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ELEVATED TEMPERATURE TESTING PROBLEM AREAS

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Foreword

The Symposium on Problem Areas in Elevated Temperature Testing was presented at the Seventy-third Annual Meeting of the American Society for Testing and Materials held in Toronto, Ontario, Canada, 21–26 June 1970. The Joint Committee on Effect of Temperature on the Properties of Metals (ASTM-American Society of Mechanical Engineers-Metal Properties Council) sponsored the two-session meeting on 22 June 1970. H. R. Voorhees of the Materials Technology Corp. served as symposium chairman; he was assisted by D. K. Faurschou, Canada Department of Energy, Mines and Resources, and G. V. Smith, Cornell University, who acted as session chairmen.

Related ASTM Publications

Fatigue at High Temperature, STP 459 (1969), $11.25

Advanced Testing Techniques, STP 476 (1970), $5.75

An Evaluation of the Yield, Tensile, Creep, and Rupture Strengths of Wrought 304, 316, 321, and 347 Stainless Steels at Elevated Temperature, DS 5-S2 (1969), $6.00

Supplemental Report on Elevated Temperature Properties of Chromium-Molybdenum Steels, DS 6-S2 (1971), $7.00

An Evaluation of the Elevated Temperature Tensile and Creep Rupture Properties of Wrought Carbon Steel, DS 11-S1 (1969), $6.00

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Introduction

The need for this symposium became evident during recent efferts to update ASTM Recommended Practices for Short-Time Elevated Temperature Tension Tests of Materials and for Conducting Creep and Time-for-Rupture Tension Tests of Materials (E 21 - 66T and E 139 - 66T, respectively). The Subcommittee on Test Methods of the ASTM-ASME-MPC Joint Committee on Effect of Temperature on the Properties of Metals found, in particular, that available information on alignment and pyrometry was insufficient to permit definition of exact effects on test results. This lack of factual information has necessitated some indefinite provisions in E 21 and E 139, while other requirements represent a compromise between opinions as to what is desired and what is attained readily in usual practice.

Although both are incomplete, an extensive cooperative creep testing program by AGARD (a NATO committee; see first paper by Coutsouradis and Faurschou) and an interlaboratory evaluation of pyrometric practices being conducted by the joint committee have developed preliminary results (see paper by J. L. Korns). Several other smaller studies also are under way to relate to questions being raised about elevated temperature testing procedures.

The symposium was organized with the hope of uncovering those data necessary to an evaluation of the effectiveness of existing standards and to make available some useful data that will supplement current standards while suitable revisions are undergoing the lengthy process of development and approval. I trust this publication will call attention to several problems that may be encountered in elevated temperature testing and will offer some guidance on the expected magnitude of their effects and possible ways to circumvent them.

H. R. Voorhees

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on the Properties of Metals.

D. Coutsouradis[1] *and D. K. Faurschou*[2]

Preliminary Report on the AGARD Evaluation of Variables Affecting High Temperature Creep Results

REFERENCE: Coutsouradis, D. and Faurschou, D. K., "**Preliminary Report on the AGARD Evaluation of Variables Affecting High Temperature Creep Results,**" *Elevated Temperature Testing Problem Areas, ASