmann [7], [37] provided further insight into the nature of the European database. Diehl and Bodmann summarized an examination of the specifications and strength characteristics of the variants of alloy 800 contained in the Hochtemperatur-Reaktorbau GmbH (HRB) material data bank. The HRB creep-rupture data included 4735 tests on 289 materials (lots) over the temperature range of 450 to 1205˚C (842 to 2200˚F). The variants were designated Alloy 800-Rk, Alloy 800-NT and Alloy 800HT and distinguished from one another on the basis of chemistry, heat treatment and grain size. The stress-rupture data based reassembled by McCoy for the MHTGR-NPR work included some of these U.S., European and Japanese data [38]. Most of the 79 heats and lots conformed to alloy 800H specification. A total of 838 rupture data were compiled in tabular form for temperatures from 538 to 816˚C (1000 to 1500˚F). Supplemental creep-rupture testing of a "reference" heat of alloy 800H was begun in 1990 [39]. A few tests in the temperature range of 538 to 816˚C (1000 to 1500˚F) were completed on base metal and weldment specimens before the MHTGR-NPR work was terminated. Additional testing of the alloy 800H reference heat was undertaken by Swindeman in 1992 [40]. Here, temperatures were in the range of 700 to 982˚C (1292 to 1800˚F).

A model for creep behavior of alloy 800HT was published by El-Magd, et. al. in 1996 [41]. The creep data were provided as log creep rate versus log time and log creep rate for temperatures in the range of 700 to 900˚C (1292 to 1650˚F).

Four significant contributions to the creep-rupture data base for alloy 800H were produced by the National Institute for Materials Science (NIMS) [41], [42], [43], [45]. Data were provided for six lots of tubing over the temperature range of 550 to 1000˚C (1022 to 1832˚F) [41]. Similarly, data were provided for six lots of plate materials over the same temperature range [42]. Data included minimum creep rate, the time to 1% total strain, the time to tertiary creep based on the 0.2% offset from the minimum creep rate projection and rupture life. Data at the lower temperatures extended to nearly 200,000 hours [43]. Creep data for a single bar product were provided along with relaxation data for temperatures to 800˚C (1472˚F) [45].

Finally, the status of the database at Petten was investigated recently. There were 1089 "creep" test results available for alloy 800H with temperatures ranging from 500 to 1000˚C (932 to 1832˚F). The data appear to be from German work on the HGR program.

4 DATA ANALYSIS PROCEDURES

The materials data currently provided in ASME Section II that are applicable to ASME III-NH include physical properties (Tables TE-1 through TE-5, Tables TCD, Tables TM-1 through TM-4 and Tables NF-1 and NF-2), short-time tensile properties (Table U, Table Y-1), buckling charts and design stress intensity values (Tables 2A, 2B and 4) corresponding to criteria identified in Appendix 2 of Section II. ASME III-NH provides additional materials data in the tables of Appendix 1-14. For purposes of high-temperature design, ASME III-NH includes stress-rupture tables, fatigue tables, creep-fatigue damage envelopes, creep-buckling charts, and isochronous stress versus strain curves in Appendix 1-14 and Appendix T. For alloy 800H, the coverage extends to 760˚C (1400˚F) and for times to $3x10^5$ hours. Fatigue curves extend to 10^6 cycles. The effects of service-aging on the yield strength and ultimate strength are included. Stress-rupture data for weld filler metals are included.

It is a matter of ASME policy that strength values for all "Code Books" be set or approved by BPV Section II. For new materials or extended coverage of existing materials, ASME often subcontracts with a consultant to derive the strength values for code cases or the appropriate tables in Section II-D. The strength values are based on the criteria developed by the specific construction code. Appendix 1 in Section II-D identifies the criteria for establishing the allowable stress for Tables 1A and 1B in Section II-D. Appendix 2 in Section II-D identifies the criteria for establishing the allowable stress intensity values for Tables 2A, 2B and 4 in Section II-D. However, Tables 2A and 2B do not cover temperatures where time-dependent properties control the allowable stress intensities. The criteria for establishing these time-dependent stress intensities are specified in ASME Section III, Subsection NH paragraph NH-3221 and differ from those ASME Section II-D Appendix 1 in several ways: (a) Appendix 1 has a creep rate criterion which is 100% of the stress to produce a creep rate if 0.01%/1000 hr., while paragraph NH-3221 has a total (elastic, plastic, primary plus secondary creep) strain criterion which is 100% of the minimum stress to produce 1% total strain in a specific time, say 100,000 hours; (b) Appendix 1 has a rupture strength criterion of F_{avg} times the average stress to produce rupture in 100,000 hours, while paragraph NH-3221 calls for 67% of the minimum stress to produce rupture in a specific time, say 100,000 hours; (c) Appendix 1 has a second rupture strength criterion of 80% of the minimum stress to produce rupture in 100,000 hours, while NH-3221 calls for 80% of the minimum stress to cause initiation of tertiary creep in a specific time, say 100,000 hours. The factor F_{ave} used in Appendix 1 has the value 0.67 or less and depends on the slope of the stress-rupture curve around 100,000 hours [46].

Over the years, the methods of data analysis needed to produce the tables and charts in ASME Sections II, III and III-NH have evolved and will continue to evolve. Several of the references identified above provide analysis procedures and it is beneficial to review some of these procedures as well as alternatives. First, the current procedures for processing creep and stress-rupture data for ASME II will be reviewed.

4.1 Current ASME Section II Procedures for Setting Time-Dependent Stress Allowables

The minimum data requirements for approval of new materials for elevated temperature construction are outlined in Appendix 5 of ASME Section II Part D. Generally, the data package is submitted as part of a code case that is applicable to a specific construction code, such as Section I or Section VIII, which covers high-temperature structural components. In addition to the construction code, the draft code case is concurrently submitted to Section II, which has the responsibility for setting stresses, and Section IX, which has the responsibility of approving the applicable rules for welded construction. As described above, consultants working under subcontracts to ASME process the data and develop stresses conforming to each of the criteria set forth in Appendix 1 of ASME Section II Part D. Although the consultants have not been restricted to the use of any specific procedure, the time-dependent allowable stresses for every new material approved in codes cases or incorporated into II-D for the last 12 years have been based on the Larson-Miller temperature-time parametric correlation method that employs a stress-dependent activation energy. Thus:

$$\left(\frac{1}{t_R} \right) = A \exp\left[\frac{-f_1(S)}{RT} \right] \tag{1}$$

Where t_R is rupture life or reciprocal creep rate, A is a constant, $f_1(S)$ is a function of stress, R is the universal gas constant and T is absolute temperature. Taking the log to base ten and rearranging produces the familiar Larson Miller parameter (LMP):

$$LMP = T\left(C + \log t_R\right) = \frac{f_1(S)}{2.303R} \tag{2}$$

Where C is log A and identified as the Larson-Miller parametric constant.

Typically, a stress function f(S) is formulated as a polynomial in log stress:

$$f(S) = \frac{f_1(S)}{2.303R} = a_0 + a_1 \log S + a_2 \left(\log S\right)^2 + a_3 \left(\log S\right)^3 + \ldots \tag{3}$$

where a_i is a series of constants that depend on the number of terms in the polynomial. Using a least squares fitting method in which log t_R is the dependent variable and T and log S are independent variables, the optimum values for C and a_i are determined. Although not explicitly required by Appendix 1 of ASME Section II-D, the consultants may employ a "lot-centered" procedure developed by Sjodahl that calculates a lot constant (C_h) for each lot along with the Larson-Miller constant, C, which represents the average lot constant (C_{ave}) for the heats (46). However, only C_{ave} is used to determine the S_{Rave} and S_{Rmin} values specified in Appendix 1. Determining S_{Rave} requires that eq. (2) be solved for S at 100,000 hours. The determination of S_{Rmin} in Appendix 1 requires that eq. (2) be solved for S at 100,000 hours after adjusting C by 1.65 multiples of the standard error of estimate (SEE) in log t_R. This minimum represents the 95% lower bound to the stress-rupture data. Thus, only a single analysis for rupture life is needed to assess two of the three time-dependent criteria in Appendix 1. The factor F_{ave} only applies to S_{Rave} and requires an estimate of the slope of the log S versus log t_R curve, n, at 100,000 hours. The F_{ave} value may be found by evaluating the partial derivative [∂f(S)/∂(log tR)]T at 100,000 hours. The value of F_{ave} is then given by the antilog of (-1/n). It has a defined upper limit of 0.67. Alternatively, F_{ave} may be determined as the ratio of the 10^5 hour strength to the 10^6 hour strength needed to produce a factor of 10 on life at 100,000 hours. Some insight into an MPC procedure for F_{ave} accepted by ASME has been provided by Prager, who provides an analysis for alloy 800H as an example [47]. He found that the F_{ave} for alloy 800H range from 0.640 at 816˚C (1500˚F) to 0.585 at 982˚C (1800˚F). The third criterion, S_c, rarely controls the allowable stresses in Tables 1A and 1B. Generally, it is only necessary to provide sufficient data to demonstrate that S_c does not control. Using eq. (2) and eq. (3), the procedures for the determination of Sc are similar to S_{Rave}, except that t_R is replaced by 1/mcr, where mcr is the minimum creep rate. Although the lot constants, variants within a lot, variants between lots and SEE of the log t_R can be produced in the analytical procedure required by ASME, it is important to recognize that the ASME II-D does not explicitly provide such information in the minutes of the responsible subgroup or in the stress tables. The minutes of ASME Section II show which time-dependent criterion controls the allowable stresses but Tables 1A and 1B in ASME Section II-D only show the controlling stresses.

4.2 ASME Subsection NH Procedures for Setting Time-Dependent Stress Intensities

The procedures used to produce the stress intensity values and minimum rupture strength values in the ASME III-NH Table I-14.4 and I-14.6 have not been standardized. However, the documentation of data used in the analyses and the details of the analytical procedures are contained in the minutes of the ASME Subgroup on Elevated Temperature Design. In some instances, reports and open literature publications provide additional information on these topics.

As mentioned above, the ASME III-NH time-dependent criteria considered for Table I-14.4 include (1) 67% of the minimum rupture strength as a function of temperature and time, (2) 80% of the minimum stress to produce the onset of tertiary creep as a function of temperature and time and (3) the minimum stress to produce 1% total strain as a function of temperature and time. Table I-14.6 provides the minimum rupture strength as a function of temperature and time. In contrast, the isochronous stress-strain curves in Appendix T of ASME III-NH represent the “average stress” vs. strain trend for temperatures and times covered by the code. For consistency within the ASME code, the same stress-rupture model developed for the ASME Section II-D tables should be used for the determination of the stresses for criterion (1) and Table I-14.6 in ASME III-NH. Unfortunately, this consistency is not always assured.

With respect to alloy 800H, as mentioned above, the original development of stress intensity values were described by Sterling [10]. A review of the procedures and an offering of alternate procedures were provided by Booker and co-workers [18], [48]. It was determined that the stress-rupture data did not support the values in the code case. Working with HAI, ORNL and others, GA Technologies revised the stress tables for CC N-47 [21]. Two of the three criteria for time-dependent stress-intensity values were addressed. For the determination of the minimum stress to rupture, SR_{min}, a correlation for the average rupture life was first developed that was a modification of the Larson-Miller parameter:

$$T\left[-b_0 + \log\left(t_R + 3\right)\right] = b_1 + b_2 \log S \tag{4}$$

Here, on the left side of eq. (4) b_o is the negative of the LM constant, C, in eq. (2) and the 3 hours are added to the rupture life, t_R, to improve the fit of the model to the data at short times. The right side of eq. (4) is a two-term polynomial in which the a_i terms of eq. (3) are labeled b_1 and b_2. This stress function is a simple power law and permits eq. (4) to be solved for stress in a straightforward procedure. The minimum rupture stress is obtained by introducing 1.65 multiples of the standard error of estimate, SEE, into the rewritten eq. (4):

$$\log S_{R_{\min}} = \left\{\left[\log\left(t_R + 3\right) + 1.65SEE - b_0\right]T - b_1\right\} \Big/ b_2 \tag{5}$$

The values provided in ASME III-NH Table I-14.6C were produced by this equation.

A correlation between the time to tertiary creep, based on the 0.2% offset definition, and the rupture life was used to develop a method to address the second of the three time-dependent criteria for setting allowable stress intensities. This correlation was a simple power law written in logarithmic form below:

$$\log t_3 = \log A + B \log t_R \tag{6}$$

Where A and B are constants. Using eq. (6), a rupture life, t’, corresponding to the t_3 of interest, was calculated and used in eq. (5) to determine the corresponding minimum stress for the initiation of tertiary in the time, t_3.

In CC 1592, the minimum stress to produce 1% total strain, $S_{1\%}$, did not control S_t for alloy 800H and no revisions were made in developing CC N-47 or ASME III-NH. A re-analysis of $S_{1\%}$, was undertaken by Booker, Baylor and Booker in 1976 [18]. Due to the difficulty in determining the minimum strength from the database, they defined $S_{1\%}$, as 80% of the average stress to produce 1% strain as a function of temperature and time. They showed that the $S_{1\%}$ did not control the S_t or S_{mt} above 593˚C (1100˚F) [18].

A Norton-Bailey power-law creep model was developed by Sterling for the time-dependent component of the isochronous stress-strain curves [10]. Here:

$$\varepsilon_c = DS^n t^m \tag{7}$$

where ε_c is creep strain and D, n, and m are constants. Sterling observed that the time to a given strain followed a "linear Larson-Miller type stress and temperature dependence."

For analysis purposes, he wrote eq. (7) as:

$$\log t = \left({u_1}/{T} \right) \log S + \left({u_2}/{T + u_3} \right) \log \varepsilon_c + \left({u_4}/{T + u_5} \right) \tag{8}$$

where u_i are constants determined by a least squares analysis. As mentioned above, this equation forms the basis for the time-dependent component of the isochronous curves in Appendix T. It represents average creep behavior. Accepting the assertion of Booker, Baylo, and Booker, one could calculate $S_{1\%}$ using the 80% factor and eq. (8).

4.3 A Few Other Data Analysis Procedures

Early work by HAI clearly demonstrated that the time dependency of rupture strength for alloy 800H follows a power law. Evaluations by Wattier [21], Prager [47], Booker [48] and Nippon Kokan [48] support the power law stress dependency with the Larson-Miller time-temperature parametric correlation.

Following Pepe [49], McCoy used the Minimum Commitment Method (MCM) procedure [50] for correlating stress-rupture life data for alloy 800H but provided no information regarding the parametric values or the stress dependency of the rupture life [38]. However, the MCM procedure produced isothermal stress-rupture curves for alloy 800H that approximated a power law for temperatures above 649˚C (1200˚F).

Although the Europeans have extensive experience in working with time-temperature parametric methods, they have favored isothermal stress-time correlations for determining average and minimum strengths. In the German code development, isothermal extrapolations are restricted to a factor of three in time [30]. This rule requires an extensive long-time data base since they provide allowable stresses for design up to 200,000 hours [51]. With respect to the nuclear construction codes, the papers by Diehl and Bodmann provide some insight into data processing procedures [7], [37]. Here, "the relationships between the characteristics of the creep and creep-rupture properties and the metallurgical parameters were investigated by multilinear regression analyses." These investigations involved isothermal data divided into groups (time segments). The regression analyses helped to identify three variants of alloy 800 (800 DE, 800 Rk and 800 HT) differing by chemistry and heat treatment (grain size). Then, stress-rupture curves and stress versus time to 1% total creep curves were produced for each variant. In contrast to the power law stress-life trend observed for alloy 800H, the log stress versus log time curves turn downward with increasing time for all variants. Of the three variants in the German code, only 800 HT is permitted for service above 700˚C (1292˚F). The duration of the data permitted the extension of allowable stresses to 100,000 hours. Stress values for 300,000 hours are provided in the KTA 3221 table but a note indicates that the extrapolation in time is beyond a factor of three.

Data correlation was undertaken at NIMS of the long-time tests results on alloy 800H [42], [43], [44]. The NIMS analysts favored the Manson-Haferd parameter in combination with a polynomial in log stress such as eq. (3). Although data for several lots approached or exceeded 100,000 hours, only four or five stresses were included at each temperature, and the estimation of the long time strength of each lot was based on the interpolation of the parametric fit to the data. Correlations included the strength-temperature dependence of rupture life, time to 1% total strain, minimum creep rate and time to 0.2% offset tertiary creep.

5 EVALUATION OF THE STRESS-RUPTURE OF ALLOY 800H AT 750˚C AND HIGHER

This section summarizes analyses that estimated the average and minimum rupture strength values for times to 300,000 hours and beyond. The evaluation consisted of the selection of applicable data, selection of analysis methods, estimation of stresses, and comparison of results with values from which ASME Section II-D and Subsection III-NH tables were derived.

5.1 Selection of Data

Stress rupture data were accumulated for more than one hundred lots of alloy 800H and its variants. The criteria for selecting usable data from this database were these:

Chemistry:	Carbon in the range of 0.05 to 0.1%,
	Al+Ti in the range of 0.5 to 1.2%
Grain size:	ASTM Grain Size Number 5 or lower
Anneal:	Annealed at 1120˚C or higher
Data Range:	Temperatures of 750˚C and higher
Products:	Plate, Bar, Pipe and Tubes

From the database, 37 lots were selected which produced 351 data at 750˚C and higher. Histograms showing the distribution of carbon and Al+Ti for the lots are provided in Figure 1 and 2. A histogram for the grain size distribution is shown in Figure 3. The distribution of temperatures is shown in Figure 4 The distribution of rupture lives is shown in Figure 5.

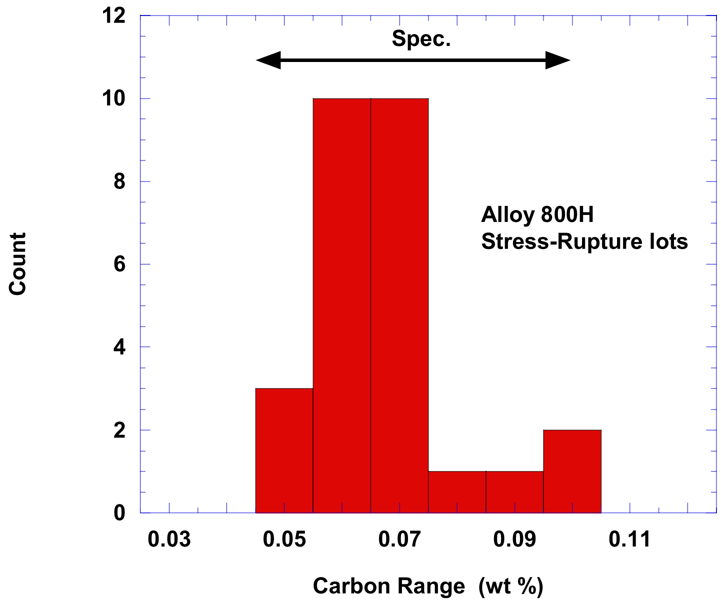

Figure 1 - Distribution of Carbon Contents in 37 Lots of Alloy 800H

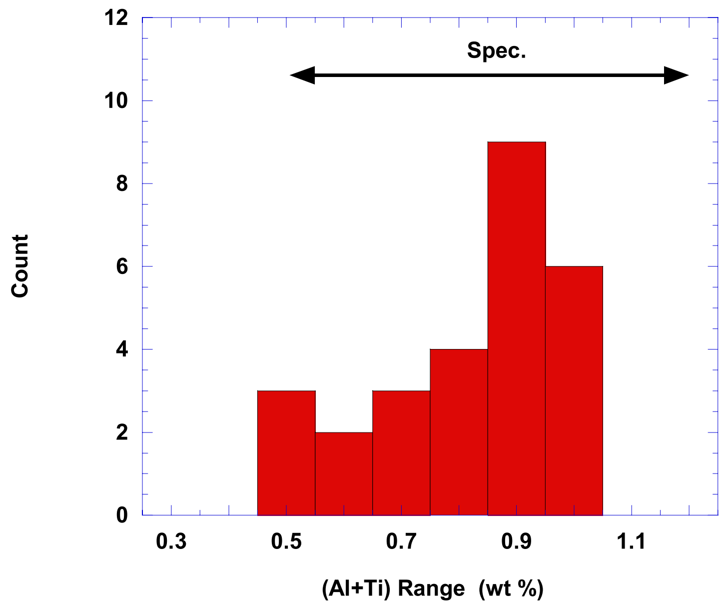

Figure 2 - Distribution of Al+Ti Contents in 37 Lots of Alloy 800H

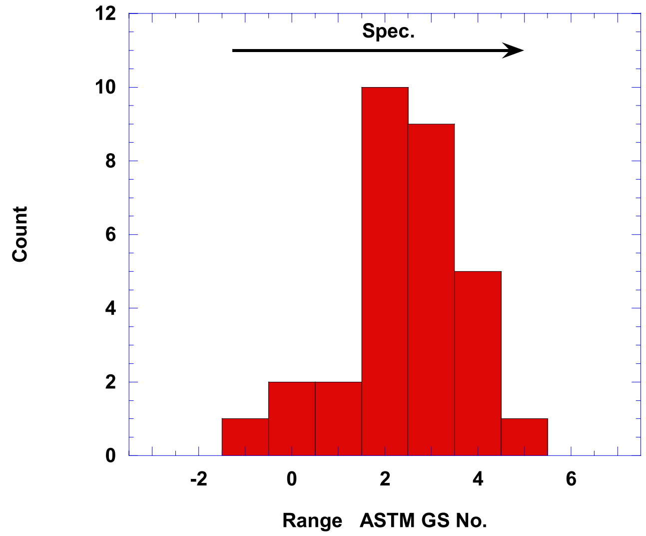

Figure 3 - Distribution of Grain Sizes in 37 Lots of Alloy 800
(ASME GS No. 00 was assigned a value of -1)

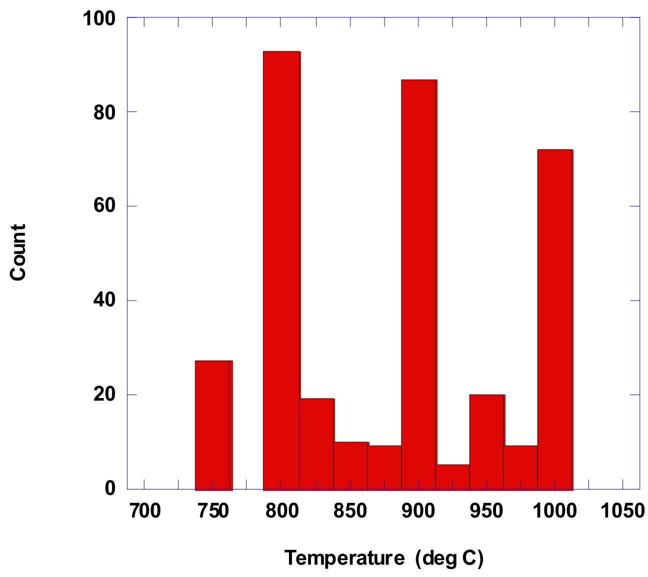

Figure 4 - Distribution of Testing Temperatures for 37 Lots of Alloy 800H

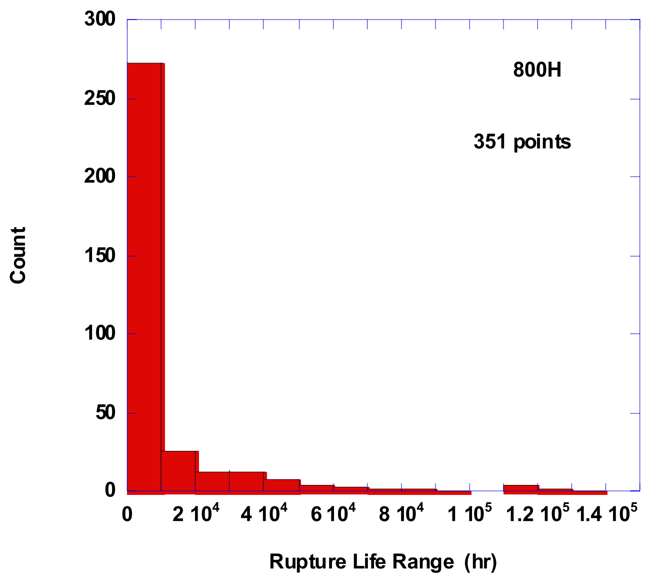

Figure 5 - Distribution of Rupture Lives for 37 Lots of Alloy 800H

5.2 Selection of Analysis Methods

As described in the review section of this report, many analysis methods were examined over the years [18], [21], [22], [38], [39], [42], [43], [44], [46], [47], [48], [49], [50]. Since it was the intent of the effort reported here to extend the current Subsection III-NH stress allowable stress intensities (Table I-14.4C) and minimum stress values (Table I-14.5C) to higher temperatures and longer times, an analysis consistent with previous "code" analyses was needed. Also, it was judged to be necessary that the analysis would produce values close to those in ASME Section II-D 1B when the criteria in

Table I-100 in II-D were invoked. The detailed analysis procedures used to set the II-D values were not published nor were they in the Code committee minutes. However, a paper by Prager provided general guidelines for the evaluation of alloy 800H for temperatures above 760˚C [47]. Here, the Larson-Miller (LM) time-temperature parametric approach was selected and parametric constant of 15.21805 was reported. Other parametric approaches were cited.

For the analysis reported here, the Larson-Miller parameter, in combination with a polynomial in log stress, was selected. See equations 2 and 3 above. Both global and lot-centered approaches were included.

Results:

The fit of the LM parameter to the high-temperature data is shown in Figure 6. The optimized parametric constant, C, was 15.12487. This number was close to the value reported by Prager (15.21805). The coefficients for the stress function were as follows:

$a_0 = 29648.78$

$a_1 = -7334.877$

$a_2 = 1903.854$

$a_3 = -619.4775$

The standard error of estimate for the fit was approximately 0.29 log cycle (in life). A histogram showing the distribution of the residuals (log tr – calculated log life) is shown in Figure 7, while the variation of residuals with life, stress and temperature are shown in Figure 8, Figure 9 and Figure 10, respectively. The plots revealed no gross trends, although a few test data at 800 and 900˚C appeared to exceed the life expectations by significant margins.

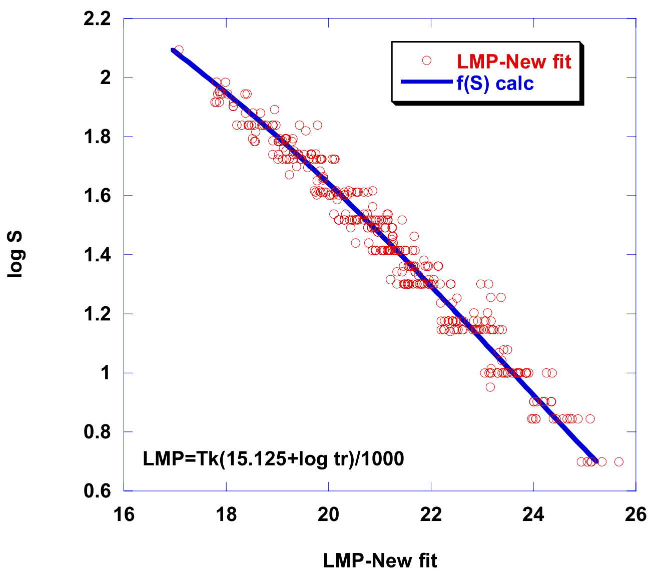

Figure 6 - Log Stress vs. Larson Miller Parameter for Alloy 800H

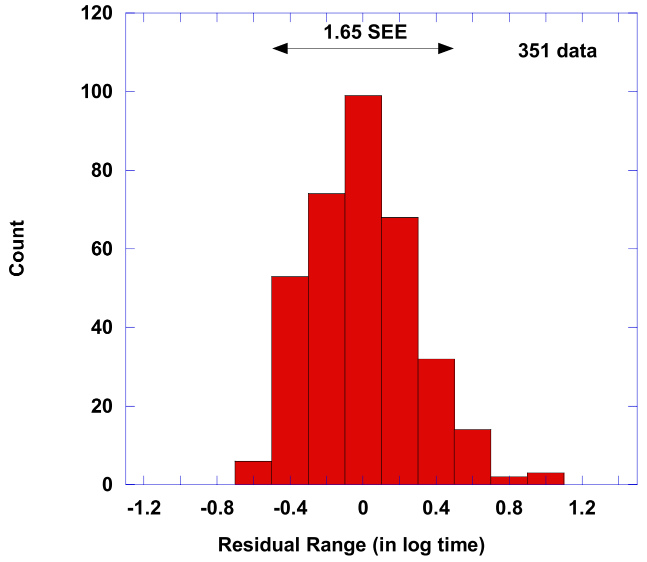

Figure 7 - Histogram of Residuals for Fit of LM Parameter for Alloy 800H

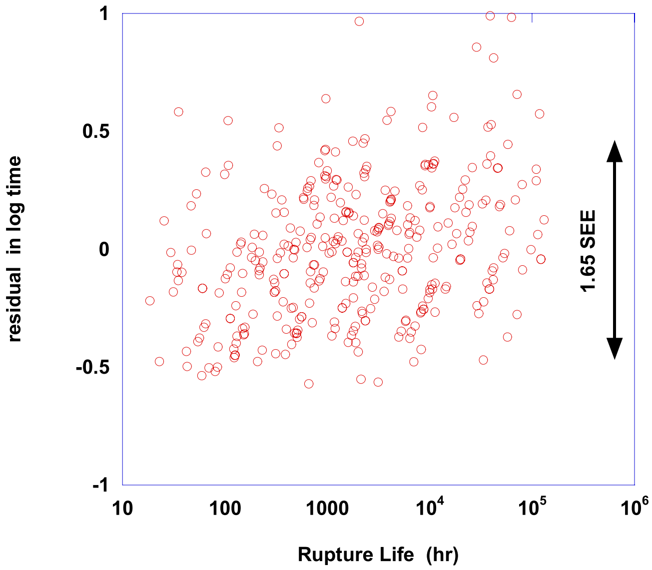

Figure 8 - Residuals vs. Rupture Life for LM Parameter Fit to Alloy 800H

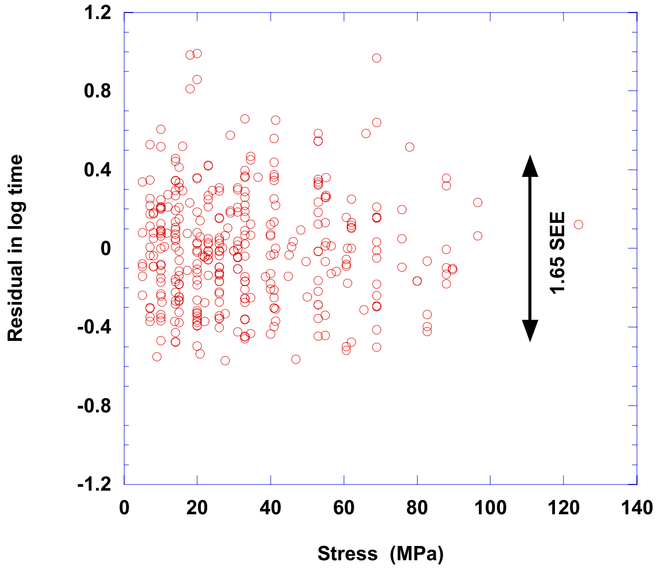

Figure 9 - Residuals vs. Stress for LM Parameter Fit to Alloy 800H

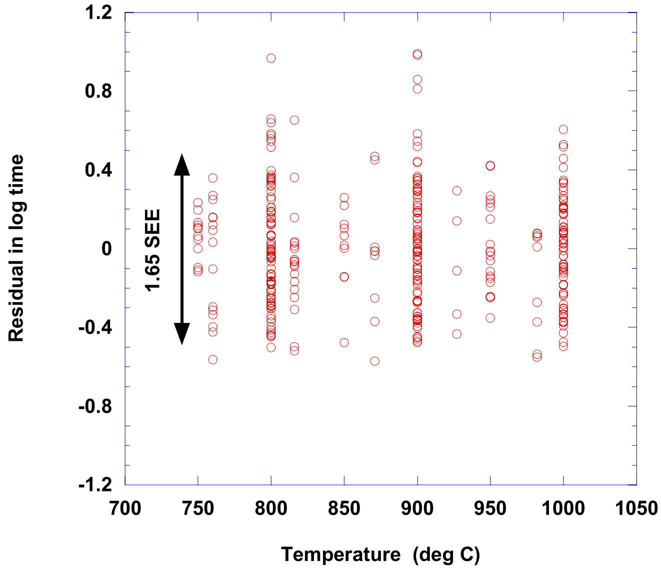

Figure 10 - Residuals vs. Temperature for LM Parameter Fit to Alloy 800H

It was expected that the lot-centering method would improve the fit to the data and permit some quantitative estimates of the influence of chemistry or microstructure on strength. However, the method was not very satisfactory. First, a single lot of plate product from the NIMS file (fdA) was examined. This material produced a C value of 18.02. Then the analysis of the NIMS file for six plate products was undertaken. This lot-centered analysis changed the LM constant for lot fdA to 16.45. Then all 37 lots were analyzed. The LM constant for lot fdA dropped to 15.66. The average LM constant for 37 lots was 15.93, somewhat higher than the value for the "global" analysis described above. The table below provides data for three lots–one from each of three groups.

Table 2 - Effect of Data Selection on the LM Constants, C, for Three Lots in a Lot-Centered Analyses

Lot	Group	Group C_{ave}	C-in-Group	C-in-All
fdA	-	-	-	18.02*
fdA	NIMS plates	16.48	16.45	15.66**
HH8099A	-	-	-	17.07*
HH8099A	HAI	17.47	17.43	15.89**
AED	-	-	-	11.52*
AED	UK	11.05	10.95	15.82**

*value as a single lot analysis,

**value for the lot within the 37 lots

Clearly, the UK lots that included bar and tube products were distinctly different from the HAI and NIMS lots and contributed to the lower value of C for the average of the 37 lots (15.92). One reason for the significant change in the C value between the single lot analysis and the multi-lot analysis was associated with the restriction on the stress function, f(S). One stress function was "forced" on all lots in the lot-centered analysis. More sophisticated lot-centering methods were available that would relax this restriction but these were not used in this work [50]. The global approach was selected as being the most representative of the current "Code" methodology. The times and stresses were estimated from the LM constant and polynomial coefficients given above for the global analysis.

The "average strength," S_{Rave}, and "minimum strength," S_{Rmin}, for 100,000 hours were calculated for temperatures from 750 to 900˚C. The minimum strength was based on the stresses corresponding to a rupture curve displaced to shorter life from the average curve by 1.65 multiples of SEE in log time. These S_{Rave} and S_{Rmin} values are listed in Table 3.

Table 3 - Calculated Stresses for 100,000 Hours (MPa) Which Form the Basis for the Time-Dependent Allowable Stresses in ASME II-D.

Temperature (˚C)	Average Strength	Minimum Strength
750	34.9	28.8
775	28.6	23.3
800	23.3	18.8
825	18.9	15.2
850	15.3	12.2
875	12.4	9.77
900	9.97	7.84

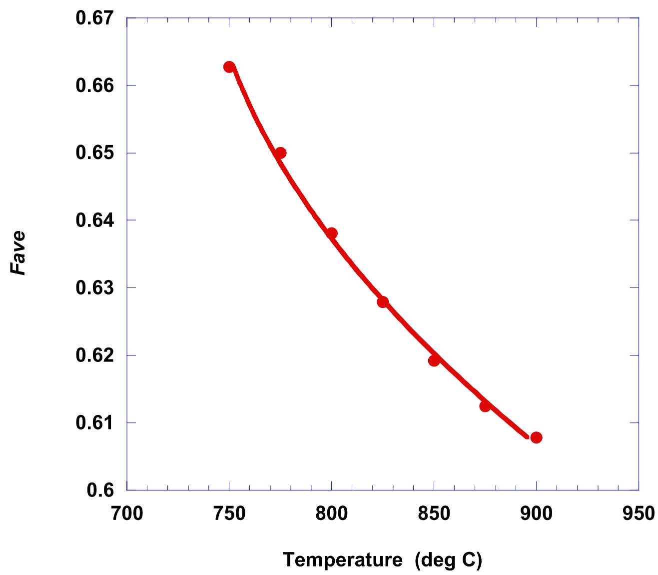

Figure 11 - F_{ave} vs. Temperature for Alloy 800H

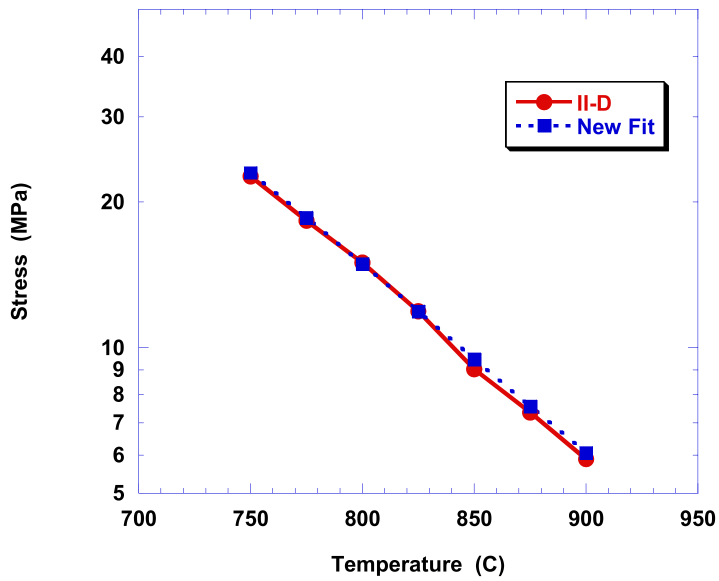

Figure 12 - Comparison of ASME II-D Stresses with the New Fit for Alloy 800H

As mentioned in the review section of this report, other methods of analysis have been used to estimate the long-time strength of alloy 800H. Several of these did not extend to the temperatures of interest in this work. McCoy, however, using the Minimum Commitment Method (MCM) provided estimates to 816˚C [38]. McCoy also cited strength estimates by Pepe who examined several parametric procedures extending into high temperatures [50]. NIMS employed the Manson-Haferd parametric procedure to estimate the strength of individual lots over a broad temperature range [42], [43]. These results may be compared to the analysis report here for 800˚C and are shown in Table 4 below. The strength at 800˚C represented by this work falls within the scatter of the other predictive procedures.

Table 4 - Comparison of the Average Strength of Alloy 800H at 800˚C and 100,000 Hours from a Number of Sources

Source	Strength	Number	Parameter	Products
This work	23.3	37	L-M	all
NIMS	25.3	6	M-H	plates
McCoy	26.5	69	MCM	all
Pepe	21	30	MCM	all
Pepe	23.9	30	L-M	all
Pepe	22.1	30	O-S-D	all

L-M Larson-Miller; M-H Manson-Haferd;

MCM Minimum Commitment Method; O-S-D Orr-Sherby-Dorn

5.3 Example of the Addition to III-NH Table I-14.6C

Figure 13 plots the calculated minimum stress rupture curves for temperatures of 750˚C to 900˚C. Included in the plot are the current III-NH values for 750˚C. The curves extrapolate the times to at least 600,000 hours and cover stresses to as low as 6 MPa at 900˚C.

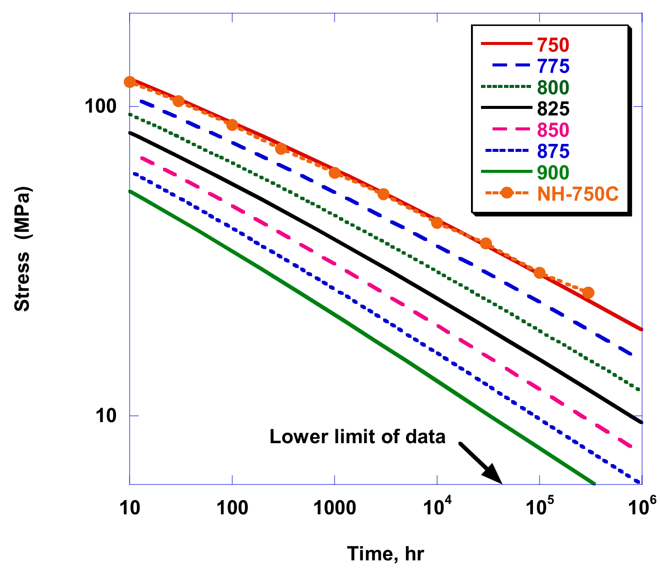

Figure 13 - Minimum Stress-to-Rupture vs. Time for Alloy 800H

6 SUMMARY AND RECOMMENDATIONS

The sources for high-temperature creep-rupture data for alloy 800H and its variants were reviewed and the development allowable stresses for pressure code construction was traced with emphasis on ASME Section III, Subsection-NH.

Criteria for setting stresses and data analysis procedures needed to develop allowable stresses were reviewed. Procedures used by ASME Section II were compared with those of ASME Section III, Subsection-NH.

The materials covered in references provided in this report were carefully reviewed to show compliance with the requirements of the alloy 800H specifications applicable to ASME Section III, Subsection-NH, and a subset was selected for the estimation of long-time rupture strength in the temperature range 750 to 900˚C (1382 to 1650˚C).

Sufficient data exited to permit the extension of the time-dependent allowable stress intensity values in ASME III-NH to 900˚C (1650˚F) and 600,000 hours.

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PART II - WELDMENTS

1 INTRODUCTION

A collaborative effort has been established between the Department of Energy (DOE) and the American Society of Mechanical Engineers (ASME) to address technical issues related to codes and standards applicable to the Generation IV Nuclear Energy Systems Program [1]. A number of tasks have been identified that will be managed through the ASME Standards Technology, LLC (ASME ST-LLC) and involve significant industry, university and independent consultant activities. Task 1 in this effort has several goals. The first goal is to assess the status of the databases for alloy 800H and its weldments and identify the data needed, if any, to extend the ASME Section III-NH coverage of alloy 800H to 900˚C (1650˚F) for service life for times approaching 600,000 hours. The second goal is to review the database for grade 91 steel and its weldments and identify the data needed, if any, to provide confidence that the steel will meet the performance requirements for service to times approaching 600,000 hours. Task 1 is primarily concerned with Code criteria related to tensile and creep rupture properties. Other tasks in the DOE-ASME project address cyclic service conditions. This report is the fourth in a series of reports that concerned alloy 800H [2]-[4]. The first three addressed the tensile, stress-rupture and creep databases for alloy 800H. This report reviews the database for deposited weld metal and weldments.

2 IDENTIFICATION OF MATERIALS

Alloy 800H is one of three classes (or “grades”) of 33Ni-42Fe-21Cr alloy that are listed in ASME Section II and approved for construction of pressure boundary components. The three grades are identified as UNS N08800, UNS N08810 and UNS N08811 for alloy 800, alloy 800H and alloy 800HT, respectively. Alloy 800 (N0880) corresponds to a relatively fine-grained annealed condition normally used at lower temperatures where creep strength is not an important consideration. Alloy 800H (N08810) corresponds to a relatively coarse-grained material (ASTM grain size number 5 or greater) with a carbon range of 0.05 to 0.10% which is typically annealed around 1150˚C (2175˚F). This material is approved for construction to 982˚C (1800˚F) under the rules of ASME Section VIII. Alloy 800HT (N08811) requires carbon to be at least 0.06%, the aluminum plus titanium to be in the range of 0.85 to 1.2% and the annealing temperature to be at least 1149˚C (2150˚F). This stronger version of alloy 800H is used when creep strength is important and relaxation cracking is not of great concern. Other variations of alloy 800 exist in the German Code KTA 3221.1 [5], and these are described briefly in an earlier report [2]. Only alloy 800H is permitted under the rules in ASME III-NH and an additional restriction requires the Al+Ti content to be in the range of 0.4 to 1.2%. The specific grade of base metal and its associated properties are important considerations in this review which includes the data produced on weldments that may rupture in the base metal heat affected zone or the base metal itself.

Typical base metal chemistries are provided in Table 5. Included are three ASTM grades, three DIN grades and one Japanese grade.

Table 5 - Comparison of Chemistries for Variants of Alloy 800

Element	ASME N08800 800	ASME N08810 800H	ASME N08811 800HT	DIN 800 DE	DIN 800 Rk	DIN 800 H	JIS-G-4904
Ni	30.0-35.0	30.0-35.0	30.0-35.0	30.0-32.5	30.0-32.5	30.0-34.0	30.0-35.0
Cr	19.0-23.0	19.0-23.0	19.0-23.0	19.0-22.0	19.0-22.0	19.0-22.0	19.0-23.0
Fe	39.5 min	39.5 min	39.5 min	bal	bal	bal	
C	0.10 max	0.05-0.10	0.06-0.10	0.03-0.06	0.03-0.08	0.05-0.10	0.05-0.10
Mn	1.50max	1.50 max	1.50 max	<1.5	<1.5	<1.5	1.50 max
S	0.015 max	0.015 max	0.015 max	<0.010	<0.010	<0.010	0.015 max
Si	1.0 max	1.0 max	1.0 max	<0.70	<0.70	<0.70	1.0 max
Cu	0.75 max	0.75 max	0.75 max	<0.15	<0.45	<0.45	0.75 max
Al	0.15-0.60	0.15-0.60	0.15-0.60	0.15-0.40	0.20-0.50	0.40-0.75	0.15-0.60
Ti	0.15-0.60	0.15-0.60	0.15-0.60	0.20-0.40	0.20-0.50	0.25-0.65	0.15-0.60
Al+Ti			0.85-1.20	<0.60	<0.70		
P				<0.015	<0.015	<0.015	
N				<0.03	<0.03	<0.03	
Co				<0.02	<0.45	<0.45	
Nb				<0.1	<0.1		
ASTM GS No.		≤5	≤5				≤5
Euronorm 103 GS				3 to 7	1 to 5	1 to 5	

A number of filler metals have been used for joining similar and dissimilar metal welds with alloy 800H. Some compositions are listed in Table 2 for coated electrodes for shielded metal arc welding (SMAW) included in the AWS 5.11 specification. Only one of these filler metals, alloy A (ENiCrFe-2), is permitted in ASME III-NH according to Table I-14.1(b). Table I-14.10 C-1 provides stress factors for the bare electrode equivalent (ENiCrFe-2) used for SMAW. The database reviewed here includes alloy 132, alloy A, alloy 617 and 21/33/Nb, which is considered to be a matching filler metal for alloy 800H. Emphasis is on alloy A.

Table 6 - Comparison of Chemistries for Coated Filler Metal Electrodes

Element	Alloy 132 ENiCrFe-1 (W86132)	Alloy A ENiCrFe-2 (W86133)	Alloy 182 ENiCrFe-3 (W86182)	Alloy 617 ENiCrCoMo-1 (W86117)	21/33/Nb
C	0.08 max	0.10 max	0.10 max	0.05-0.15	0.06-0.12
Mn	3.5 max 1.0-	3.5	5.0-9.5	0.3-2.3	1.6-4.0
Fe	11.0 max	12.0 max	10.0 max	5.0 max	Rem
P	0.03 max	0.03 max	0.03 max	0.03 max	0.03 max
S	0.015 max	0.02 max	0.015 max	0.015 max	0.02 max
Si	0.75 max	0.75 max	1.0 max	0.75 max	0.6 max
Cu	0.50 max	0.50 max	0.50 max	0.50 max	-
Ni	62.0 min	62.0 min	59.0 min	Rem	30.0-35.0
Co	-	0.12 max*	0.12 max*	9.0-15.0	-
Ti	-	-	1.0 max	-	-
Cr	13.0-17.0	13.0-17.0	13.0-17.0	21.0-26.0	19.0-23.0
Nb	1.5-4.0	0.5-3.0	1.0-2.5	1.0 max	0.08-1.5
Mo	-	0.5-2.5	-	8.0-10.0	0.5 max

Notes: * Co 0.12 max when specified by purchaser; max for other elements is 0.50.

Compositions for bare filler metal electrodes (SFA-5.14) are listed in Table 3. Only ERNiCr-3 (alloy 82) is permitted for use by ASME III-NH, according to Table I-14.1(b), and Table I-14.10 C-2 provides stress factors for joints with this alloy.

Table 7 - Comparison of Chemistries for Bare Filler Metal Electrodes

Element	Alloy 82 ERNiCr-3 (N06082)	Alloy 617 ERNiCrCoMo-1 (N06617)
C	0.10 max	0.05-0.15
Mn	2.5-3.5	0.3-2.3
Fe	3.0 max	5.0 max
P	0.03 max	0.03 max
S	0.015 max	0.015 max
Si	0.50 max	0.75 max
Cu	0.50 max	0.50 max
Ni	67.0 min	Rem
Co	0.12 max*	9.0-15.0
Ti	0.75 max	-
Cr	18.0-22.0	21.0-26.0
Nb	2.0-3.0	1.0 max
Mo	-	8.0-10.0

Notes: * Co 0.12 max when specified by purchaser;
max for other elements is 0.50.

3 REVIEW OF DATABASES FOR DEPOSITED FILLER METALS AND WELDMENTS

Early data on filler metals and weldments used for alloy 800 and nickel base alloys were summarized in The Elevated-Temperature Properties of Weld-Deposited Metal and Weldments (ASTM STP No. 226) [6]. Pages 154 to 170 of the report provided McBee-type data sheets for a number of filler metals. Two data sheets are provided for alloy 132 deposited filler metal. Two data sheets are provided for alloy 132 filler metal in alloy 800H plates. The results of short-time stress-rupture testing were given for testing in the temperature range of 760 to 982˚C (1400 to 1800˚F). Most weldment ruptures occurred in the weldment fusion line.

York and Flury performed a literature search for a suitable filler metals for alloy 800 and selected Incoloy 88 and 182 filler metals for joining alloy 800 [7]. It was reported that weldments from the two filler metals exhibited similar tensile and creep-rupture properties for temperatures less than 649˚C (1200˚F). Tensile data to 760˚C (1400˚F) and creep data to 649˚C (1200˚F) were provided. This work was in support of the fast-breeder reactor (FBR) program which had a need for a steam generator operating at less than 649˚C (1200˚F).

Klueh and King investigated the elevated tensile properties of ERNiCr-3 weld metal [8].

Tensile data on deposited alloy 82 filler metal to 732˚C (1350˚F) were reported. Again, this work was in support of the FBR program needs.

King and Reed investigated the weldability of alloy 800 [9]. They examined the hot cracking tendencies of seven heats of alloy 800 with varying carbon, aluminum and titanium contents. The ratio (Al+Ti)/(C+Si) was found to be a reasonable predictor of cracking behavior in the Tigmajig test. No tensile or creep data were gathered.

Further studies by Klueh and King in support of the FBR program were published in 1978 and 1979 and included creep and stress-rupture behavior of ERNiCr-3 weld metal [10], [11]. Data for deposited alloy 82 filler metal were reported to 732˚C (1350˚F).

Sartory required a creep law for an inelastic ratcheting analysis of a 2¼Cr-1 Mo steel pipe joined to type 316H stainless steel using alloy 82 filler metal [12], [13]. The creep law was developed and revised from test data on coupons machined from a dissimilar metal weld test article. Data were in the range of 510 to 566˚C (950 to 1050˚F).

Booker and Strizak produced cyclic data on weld-deposited alloy 82 at 649˚C (1200˚F) **Error! Reference source not found.**. Hold times at constant stress were introduced in tensile or compression and strains were reversed by strain-rate control to produced creep reversed by plasticity or plasticity reversed by creep. Tests were also performed with creep reversals in both tension and compression. No effort was made to develop expressions for the creep behavior.

Klueh and King examined the thermal aging behavior of alloy 82 weld metal and weldments [15]. Aging was performed at 510 and 566˚C (950 and 1050F). Tensile testing was performed to 677˚C (1250˚F) and creep-rupture tests to 566˚C (1050˚F).

Nippon-Kokan (NKK) reported the properties of Tempaloy 800H tubes welded with matching filler metal and alloy 82 [16]. Information included composition, microstructures, cross weld hardness and tensile properties for as-welded and solution-annealed weldments in 11-mm plates. The tensile data indicated higher yield strengths than for base metal for the as-welded cross-weld samples for temperatures to 1000˚C (1832˚F) but the same ultimate strength. No stress-rupture data for weldments are provided.

Data for pressurized alloy 800H tubes containing butt welds were reported by Stannett and Wickens [17]. Alloy 82 and 182 fillers were used. Testing was at 550 and 700˚C (1022 to 1292˚F). All tube burst failures occurred in the base metal.

In 1982, Klueh and J. F. King examined the elevated-temperature tensile and creep-rupture behavior of alloy 800H/ERNiCr-3 Weld Metal/2¼Cr-1Mo steel dissimilar-metal weldments [18]. Creep-rupture data extended to 732˚C (1350˚F).

McCoy and King investigated the tensile and creep-rupture properties of weld-deposited alloy A (EniCrFe-2) and alloy 82 filler metal and weldments including alloy 800H and Hastelloy X [19]. Tensile data on deposited alloy A weld metal went from 23 to 871˚C (70 to 1600˚F) and creep rupture data were gathered from 482 to 760˚C (900 to 1400˚F). Tensile and creep-rupture data for weldments were produced to 649˚C (1200˚F) for both filler metals. Testing data for aged weldments were included.

Lindgren, Thurgood, Ryder and Li reviewed the mechanical properties of welds in commercial alloys for high-temperature gas-cooled reactor components in 1984 [20]. They presented creep-rupture data for several filler metals and weldments used for joining alloy 800H and dissimilar metal tubes or pipes. Included were alloy 88 and alloy 188, alloy 82 and alloy 182. Plots of stress-rupture behavior were shown for temperatures to 760˚C (1400˚F).

In the same issue of Nuclear Technology, Bassford and Hosier discussed the production and welding technology of some high-temperature nickel alloys and provided guidance and data for welding alloy 800H for applications up to 790˚C (1450˚F) [21]. Stress-rupture data for all-weld metal were tabulated for alloy A and alloy 82 to 982˚C (1800˚F).

Schubert, Bruch, Cook, Diehl, Ennis, Jakobeit, Penkalla, te Heesen and Ullrich reviewed the creep-rupture behavior of candidate materials for nuclear process heat applications [22]. The paper provided one figure that plotted stress versus rupture life for alloy 82 and a 21/33/Nb at 850 and 950˚C (1575 and.1650˚F) The alloy 82 weld metal was weaker than average strength alloy 800H while the 21/33Nb matching filler metal appeared to have strength comparable to the base metal.

King and McCoy reported on the weldability and mechanical property characterization of weld-clad alloy 800H tubesheet forging. Tensile properties were provided for Inconel 82 weld-deposited cladding for temperatures to 649˚C (1200˚F) [23]. Data were gathered for composite and base metal samples over the same temperature range. Failure locations at 649˚C (1200˚F) often occurred at the weld interface.

In 1986, an INCO brochure provided a table for the stress-rupture for strength of alloy A and alloy 82 for temperatures in the range of 538 to 982˚C (1000 to 1800˚F) and times to 10,000 hours [24]. Also, a figure was provided for the stress-rupture of deposits from welding electrode 117 in comparison to alloy 800HT for temperatures in the range of 649 to 982˚C (1200 to 1800˚F) and time to 10,000 hours. About the same time, Bassford provided tensile and stress-rupture data for alloy 117 and alloy 112 deposited weld metal and cross welds in alloy 800H [25]. Temperatures ranged to 1093˚C (2000˚F).

A Survey and Guidelines for High Strength Superheater Materials- Alloy 800H was compiled for the Electric Power Research Institute in 1987 [26]. This report included a "steel maker's search on alloy 800H" by three participants: Sumitomo Metal Industries, Ltd., Nippon Steel Corp. and Nippon Kokan K. K. (NKK). The reviews drew heavily on the studies of alloy 800H that were performed in support of the high-temperature gas-cooled reactor programs (in the U.S., UK and Germany) and the fast breeder reactor programs in the U.S. In the summary section, plots for tensile data were supplied that were constructed from seven sources and ranged to 1100˚C (2000˚F). Several filler metals including alloys 82 and 182 were listed and both deposited metal and joint configurations were included. Stress-rupture data were provided as a stress versus Larson Miller parameter plots. Again, both deposited metal and joint data were included. However, the data did not appear to be original data but rather were derived from processed curves or tables. The review by Sumitomo Metal Industries, Ltd. was the most extensive with respect to filler metals. Of the 193 references, there were 32 references that addressed weld metal and weldment issues. About 14 of these references reported mechanical behavior such as tensile or creep-rupture properties. About half of these were of Japanese origin. Figures were provided that were reproduced from many of these references.

McCoy produced tensile and creep test data for a heat of alloy 800H in 1993. Data for deposited alloy 82 weld metal and weldments were provided [27], [28]. Tensile data ranged to 871˚C (1600˚F) and creep-rupture data range to 816˚C (1500˚F).

4 DATA ANALYSIS

The materials data for base metals currently provided in ASME Section II that are applicable to Section III-NH include physical properties (Tables TE-1 through TE-5, Tables TCD, Tables TM-1 through TM-4 and Tables NF-1 and NF-2), short-time tensile properties (Table U, Table Y-1), buckling charts and design stress intensity values (Tables 2A, 2B and 4) corresponding to criteria identified in Appendix 2 of Section II. Section III-NH provides additional materials data in the tables of Appendix 1-14. For purposes of high-temperature design, Section III-NH includes an extension of the tensile strength values (Table NH-3225-1) and the yield strength values (Table I-14.5), maximum allowable stress intensity values (Table I-14.2), allowable stress intensity values as a function of temperature and time (Tables I-14.3 and I-14.4), expected minimum stress-to-rupture tables (Table I-14.6), stress-rupture factors for weldments (Table I-14.10), design fatigue tables (Fig. T-1420-1), creep-fatigue damage envelopes (Fig. T-1420-2), creep-buckling charts (Fig. T-1522) and isochronous stress versus strain curves (T-1800) in Appendix 1-14 and Appendix T. For alloy 800H, the coverage extends to 760˚C (1400˚F) and for times to $3x10^5$ hours. Fatigue curves extend to 106 cycles. The effects of service-aging on the yield strength and ultimate strength are included (NH-2160 and Table NH-3225-2). The Section III Code Case N201-4 contains data tables and figures that are intended to be consistent with Section III-NH. No data for deposited filler metals or weldments are provided in either Section II or Section III-NH. Instead, the stress-rupture factors for weldments are provided for some combinations of base metals and filler metals. Stress-rupture factors for weldments with alloy A (ENiCrFe-2) welds and alloy 82 (ERNiCrFe-3) joining alloy 800H are provided in Table I-14.10, as mentioned above. Values for the factors range from 1.0 to 0.59 for alloy A over the temperature range from 427 to 760˚C (800 to 1400˚F) and from 1.0 to 0.54 for alloy 82.

Over the years, the methods of data analysis needed to produce the tables and charts in ASME Sections II, III and III-NH have evolved and will continue to evolve. The procedures for establishing the Section II Table 1A and 1B allowable stresses were reviewed in prior reports on this project [2]-[4]. Also, the Section II procedures for determining the Y-1 and U values were reviewed earlier [2]. Methods for extending the S_{Y1} and S_U values in Section III-NH to 900˚C (1650˚F) were recommended [2]. Section II procedures for establishing time-dependent allowable stresses were reviewed [3], [4]. At present, however, there is no well-established procedure for determining the values for the stress-rupture factors (SRFs) for weldments provided in Section III-NH. In the case of the austenitic alloys, the SRFs have been based on the ratio of the deposited weld metal strength to the base metal strength for the specific temperatures and times provided in the stress factor table. To some extent, the weldment strength has been "considered" in establishing these ratios, but it has not been established whether small cross-weld specimen data should be included in the analysis that determines the strength ratios. In this report, deposited filler metal and weldment data will be treated separately sometimes and together at other times. Although tensile properties of weldments are not considered in the Section III-NH, the available properties are discussed below and compared to base metal properties. Then the stress-rupture properties will be compared to base metal.

4.1 Tensile Data

Procedures for analyzing the base metal tensile data to produce S_{Y1} and S_U values were outlined previously [2]. The analysis makes use of a trend curve based on the ratio of elevated temperature strength to the room temperature strength as a function of temperature [29], [30]. Since few tensile data exist for the deposited weld metals, a trend curve for weld metal is of limited value in a statistical sense, but a comparison of the weld data or weldment data with the base metal trend curve enables an estimate of the similarity or difference in short-time behavior. In this report, however, the comparison will be between the available weld metal data and curves constructed from the Y-1 and recommended S_{Y1} values for yield behavior and the U and recommended S_U values for the ultimate tensile strength.

Figure 14 compares the yield strength for alloy A weld metal with alloy 800H. The curve for alloy A was developed by INCO [24] while the datum points were obtained from McCoy and King [19]. The alloy 800H curve represents the Y-1 and S_{Y1} trend curve anchored to the minimum specified room-temperature yield strength for alloy 800H (172 MPa). The average yield strength curve would be anchored to 225 MPa at room temperature [2]. It is clear that alloy A weld metal in the as-deposited condition is much stronger than alloy 800H. The same is true for the U and S_U trend curve as may be seen in Figure 2.

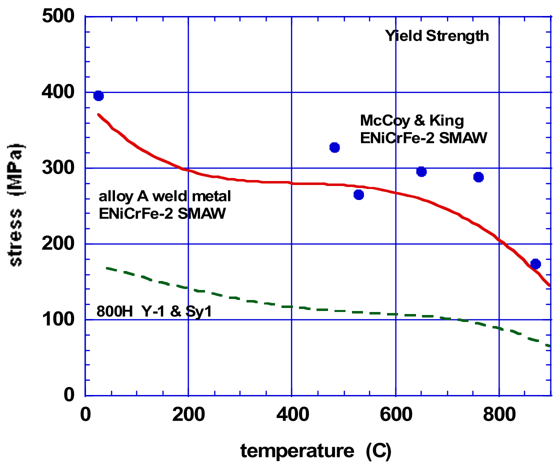

Figure 14 - Comparison of the Yield Strength for Alloy A Weld Metal with Alloy 800H

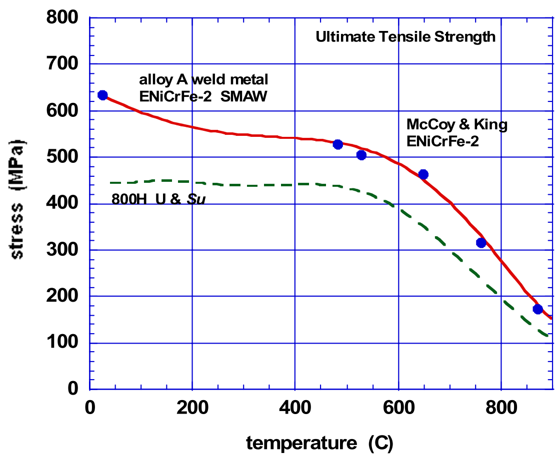

Figure 15 - Comparison of the Tensile Strength for Alloy A Weld Metal with Alloy 800H

Figure 16 and Figure 17 provide data for the 21/33Nb filler metal with the Y-1 and S_{Y1} trend curve curve and the U and SU trend curve for alloy 800H base metal. Also included are the trend curves for alloy A developed by INCO. Here, it may be seen that the 21/33Nb weld metal produces slight higher yield strengths than alloy A but similar ultimate tensile strengths.

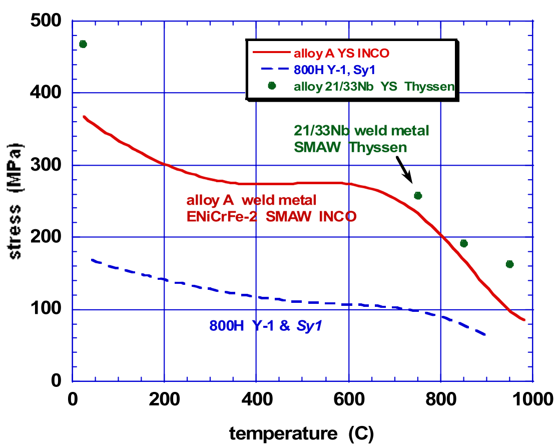

Figure 16 - Comparison of the Yield Strength for 21/33Nb Weld Metal with Alloy 800H and Alloy A Weld Deposit

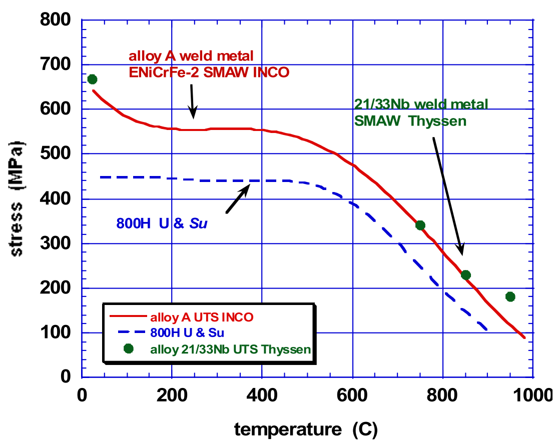

Figure 17 - Comparison of the Tensile Strength for 21/33NB Weld Metal with Alloy 800H and Alloy A Weld Deposit

Figure 18 and Figure 19 show comparisons of the strength of alloy 617 filler metal deposits with those of alloy A and alloy 800H. The tensile yield and ultimate strengths of deposits from the alloy 117 electrodes are much stronger than those of alloy A and alloy 800H. The material is clearly "overmatched" in strength with alloy 800H from this aspect.

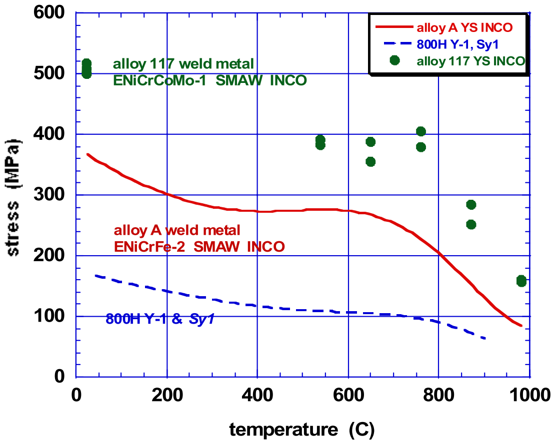

Figure 18 - Comparison of the Yield Strength for Alloy 117 Weld Metal with Alloy 800H

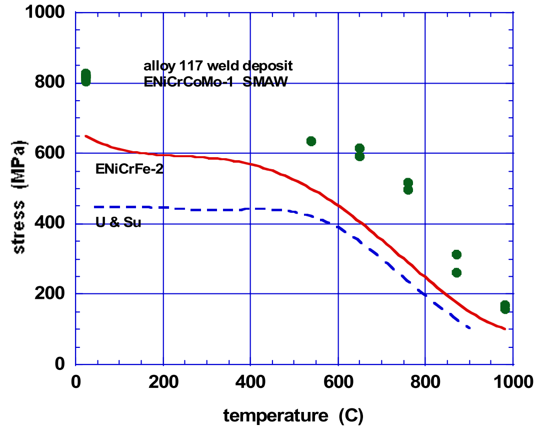

Figure 19 - Comparison of the Tensile Strength for Alloy 117 Weld Metal with Alloy 800H

Strength curves for the weld metal produced by the alloy 82 wire (ERNiCrFe-3) are shown in Figure 20 and Figure 21 where they may be compared to data for the alloy 182 electrode and alloy 800H base metal. The INCO curves indicate that the weld metal deposited from the alloy 82 wire has slightly more strength than weld metal deposited from alloy 182 electrodes. The strengths of both weld metals are roughly comparable to alloy A weld metal. Typical data produced on alloy 82 weld metal are shown in Figure 9 and Figure 10. Yield strength data for four lots extracted from the literature exhibit considerable scatter and generally fall below the curve developed by INCO. Yield strength data remain well above the Y-1 and S_{y1} strength curves for alloy 800H. Ultimate tensile strength data for alloy 82 weld metal generally fall below the curve developed by INCO but are above the U and S_U strength curves for alloy 800H.

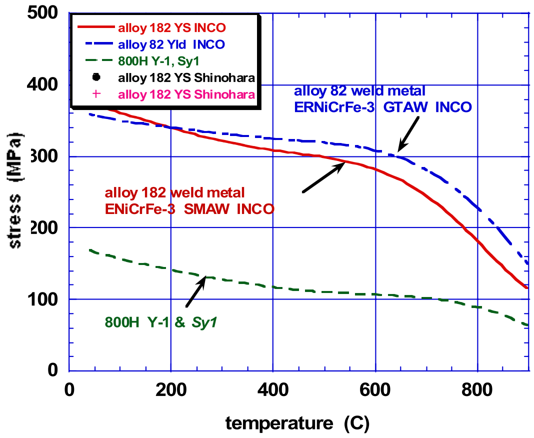

Figure 20 - Comparison of the Yield Strengths of SMA and GTA Weld Metals

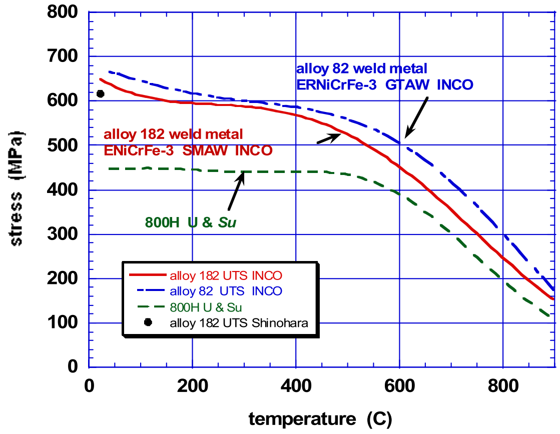

Figure 21 - Comparison of the Tensile Strengths of SMA and GTA Weld Metals

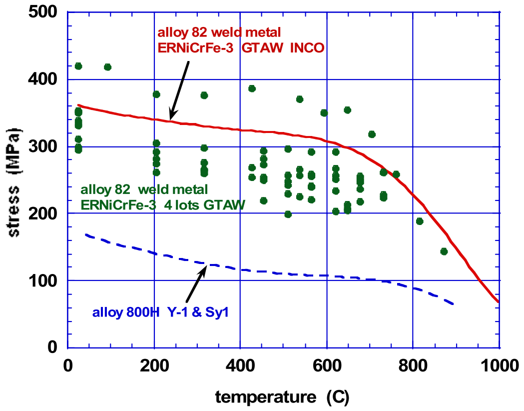

Figure 22 - Comparison of the Yield Strength for Alloy 82 Weld Metal with Alloy 800H

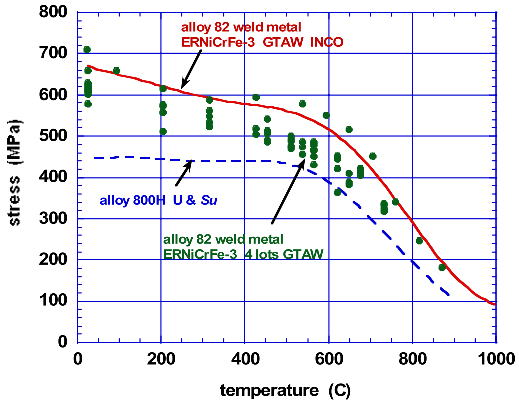

Figure 23 - Comparison of the Tensile Strength for Alloy 82 Weld Metal with Alloy 800H

Weldment data are shown in Figure 24 and Figure 25. Filler metals include alloy A, alloy 182, alloy 112, alloy 117 and alloy 82. Typically, the higher yield strengths of the filler metals boost the yield strength of the weldments over that of the base metal (alloy 800H). The weldments, however, have lower yield and ultimate tensile strengths than the weld metals. Failures occur in the alloy 800H base metal somewhat removed from the fusion line for some filler metals but near the fusion line for other filler metals.

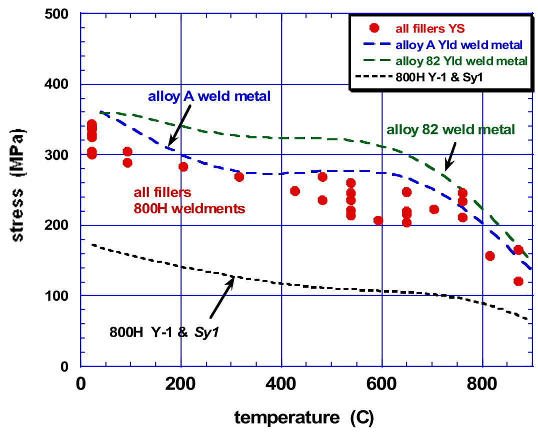

Figure 24 - Comparison of Weldment Yield Strength with Alloy 800H Base Metal

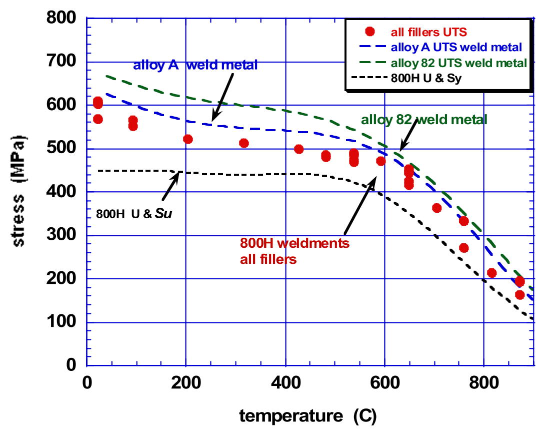

Figure 25 - Comparison of Weldment Tensile Strength with Alloy 800H Base Metal

With respect to extending ASME Section III-NH to 900˚C (1650˚F) for alloy 800H, additional tensile testing of filler metals is needed to more clearly define tensile data in the temperature range from 750 to 900˚C (1382 to 1650˚F).

4.2 Assembly of the Stress-Rupture Database

In an earlier section of this report, the sources for stress-rupture data on filler metals for joining alloy 800H were reviewed. The bulk of the data in these sources was developed from programs focused on components intended for operation below 750˚C (1382˚F). These data were used to develop the Stress Rupture Factors (SRFs) in ASME Section III-NH Tables I-14-10 C-1 and C-2. However, it was the intent of this report to collect and evaluate the data needed to extend coverage in the tables to longer times and 900˚C (1650˚F). It was not intended that the current SRFs be changed, hence data below 750˚C (1382˚C) were assembled but only data for 732˚C (1350˚F) and higher were included in the analyses. Data tables are summarized in Appendix 1. The tabulated data were extracted from tables in reports, when possible, but some data were extracted from plots in papers and reports. These data lacked the precision and accuracy that was desired, but taking in account the overall lot-to-lot variability, these data were considered to be better than no data at all. Since ASME III-NH only provides SRFs which are based on stress-rupture behavior, data bearing on other aspects of the time-dependent behavior of filler metals, such as time to 1% creep and the time to the initiation of tertiary creep, were not collected. Data for several types of filler metals were included. These filler metals are listed in Table 2 and Table 3 of this report. Alloy 132 (ENiCrFe-1) was an exception, and data for this filler metal were not included in Appendix 1.

4.3 Procedure for Determining the Stress Reduction Factors

The SRFs provided in ASME III-NH have been defined as the ratios of the strength of the weldment to the strength of the base metal for the specific temperature and time at which the ratio was determined. It is assumed that the ratios were based on the average strengths of the weldment and base metal, not the minimum strengths. In actual practice, the SRFs for the austenitic stainless steels such as types 304H and 316H were based on the ratios of the strength of the deposited filler to the strength of base metal. These strengths were obtained from the testing of coupons extracted from the deposited weld metals and base metals, but data from cross-weld test coupons and “full-thickness” weldment tests were used to validate the SRFs or make adjustments to the values. Little or no testing was performed on full-thickness weldments of alloy 800H, hence the analytical procedures for determining the SRFs involved the analysis of data from samples extracted from deposited filler metal and taking the ratios with respect to the average strength of the 800H base metal reported earlier [3].

The procedures used to determine the average and minimum rupture strength values for the ASME III-NH have not been standardized. In some instances, reports and open literature publications provide information on this topic, but, for the effort reported here, a procedure similar to that adopted by ASME Section II was followed. This was based on the use of the Larson-Miller temperature-time parametric correlation method that assumed a stress-dependent activation energy. Thus,

$$\left(1/t_R\right) = A\exp\left[-f_1(S)/RT\right] \tag{9}$$

Where t_R is the rupture life, A is a constant, f1(S) is a function of stress, R is the universal gas constant and T is absolute temperature. Taking the log to base ten and rearranging produces the familiar Larson Miller parameter (LMP):

$$LMP = T\left(C + \log t_R\right) = f_1(S)/2.303R \tag{10}$$

Where C is log A and identified as the Larson-Miller parametric constant.

Typically, a stress function f(S) is formulated as a polynomial in log stress:

$$f(S) = f_1(S)/2.303R = a_0 + a_1 \log S + a_2\left(\log S\right)^2 + a_3\left(\log S\right)^3 + \ldots \tag{11}$$

where a_i is a series of constants that depend on the number of terms in the polynomial. Using a least squares fitting method in which log t_R is the dependent variable and T and log S are independent variables, the optimum values for C and a_i are determined. Although not explicitly required by Appendix 1 of ASME Section II-D, the consultants may employ a "lot-centered" procedure developed by Sjodahl that calculates a lot constant (C_h) for each lot along with the Larson-Miller constant, C, which represents the average lot constant (C_{ave}) for the lots [29]. However, only C_{ave} is used to determine the S_{Rave}. To determine S_{Rave}, eq. (10) needs to be solved for S at 100,000 hours. Although the lot constants, variants within a lot, variants between lots and SEE of the log t_R can be produced in the analytical procedure, it is important to recognize that the ASME II-D does not explicitly provide such information. Both the global and lot-centered fitting procedures were used for alloy A and alloy 82. Only the global procedure was used for other candidates.

Qualitative Evaluation of the Strength of Weld Metal and Weldments Relative to 800H:

Figure 26 through Figure 36 compare stress-rupture data for weld metal and weldments with the trend for alloy 800H on the basis of the Larson Miller parameter. Here, the alloy 800H parametric curve is given by the parametric constant, C, 15.12487 and the following coefficients for the stress function, f(S), of equation (11):

a_0 = 29,648.78

a_1 = -7334.877

a_2 = 1903.854

a_3 = -619.4775

The comparisons for alloy A (ENiCrFe-2) are shown in Figure 26 for weld metal and Figure 27 for weldments. As may be seen, the data are few but define a trend for weld metal and weldments. For low values of the Larson Miller parameter (LMP), welds and weldments appear to be stronger than base metal and SRF should be 1.0. At 750˚C (1382˚F), the pointers in the figures indicate that the SRF at 100,000 hr. should be less than 1.0. In ASME III-NH, Table I-14 C-1 provides a value of 0.66 for 100,000 hr. at 750˚C (1382˚F), which appears to be close to an estimate based on the data plotted in Figure 27. At high values of the LMP, the SRFs could be as low as 0.5. There are no data for the LMP value near 600,000 hr. at 900˚C (1650˚F).

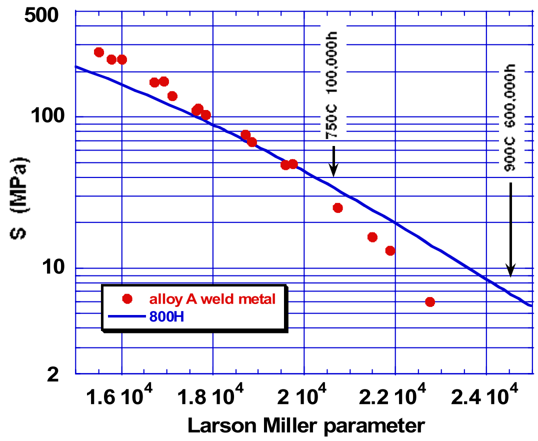

Figure 26 - Comparison of Alloy A Weld Strength with Alloy 800H Base Metal

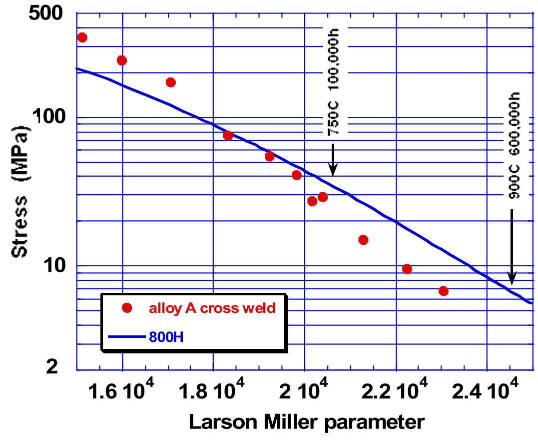

Figure 27 - Comparison of Alloy A Weldment Strength with Alloy 800H Base Metal

Comparisons for alloy 182 (ENiCrFe-3) deposited metal and weldments with alloy 800H are shown in Figure 27. Quite low strengths were observed over the entire range of test conditions. The 21/33Nb filler metal, however, appeared to be stronger than alloy 800H at low temperatures and maintained good strength at high temperatures. As shown in Figure 28, good strength persisted to a LMP value of at least 23,000. This parametric value would correspond to 300,000 hr. at 850˚C (1652˚F) and suggests that further assessment of this filler metal would be beneficial.

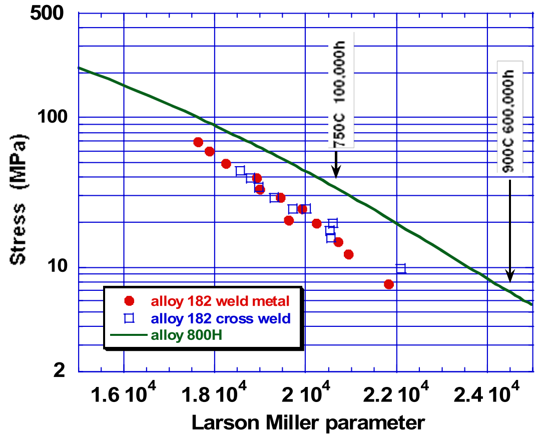

Figure 27 - Comparison of Alloy 182 Weld and Weldment Strength with Alloy 800H Base Metal

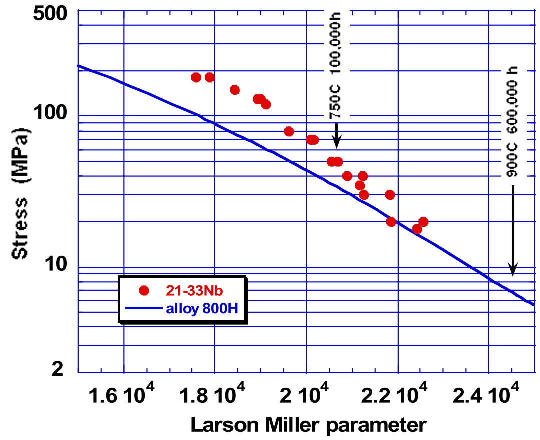

Figure 28 - Comparison of Alloy 21/33Nb Weld Strength with Alloy 800H Base Metal

Most of the evaluation of filler metals and weldments for alloy 800H focused on the bare wire material-alloy 82 (ERNiCr-Fe-3). A comparison of the strength of this deposited material with alloy 800H is shown in Figure 35 while weldment strengths are compared in Figure 36. Clearly, the data base is larger for this filler metal but the dearth of data at large values of the LMP is also evident. As with the other filler metals, the strength was greater than alloy 800H at low temperatures and LMP values. The alloy 82 strength crossed the LMP parametric curve for alloy 800H around the LMP value of 20,000.

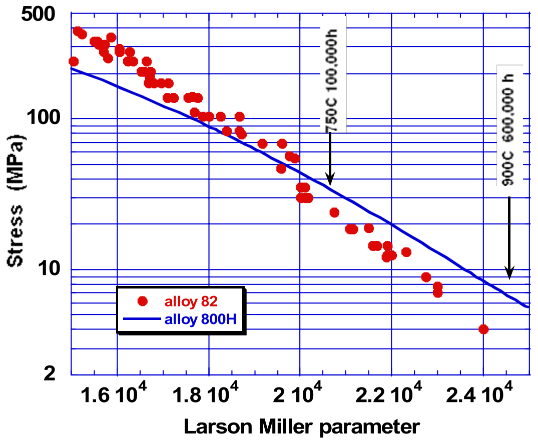

Figure 29 - Comparison of Alloy 82 Weld Strength with Alloy 800H Base Metal

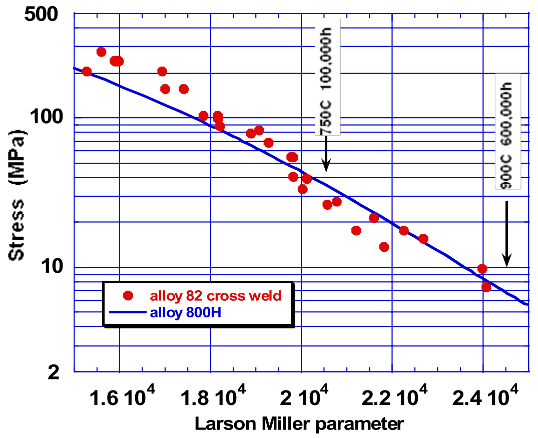

Figure 30 - Comparison of Alloy 82 Weldment Strength with Alloy 800H Base Metal

4.4 Calculation of Stress Reduction Factors

It is clear in Figure 26 to Figure 36 that the stress function f(S) for the weld metal and weldments differed from that for the alloy 800H base metal. An "optimized" calculation of the LMP was needed to estimate the weld metal and weldment strengths. Equations (10) and (11) above were selected and a third-order polynomial was used in the f(S) formulation. Only two of the filler metals were evaluated in this respect: alloy A (ENiCrFe-2) and alloy 82 (ERNiCrFe-3). Data for temperatures of 732˚C (1350˚F) and higher were selected. Alloy 82 was evaluated as two groups: all-weld metal and weld metal plus weldment. For each group two analyses were performed: Global and Lot-Centered. The SRFs at 100,000 h were calculated for each of the group and the value at 750˚C (1382˚F) was compared to the SRC tabulated in ASME III-NH. Table 4 lists the results of these calculations. Details of the parametric fits are provided in Appendix 2. Figure 37 provides a visual display of the results. Here, it may be seen that the Global parametric analyses produced lower SRFs at 100,000 h than the Lot-Centered analyses. The combined weld and cross-weld group produced the lowest SRFs at 750 and 800˚C (1382 and 1472˚F). The lowest value at 750˚C (1382˚F) was 0.72 which was greater than the tabulated value of 0.66 in ASME III-NH for alloy 82 to alloy 800H weldments.

Table 8 - Calculated 105 H Rupture Strengths and SRFs for Alloy 82 Welds and Weldments

Temp	Base Metal	Global Analysis		Lot-Centered Analysis	
(˚C)	S_R (MPa)	S_R (MPa)	SRF	S_R (MPa)	SRF
750	34.9	25.1	0.72	29.4	0.84
800	23.3	14.1	0.61	17.7	0.76
850	15.3	8.45	0.55	10.5	0.69
900	9.97	5.5	0.55	6.1	0.61

Alloy A presented a problem. First, very few data were available at 732˚C (1382˚F) and above. Secondly, the optimized parametric function produced a stress function, f(S), that could not be extrapolated to long times at the higher temperatures. Whereas the alloy 82 LMP constant C was fairly close to that for alloy 800H, the constant for alloy A was almost 19. The LMP analysis produced a significantly higher strength when the stress curve was extrapolated to 100,000 hr. at 750˚C (1382˚F). The resulting SRFs were greater that expected as illustrated in Figure 38. Some of the rupture data for weld metal and weldments are compared to curves based on the parametric fits in Figure 39 and Figure 34.

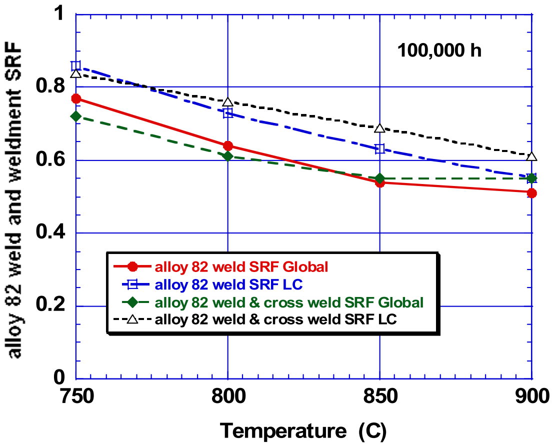

Figure 31 - Calculated Stress Rupture Factors for Alloy 82 for 100,000 hr.

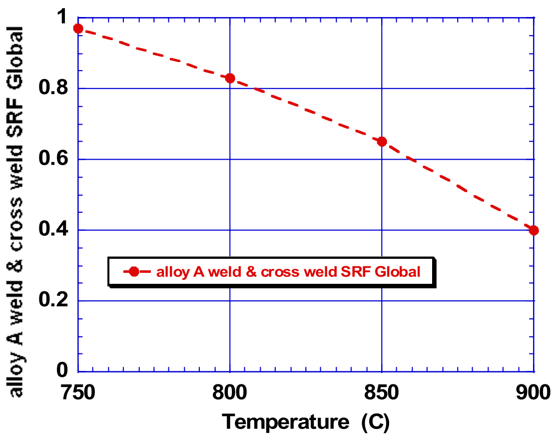

Figure 32 - Calculated Stress Rupture Factors for Alloy A for 100,000 hr.

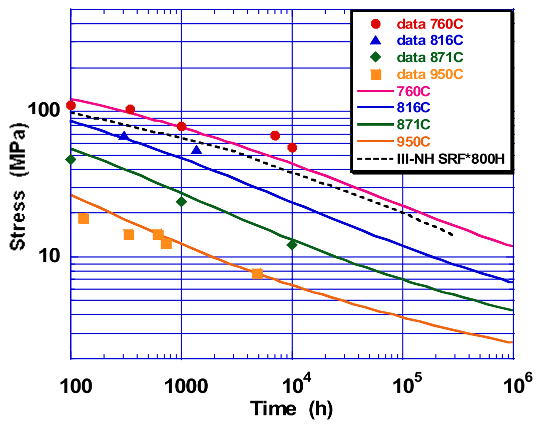

Figure 33 - Comparison of Rupture Data for Alloy 82 Weldments with Calculated Curves Based on the LMP
(Included is a Curve for 760C based on the SRFs Currently Listed in ASME III-NH)

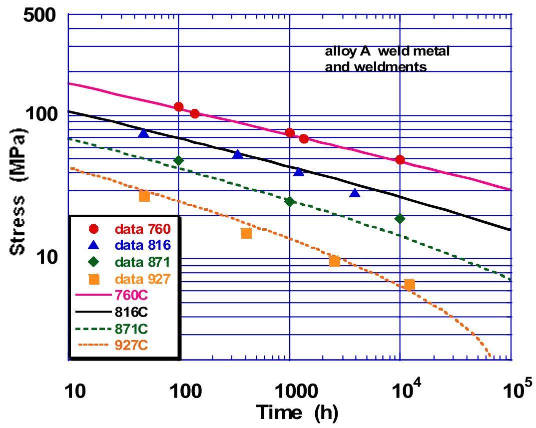

Figure 34 - Comparison of Rupture Data for Alloy A Weldments with Calculated Curves Based on the LMP

The calculated curves in Figure 39 and Figure 34 exhibit either upward or downward curvature at long times and low stresses and these trends reflect the characteristics of the third order polynomial, f(S) used to optimize the parametric constants. The curves should not be considered to be representative of long-time, low-stress behavior. The "cut-off" for estimating the SRFs is a matter of judgment but it is reasonable not to permit estimates for stresses lower than the lowest stress at which data were available or for times that exceed the longest rupture datum by an order of magnitude. For stresses, this position requires that values less than 6 MPa cannot be used to estimate the SRFs, while stresses for rupture lives in excess of 100,000 hours cannot be used to estimate SRFs. Examples of the calculated SRFs are tabulated in Appendix 3.

5 DISCUSSION

This report focused on the two filler metals currently approved for ASME III-NH, namely alloy A (ENiCrFe-2) and alloy 82 (ERNiCr-3). The database and experience with these two fillers is quite extensive at lower temperatures and there is no need to change the SRF values that are provided in ASME III-NH. It is interesting that efforts are underway to incorporate "weld strength reduction factors" (WSRFs) in ASME Section I, B31.1 and B31.3 for long-seam welded piping. Alloy 800H is included, and values without the identification of a specific filler metal are expected to be provided to 815˚C (1500˚F). It is anticipated that the WSRFs will be lower than the SRFs in ASME III-NH for 100,000 hr. but could be similar to those in ASME III-NH for longer time service. It is clear that the ASME III-NH approved filler metals produce low SRFs at temperatures above 750˚C (1382˚F), but it may be necessary to validate these values should the work on WSRFs be expanded to obverlap the intent of the SRFs in ASME III-NH. The alloy 800H strength is quite low at the high temperatures, and further reduction of allowable stress intensities in ASME III-NH to accommodate the SRFs could make the use of alloy 800H impractical. Alternate base metal materials should be considered for long-time service at the higher temperatures. A better matched filler metal, such as 21/33Nb, or an overmatched filler metal, such as alloy 117 (617), could mitigate the problem and their usage should be examined. Recommendations for testing filler metals and weldments are provided in Appendix 4. Appendix 5 of this report suggests that one can expect issues to arise for undermatched and overmatched filler metals.

Although not part of this effort, the issue that needs to be addressed is how one uses the SRFs when the S_{mt} and S_t values in ASME III-NH at temperatures above 750˚C (1382˚F) are not controlled by the rupture strength. Minimum stress-to-rupture data are provided in ASME III-NH but it has not been established that the SRFs for weldments are the same for minimum strengths as for average strengths.

6 SUMMARY AND RECOMMENDATIONS

Filler metals for joining alloy 800H were reviewed and references bearing on the tensile and stress-rupture behavior of deposited weld metal and weldments were summarized. Data were collected for several coated and bare-wire electrodes.

Yield data for several weld and weldment materials were compared to the Y-1 and S_{y1} versus temperature trends for alloy 800H. Similarly, ultimate tensile strength data were compared to the U and S_U versus temperature trend for alloy 800H. Weld metal and weldments always exceeded the strength of the alloy 800H base metal.

The stress-rupture strengths of several weld and weldment materials were compared to the rupture strength of alloy 800H for the temperature range 750 to 1000˚C (1382 to 1832˚F) on the basis of the Larson Miller parametric curve using a common parametric constant characteristic of alloy 800H. Weld metals and weldments were stronger than alloy 800H at low temperatures and high stresses but appeared to be weaker at high temperatures. Alloy 21/33Nb was an exception and the deposited filler metal was stronger or equivalent to alloy 800H over the range of temperatures and stresses where data were available.

An attempt was made to estimate the Stress Rupture Factors (SRFs) for weldments made with alloy A (ENiCrFe-2) and alloy 82 (ERNiCrFe-3). The lack of long-time, high-temperature data made it difficult to produce reliable results. Analysis was undertaken using the Larson Miller parametric procedure. Both global (batch) and lot-centered methods were applied. For alloy 82, estimates of SRFs were reasonably close to those provided in ASME III-NH Table I-10 C-2 for 760˚C (1400˚F). Values for alloy A were higher than expected and well above the SRFs provided in ASME III-NH Table I-10 C-1.

If a need for SRFs in the temperature range 750 to 900˚C (1382 to 1650˚F) was established, further testing of weld deposits and weldments was recommended. Testing of deposits from 21/33Nb coated electrodes and alloy 82 (ERNiCFe-3) bare wire electrodes was recommended. Testing to at least 10,000 hr. at temperatures of 900˚C (1650˚F) was recommended.

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APPENDIX 1 - COMPILATION OF DATA ON WELD METALS AND WELDMENTS

Table 9 - Stress-Rupture Data for Alloy A Deposited Weld Metal

Lot ID	Temp (˚C)	Stress (MPa)	Life (h)
INCO	760	114	100
INCO	760	76	1000
INCO	760	49	10000
INCO	871	48	100
INCO	871	25	1000
INCO	871	19	10000
INCO	982	16	100
INCO	982	6	1000
HT7728HEM	482	482	47
HT7728HEM	538	414	436
HT7728HEM	649	241	177
HT7728HEM	649	172	1675
HT7728HEM	649	103	16900
HT7728HEM	760	138	27
HT7728HEM	760	103	139
HT7728HEM	760	69	1330

Table 10 - Stress-Rupture Data for Alloy A Deposited Cross Welds

Lot ID	Temp (˚C)	Stress (MPa)	Life (h)	Failure
HT7728HEM	482	551		Weld
HT7728HEM	482	482		Weld
HT7728HEM	482	414	11550	Weld
HT7728HEM	538	414	315	Weld
HT7728HEM	538	345	3266	Weld
HT7728HEM	649	241	163	Weld
HT7728HEM	649	172	2318	Weld
BMI	816	75.8	48	
BMI	816	54.5	340	
BMI	816	40.7	1200	
BMI	816	29.0	3900	
BMI	927	27.6	48	
BMI	927	15.2	400	
BMI	927	9.7	2500	
BMI	927	6.8	12000	

Table 11 - Stress-Rupture Data for 21-33Nb Weld Metal

Lot ID	Temp (˚C)	Stress (MPa)	Life (h)
33431	750	180	220.7
33431	750	130	2807.7
33431	750	80	11333.0
33431	850	70	661.9
33431	850	50	1961.9
33431	850	40	6058.8
19424	950	30	536.0
19424	950	20	2078.7
19424	750	180	117.5
19424	750	150	761.1
19424	750	130	2398.4
19424	750	120	3516.3
19424	850	70	597.4
19424	850	50	1472.4
19424	850	40	2956.3
19424	850	35	5357.5
19424	950	30	183.3
19424	950	20	546.1
19424	950	18	1597.1

Table 12 - Stress- Rupture Data for Alloy 182 Deposited Weld Metal

Lot ID	Temp (˚C)	Stress (MPa)	Life (h)
Shino	816	68.6	11.5
Shino	816	59.8	19.5
Shino	816	49.0	43
Shino	816	39.2	180
Shino	816	33.3	205
Shino	816	20.6	800
Shino	927	29.4	12
Shino	927	24.5	30
Shino	927	19.6	56
Shino	927	14.7	140
Shino	927	12.3	215
Shino	927	7.6	1150

Table 13 - Stress-Rupture Data for Alloy 82 Deposited Weld Metal

Lot ID	Temp (˚C)	Stress (MPa)	Life (h)
INCO	538	400.0	100.0
INCO	538	359.0	1000.0
INCO	538	324.0	10000
INCO	649	252.0	100
INCO	649	190.0	1000
INCO	649	141.0	10000
INCO	760	110.0	100
INCO	760	79.0	1000
INCO	760	57.0	10000
INCO	871	47.0	100
INCO	871	24.0	1000
INCO	871	12.0	10000
INCO	982	19	100.0
INCO	982	9	1000.0
INCO	982	4	10000.0
TM5404	454	517.1	3.2
TM5404	454	510.2	142.3
TM5404	454	496.4	715.1
TM5404	454	496.4	1012.6
TM5404	454	489.6	1075.4
TM5404	510	482.7	10.9
TM5404	510	455.1	39.4
TM5404	510	448.2	357.1
TM5404	510	434.4	1205.1
TM5404	510	413.7	1645.4
TM5404	510	393.0	3255
TM5404	510	379.2	6770.4
TM5404	566	434.4	29.5
TM5404	566	413.7	112.8
TM5404	566	396.5	448.2
TM5404	566	379.2	841.1
TM5404	566	365.4	1087.5
TM5404	566	344.8	6003.3
TM5404	621	379.2	21.2
TM5404	621	310.3	295.1
TM5404	621	293.0	653.1
TM5404	621	275.8	1195.9
TM5404	621	241.3	3109.4
TM5404	677	275.8	26
TM5404	677	241.3	89
TM5404	677	206.9	215
TM5404	677	172.4	778.5
TM5404	677	137.9	3590
TM5404	732	172.4	30.7
TM5404	732	137.9	103.6
TM5404	732	103.4	634.4
TM5404	732	82.7	2792.8
TM5491	454	496.4	1671.2
TM5491	454	482.7	4228.8
TM5491	454	455.1	8222.4
TM5491	510	448.2	106.1
TM5491	510	434.4	260
TM5491	510	413.7	1049.7
TM5491	510	396.5	6637.7
TM5491	510	241.3	12746
TM5491	566	379.2	129.8
TM5491	566	365.4	247.1
TM5491	566	344.8	432.3
TM5491	566	327.5	2776.1
TM5491	621	310.3	204.7
TM5491	621	275.8	652.9
TM5491	621	241.3	1401.2
TM5491	677	206.9	183
TM5491	677	172.4	546.7
TM5491	677	172.4	366.8
TM5491	677	137.9	2263.1
TM5491	732	82.7	1526.6
TM5491	732	103.4	459.1
TM5491	732	137.9	77.2
HEM7399	538	344.8	
HEM7399	538	448.2	178
HEM7399	593	206.9	
HEM7399	593	275.8	
HEM7399	649	137.9	
HEM7399	649	206.9	1069.6
HEM7399	704	103.4	9767
HEM7399	704	137.9	
HEM7399	760	69.0	6940
HEM7399	760	103.4	347
HEM7399	816	55.2	1364
HEM7399	816	69.0	301
Schubert	850	35.0	500
Schubert	850	30.0	500
Schubert	850	30.0	600
Schubert	850	35	600
Schubert	850	30	680
Schubert	950	18.5	130
Schubert	950	18.5	145
Schubert	950	14.5	330
Schubert	950	14.5	390
Schubert	950	14.5	600
Schubert	950	12.5	600
Schubert	950	12.5	720
Schubert	950	13	1300
Schubert	950	7.8	4800
Schubert	950	7	4800

Table 14 - Stress-Rupture Data for Alloy 82 Cross Welds

Lot ID	Temp (°C)	Stress (MPa)	Life (h)
tm12438	811	275.8	
tm12438	811	344.8	576
tm12438	811	344.8	1332
tm12438	866	275.8	760
tm12438	922	137.9	
tm12438	977	103.4	1399
tm12438	977	103.4	
tm12438	1033	69.0	3450
tm12438	1033	103.4	288
tm12438	1089	55.2	1159
tm12438	1089	55.2	1082
tm9108	922	206.9	1695
tm9108	922	206.9	27.6
tm9108	922	241.3	141
tm9108	922	241.3	126
tm9108	922	241.3	139
tm9108	922	241.3	163
tm9108	922	241.3	139
tm9108ann	922	241.3	157
tm9108ann	922	241.3	126
tm8728	755	413.7	15373
tm8728	755	482.7	1964
tm8728	755	413.7	9578
epri 82-15	1173	40.2	58
epri 82-15	1173	33.3	90
epri 82-15	1173	26.5	260
epri 82-15	1173	17.7	900
epri 82-15	1173	13.7	3000
epri 82-13	973	156.9	220
epri 82-13	973	156.9	580
epri 82-13	973	98.1	3500
epri 82-13	973	78.5	19000
epri 82-13	1073	88.3	68
epri 82-13	1073	83.4	440
epri 82-13	1073	39.2	4200
epri 82-13	1173	27.5	380
epri 82-13	1173	21.6	1900
epri 82-13	1173	17.7	7000
epri 82-13	1273	15.7	490
epri 82-13	1273	9.8	5200
epri 82-13	1273	7.4	6000

Table 15 - Stress-Rupture Data for Alloy 182 Cross Weld

Lot ID	Temp (°C)	Stress (MPa)	Life (h)
Shino	816	44.1	82.0
Shino	816	39.2	135.0
Shino	816	34.3	200
Shino	816	29.4	400
Shino	816	24.5	1750
Shino	927	24.5	20
Shino	927	19.6	110
Shino	927	17.7	99
Shino	927	15.7	100
Shino	927	9.8	1920

APPENDIX 2 - COEFFICIENTS FOR THE LARSON MILLER FIT TO STRESS-RUPTURE DATA

Item	Type Analysis	C	a_0	a_1	a_2	a_3	SEE
Alloy 82 Weld	Global	14.49396	28782.890	-12051.550	6372.657	-1583.916	
Alloy 82 Weld	Lot-Centered	15.64275	27907.730	-6003.623	1787.850	-566.021	
Alloy 82 Cross & Weld	Global	12.87579	27049.820	-11949.620	6149.193	-1486.36	
Alloy 82 Cross & Weld	Lot-Centered	14.27747	26069.800	-4898.113	906.714	-351.803	
Alloy A Weld & Weld	Global	18.87048	28410.550	991.541	-3051.934	454.203	0.170
Alloy A Weld & Weld	Lot-Centered	19.02555	28342.266	1619.864	-3494.767	544.756	
Alloy 82 Cross Weld	Global	18.79754	33930.656	10931.240	4728.953	-1156.372	0.049
Alloy 82 Cross Weld	Lot-Centered	18.58448	33781.492	11203.340	4974.860	-122.845	
Alloy 800H Base Metal	Global	15.12487	29648.780	-7334.877	1903.854	-619.478	0.290

$$\log t_R = \frac{f(S)}{T_K} - C$$

$$f(S) = a_0 + a_1 \log S + a_2 (\log S)^2 + a_3 (\log S)^3$$

t_R in hours, T_K in Kelvin, S in MPa

APPENDIX 3 - EXAMPLES OF CALCULATED STRESS FACTORS FOR ALLOY 82 WELDMENTS

Item	Temp (˚C)	10 h	100 h	1,000 h	10,000 h	100,000 h	600,000 h
Base Metal	750	142	104	74	51.4	34.9	24.4
Weldment	750	188	131	83.8	48.3	25.1	15
SRF (Alloy 82)	750	1	1	1	0.94	0.72	0.61
Base Metal	800	111	18.2	53.8	35.9	23.3	16.5
Weldment	800	148	95.4	55.1	28.5	14.1	5.75
SRF (Alloy 82)	800	1	1	1	0.79	0.61	0.53
Base Metal	850	85.7	58	38.1	24.5	15.3	10.6
Weldment	850	112	66	34	16.3	8.45	
SRF (Alloy 82)	850	1	1	0.89	0.66	0.55	
Base Metal	900	65	42.2	26.3	16.4	9.97	6.75
Weldment	900	81.5	43.3	20.1	9.8		
SRF (Alloy 82)	900	1	1	0.76	0.6		
ASME III-NH	750	1	1	0.94	0.82	0.67	

APPENDIX 4 - RECOMMENDED CREEP-RUPTURE EXPERIMENTAL PROGRAM TO ADDRESS STRESS RUPTURE FACTORS FOR WELDMENTS IN ALLOY 800H FOR SERVICE ABOVE 750˚C

To develop reliable stress-rupture factors for use above 750˚C in ASME III construction of Class 1 components, a substantial experimental testing program will be necessary. The program should include the following elements:

- Selection of base metal for weldments
- Selection of filler metals and welding processes
- Specifications for testing coupons and testing methods
- Design of weldment specimens and testing methods
- Selection of testing temperatures and times
- Selection of analysis methods.

It is recommended that the base material be taken from archival material Jessup Steel Heat No. 37459 currently in storage at the Oak Ridge National Laboratory. See ORNL/TM-12436 [1]. This material was purchased for use on the Modular High-Temperature Gas-Cooled Reactor Program and meets the necessary specifications required by ASME III-NH. When welded, the 12.7-mm (1/2-in) thick plates will be adequate for weld metal coupons, cross-weld coupons and "full-thickness" weldments with transverse and longitudinal weld orientations.

Three filler metals for shielded metal arc (SMA) welding should be included: alloy A (ENiCrFe-2), alloy 117 (ENiCrCoMo-1) and 21/33Nb [2]. Two fillers for gas tungsten arc (GTA), gas metal arc (GMA), or submerged arc (SA) welding should be included: alloy 82 (ERNiCr-3) and alloy 617 (ERNiCrCoMo-1). The introduction of the bare wire 21/33Nb wire should be optional and based on the experience with the material in the petrochemical and refining industries.

Testing coupons including base metal, weld metal and cross welds should be round bars manufactured from the weld plates with a minimum test section diameter of 6.3 mm (1/4 in.) for short-time tests and 9.5 mm (3/8 in.) for long-time tests. Testing methods shall conform to ASTM E 139.

Full thickness weldment specimens should be of two types: weld transverse to the loading axis and weld parallel to the loading axis. Typically, the length-to-width of the weldments should permit the relaxation of discontinuity stresses and produce a region of unaffected base metal.

Previous research on weldments in alloy 800H was limited to temperatures below 750˚C. The program recommended here should cover the temperature range of 750 to 1000˚C.

Alloy 82 Testing (ERNiCr-3):

The testing plan for alloy 82 deposited weld metal or cross weld specimens should be designed to supplement existing data. Two data sets that may be considered are those published by McCoy [1] and Schubert, et. al. [3]. An example minimum test matrix is recommended in Table 16. No testing below 900˚C is included under the assumption that the existing database is adequate to establish SRFs at lower temperatures and the test data recommended will be used to estimate SRFs for long times by means of time-temperature parametric prediction methods.

Table 16 - Test Matrix for Alloy 82 Weldment Evaluation

Temp (˚C)	Stress (MPa)	Time (h) weld/base	Cross Weld	Weld Metal
900	12	4000/50,000	X	X
900	8	20000/300,000	X	
925	12	2000/10,000	X	
925	8	10000/100,000	X	
950	8	4000/50,000	X	X
950	5	15000/300,000	X	
975	8	2000/2000	X	
975	5	7000/12,000	X	
1000	5	3000/5000	X	X
1000	3	12000/500,000	X	

Of these, the low-stress, high-temperature tests are the most significant. However, McCoy observed that failures occurred in the base metal for all testing conditions to 816˚C [1]. If so at the higher temperatures, then the testing times will prove to be far too long to be practical and the test stresses will need to be adjusted upwards. Such a trend is in conflict with the observations that the SRFs are less than 1.0 at high temperature and long times.

Alloy A (ENiCrFe-2):

The test matrix for alloy A may be the same as for alloy 82 (Table 12).

Alloy 117 (ENiCrCoMo-1) and Alloy 617 (ERNiCrCoMo-1):

The testing plan for alloy 117 and 617 specimens should be directed toward the understanding of the effect of the mismatch in strength on the high-temperature performance of weldments. The creep-behavior of the high-alloy weld metals (alloys 117 and 617) needs to be estimated from test data (a few cross-weld tests would be of benefit to establish the failures will occur in the base metal removed from the fusion line when restraint is minimal). It should be recognized that the performance of alloy 117 and alloy 617 at 750˚C and above will be investigated as part of the DOE project work on Gen IV materials at the national laboratories [4], so only a minimal test matrix is needed. The temperatures, stress values, and estimated times in the table below are based on short-time test data produced on alloy 117 weld metal by INCO (Special Metals Inc.).

Table 17 - Test Matrix for Alloy 117 or Alloy 617 Weld Metal Evaluation

Temp (˚C)	Stress (MPa)	Time (h) weld/base	Weld Metal
900	60	1000	X
900	30	10000	X
950	30	1000	X
950	18	10000	X
1000	11	1000	X
1000	7	10000	X

Alloy 21/33Nb:

The matching weld metal, alloy 21/33Nb, is used extensively for high-temperatures service. Test data are scarce, though some has been reported by Metrode [2] and Schubert, et. al. [3]. Again, if this material is to be evaluated for service above 750˚C, some creep data would be helpful in the analysis of tests on weldments. Alloy 800H stresses and temperatures provide a basis for developing a test matrix, and a minimal testing program on deposited filler metal is suggested below in Table 18.

Table 18 - Test Matrix for Alloy 21/33Nb Weld Metal Evaluation

Temp (˚C)	Stress (MPa)	Time (h) weld	Weld Metal
900	30	1000	X
900	16	10000	X
950	20	1000	X
950	12	10000	X
1000	12	1000	X
1000	7	10000	X

Weldment Testing:

The “full-thickness” weldment tests should be performed on plate-type specimens with a nominal cross-section of 100 mm (4 in.) in width and 12.5 mm (1/2 in.) in thickness. The “reduced section” length should be at least 300 mm (12 in.) for transverse welds and 300 mm (12 in.) in length for longitudinal welds. These dimensions assume that the weld crown width is 25 mm (1 in.). A narrower weld would permit a smaller specimen cross section and reduced section length.

Two weldment tests should be performed on each orientation and each filler metal. A recommended test matrix is shown below in Table 19. Two temperatures are recommended: 800˚C and 900˚C. It is assumed that sufficient data exist at 800˚C to undertake analysis of the weldment test [1]. If this is not the case, additional testing at 800˚C may be required. Three filler metals are recommended: alloy 82, alloy 117 and 21/33Nb. Weldments of alloy 82 should be weaker than alloy 800H at both temperatures. Weldments of alloy 117 should be stronger than alloy 800H at both temperatures. Weldments of 21/33Nb may be stronger at 800˚C and equivalent at 900˚C.

Table 19 - Test Matrix for Alloy 800H Weldments

Filler Metal	Weld Orientation	Temp (˚C)	Stress (Mpa)	Time (h) weld
alloy 82	transverse	800	50	10000
alloy 82	transverse	900	15	10000
alloy 82	longitudinal	800	50	10000
alloy 82	longitudinal	900	15	10000
alloy 117	transverse	800	50	10000
alloy 117	transverse	900	15	10000
alloy 117	longitudinal	800	50	10000
alloy 117	longitudinal	900	15	10000
alloy 21/33Nb	transverse	800	50	10000
alloy 21/33Nb	transverse	900	15	10000
alloy 21/33Nb	longitudinal	800	50	10000
alloy 21/33Nb	longitudinal	900	15	10000

Analysis methods:

The analysis methods for evaluating the creep and stress-rupture response of weldments at high-temperature are well-developed and were used extensively in the determination of stress-rupture factors for the materials incorporated in ASME III-NH [4] to [8]. Appendix 5 provides an analysis of value for the round-bar samples recommended in Table 16 above. Also, the use of a special notched bar sample is suggested.

References Appendix 4:

[1] H. E. McCoy, Tensile and Creep Tests on a Single Heat of Alloy 800H, ORNL/TM-12436, Oak Ridge National Laboratory, Oak Ridge, TN, Sept. 1993.

[2] Thermet 800Nb, Data Sheet 09-06A, Technical Handbook, Metrode Products Ltd., Surrey, KT, UK (not dated).

[3] F. Schubert, U. Bruch, R. Cook, H. Diehl, P. J. Ennis, W. Jakobeit, H. J. Penkalla, E. te Heesen and G. Ullrich, "Creep Rupture Behavior of Candidate Materials for Nuclear Process Heat Applications," Nuclear Technology, Vol. 66, No. 1, pp. 227-239, July 1984.

[4] W. Ren and R. W. Swindeman, "Preliminary Consideration of Alloy 617 and 230 for Generation IV Nuclear Reactor Applications," paper PVP 2007-26091 presented at the 2007 Pressure Vessel and Piping Conference, July 23-28, San Antonio, TX, 2007.

[5] J. M. Corum, "Evaluation of Weldment Creep and Fatigue Strength-Reduction Factors for Elevated Temperature Design," Structural Design for Elevated Temperature Environments-Creep, Ratchet, Fatigue, and Fracture, ASME PVP Vol. 163, American Society of Mechanical Engineers, New York, NY, pp. 9-17, 1989.

[6] M. J. Manjoine, Creep-Rupture Tests of Type 304 Stainless Steel Weldments with Central Axial Welds of Type 308 Stainless Steel at 593˚C, WARD-HT-94000-3, Westinghouse Advanced Reactors Division, Pittsburgh, Sept. 1979.

[7] W. J. McAfee, R. L. Battiste and R. W. Swindeman, Elevated Temperature (593˚C) Tests and Analysis of Type 304/308-CRE Stainless Steel Plate Weldments, ORNL/TM-9064, Oak Ridge National Laboratory, Oak Ridge, TN, May 1984.

[8] J. M. Corum and W. K. Sartory, "Assessment of Current High-Temperature Design Methodology Based on Structural Failure Tests," Journal of Pressure Vessel Technology, Vol. 109, pp. 160-168, May 1987.

APPENDIX 5 - PARAMETRIC STUDY OF WELDMENT BEHAVIOR

This study has been carried out as a preparatory step toward predicting weldment creep life from the basic properties of the parent and weld metal. The objective was to explore the effects of different parent and weld metal creep properties on weldment rupture life,

Test data indicates that, regardless of which of the parent or weld metal is the weaker, weldment strength is invariably less than the weaker of the two components. In the most common situation, when weld metal is stronger than the parent metal, the weldment is still weaker even than the parent plate.

There are two possible contributory causes for this finding. First, it may be that the welding process generates an interface layer which is weaker than either of the two metals being joined. The second is that the complex stress state developed by inhomogeneous properties causes premature failure in the weaker component.

If the problem lies in the formation of complex low strength layers in the fusion zone, then it will be necessary to develop some equally complex test methods to evaluate local strength variations.

The development of complex stress states is easier to evaluate, since this is largely a question of stress analysis. With a view to examining this possible factor, if necessary for the purposes of eliminating it if that be the case, some typical weld geometries have been analyzed under creep conditions. These geometries are illustrated in Figure 35, and include a round bar, often used for weldment testing programs, tubes in axial tension or pressure and a plane strain configuration. The basic weld geometry for the bar is shown in Figure 36 and Figure 37 which includes a blow-up of the weld/parent interface. This geometry represents a V-prep weld in a 6.3-mm (0.25-in.) thick specimen. Allowance has been made in the model to account for variations of fusion line properties, but no such variations have been considered as yet. This study has been limited to a single variation, which is a difference in creep properties between the parent metal and the weld.

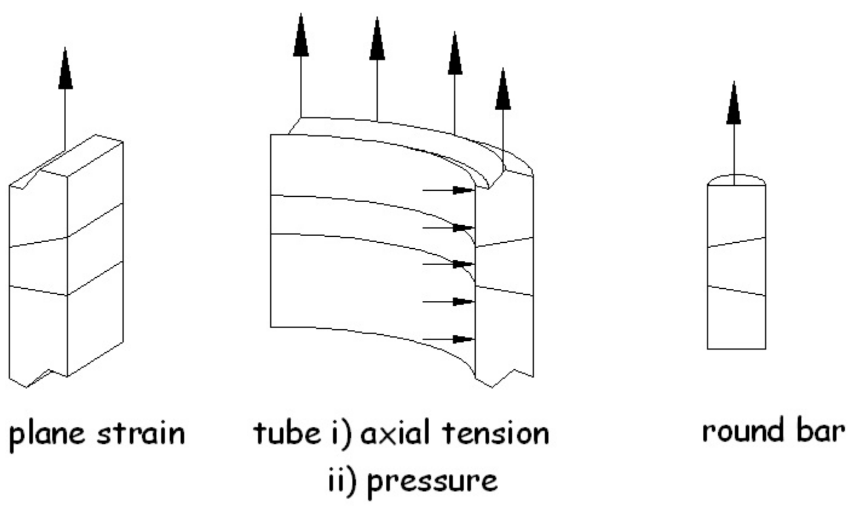

Figure 35 - Example Geometries of Weldments with 20˚ Interface Angle

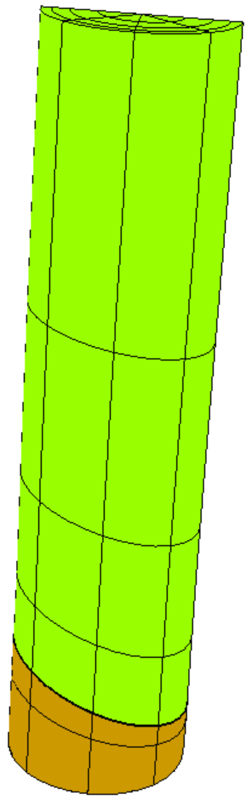

Figure 36 - General View of Weld FE Model

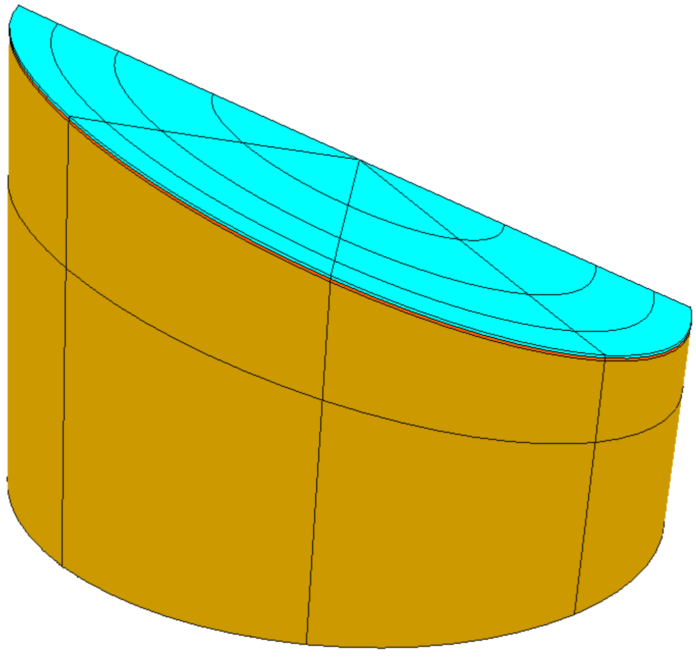

Figure 37 - Detail of Weld Interface

It is difficult to find equivalent data on parent plate and weld material for the purpose of generating the types of material model required in finite element analysis and, since this is intended as a trend analysis only, use has been made of the fact that creep strength in weld components appears to be proportional to indentation hardness and this, in turn, suggests a typical strength ratio of approximately 1.5 between the plate and the weld. Furthermore, this ratio can apply in both directions, with the parent plate being either 1.5 times weaker, or 1.5 times stronger, than the weld.

This study has therefore used a single material, alloy 800H, at a temperature of 850˚C (1562 ˚F) where its nominal design allowable would be approximately 10 MPa (1.45 ksi), based on the minimum of 1% in 100,000 hours or 2/3 of the 100,000 hour rupture strength.

Creep properties for the above condition were extracted from the MPC Omega model published in API 579, Part 10, using a simplified Bailey/Norton power law with a best fit exponent "n," calculated at the nominal stress of 10 MPa (1.45 ksi). Three material models were used in the study, a nominal model, one with an equivalent strength of 1.5 times the nominal and a third with 2/3 of the nominal strength. Given that "n" for this material is approximately 7.35, the ratios of creep rates in the strongest/nominal and nominal/weak at the same stress level are both approximately 18:1.

The weldment configurations shown in Figure 35 have been run under four different boundary conditions. These are,

[1] Plane strain

[2] Axisymmetric circ. weld in 50-mm (2-in.) diameter tubing under axial load

[3] As 2. above but under internal pressure with closed ends

[4] Round cross weld specimen

In all cases loading was adjusted to produce the same equivalent (Mises) stress of 10 MPa (1.45 ksi).

Failure in a weldment is complicated due to the stress state. It has been assumed that failure is defined by an effective S_{eff} which is a function of the stress state. A version of Huddleston's multiaxial rupture criterion, as employed in API 579, was used to calculate Seff in this study, i.e.,

$$S_{eff} = S_{mises}\exp\left[0.24\left(\frac{J_1}{S_s}-1\right)\right] \tag{12}$$

$$J_1 = \left(S_{11}+S_{22}+S_{33}\right) = 3S_h$$

$$S_s = \left(S_{11}{}^2+S_{22}{}^2+S_{33}{}^2\right)^{0.5}$$

This criterion only governs the onset of creep rupture failure. In practice, in an inhomogeneous stress field, damage starts at the highest stresses location, and propagates until the material loses load carrying capacity. This can only be evaluated accurately with a continuum damage model such as Kachanov, Dyson or Omega. To avoid the complications of user subroutines introduced by a more detailed analysis, it has been assumed that the onset of creep rupture damage is equivalent to initiation of a creep crack. A simplified C* analysis then established that crack growth following creep rupture damage occurring at one location would be rapid, and that onset of creep damage is therefore a reasonable approximation to specimen life–in this application. It is recognized that this may not be a generally correct assumption, but is reasonable in this instance because there are no severe stress concentrations or gradients involved.

Failure in this study is therefore defined as the time to rupture, as predicted by simple tensile creep rupture versus time curves, using the effective stress calculated as a function of the multiaxial stress state using Equation 12 above.

A typical result is shown in Figure 39 and Figure 34, for the round-bar cross-weld specimen. Note that, although this model appears relatively crude, it consists of high order 20-node brick elements and the region of high stress needs to be sufficiently extensive to produce significant creep damage. Therefore a geometrically crude model is adequate in this case.

Figure 33 shows the distribution of the Mises stress on the interface. On the other hand, the hydrostatic stress, S_h, (Figure 39) which has a value of only 3.3 MPa (0.48 ksi) remote from the weld, increases to 10 MPa (1.45 ksi) locally, and is greater than 7 MPa (1 ksi) over a large proportion of the weld interface. According to the Huddleston multiaxial criterion, this would result in an S_{eff} of about 11.6 MPa (1.68 ksi), or an increase of 15% over the nominal uniaxial value. Translated into weld SRFs, this predicts a value of SRF = 0.87.

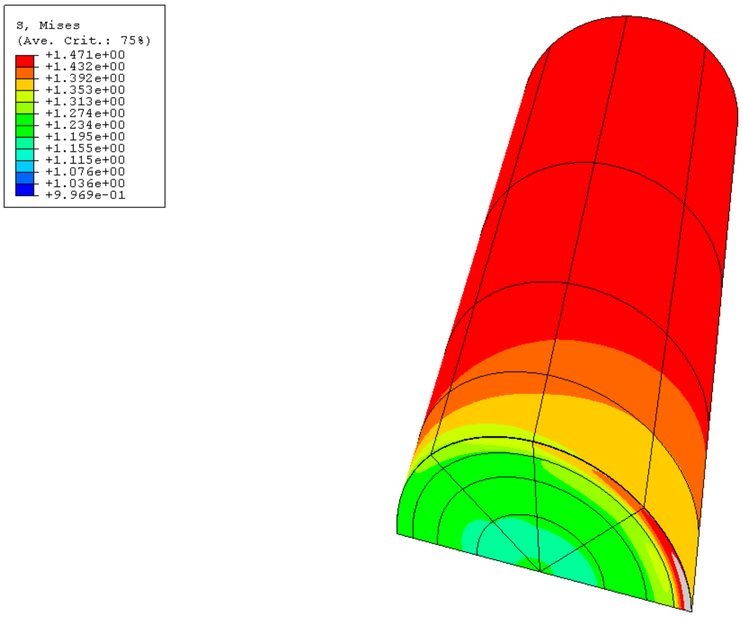

Figure 38 - Mises Stress Distribution on Weld Interface Under Full Developed Creep Conditions

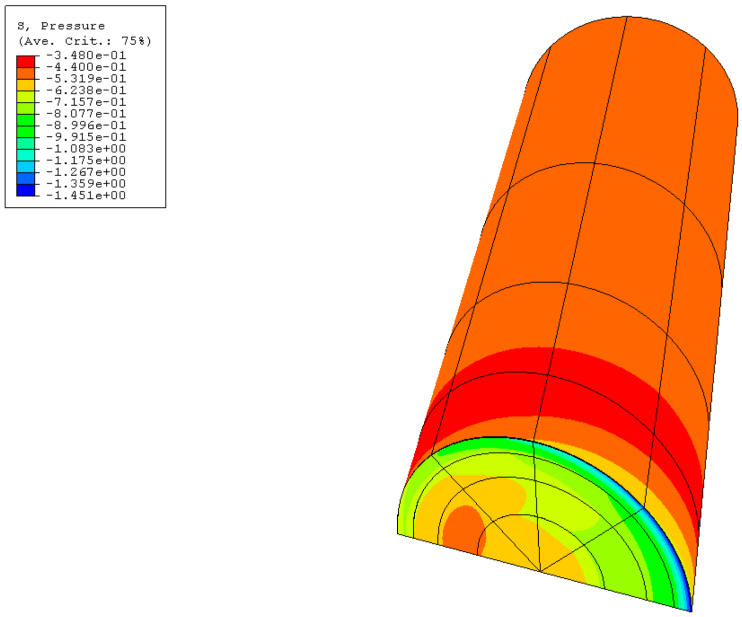

Figure 39 - Hydrostatic Stress Distribution on Weld Interface Under Full Developed Creep Conditions

Additional analyses were performed on the geometries shown in Figure 35, but with different assumptions regarding the material behavior. Here, the exponent of the Bailey/Norton power law was reduced to 5 and the relative creep rates of the weld metal to based metal was assumed to 0.1 (stronger weld) and 10 (weaker weld). Again, the Mises stress was taken to be a nominal 10 MPa (~1.45 ksi). The results, which include the SRFs for 10 conditions, are shown in Table 20.

Table 20 - Effect of Weldment Geometry on the Calculated Strength Reduction Factor

	Material relative strength		Nominal Mises Stress (A) (ksi)	Huddleston Effective Stress (B) (ksi)	Strength Reduction Factor SRF (A/B)
	Parent	Weld metal			
Tensile Bar	nominal	strong x10	1.447	1.45	1.000
	strong x10	nominal	"	2.39	0.605
Tube-in-tension	nominal	strong x10	"	2.08	0.695
	strong x10	nominal	"	2.45	0.589
Tube-under-pressure	nominal	strong x10	"	1.99	0.727
	strong x10	nominal	"	2.02	0.717
Plane Strain tension	nominal	strong x10	"	2.13	0.679
	strong x10	nominal	"	2.33	0.621
Round bar tension	nominal	strong x10	"	2.48	0.582

Conclusions:

- A creep strength disparity between parent metal and weld metal reduces creep rupture strength by producing a metallurgical SCF at the interface together with elevated hydrostatic stress.
- This effect alone is sufficient to develop significant weld SRFs for a typical difference in creep strength of the two constituents.

- The SRF depends on the weld geometry but is generally on the order of 0.6 to 0.7, regardless of which constituent is the weaker.
- Additional reduction in weldment strength may result from weak or brittle zones forming along the weld interface. This problem has not been fully investigated yet for lack of reliable material data on interface material.

Recommendation:

There is a need for a test on weldments to identify the effects of multiaxiality and, if possible, the specific properties on the weld/parent metal interface.

A candidate specimen that could serve both purposes is the so-called “yoyo” specimen, a deeply notched, but blunt root notch specimen which generates a high level of hydrostatic stress over a large proportion of the neck area. This is a good geometry to test both parent and weld metals separately and, by placing the notch root carefully at the weld/parent metal interface, distinctive behavior of the interface could be deduced by comparison with similar tests on the homogeneous materials.

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ABBREVIATIONS AND ACRONYMS

ASME	American Society of Mechanical Engineers
ASME ST-LLC	ASME Standards Technology, LLC
BVP	ASME Boiler and Pressure Vessel Code
DOE	U.S. Department of Energy
Gen IV	Generation IV Reactor Materials Project
FBR	Fast Breeder Reactor
FEA	Finite Element Analysis
GA	General Atomic Co.
HAI	Huntington Alloys Inc.
HRB	Hochtemperatur-Reaktorbau GmbH
HTGR	High Temperature Gas-Cooled Reactor
LMP	Larson Miller Parameter
MCM	Minimum Commitment Method
MHTGR-NPR	Modular High Temperature Gas-Cooled Reactor – New Production Reactor
NIMS	National Institute for Material Science
NKK	Nippon-Kokan
ORNL	Oak Ridge National Laboratory
SEE	Standard Error of Estimate
SMAC	Shielded Metal Arc Welding
SRF	Stress Rupture Factors
VHTGR	Very-High Temperature Gas-Cooled Reactor
W-T	Westinghouse-Tampa
WSRF	Weld Strength Reduction Factors

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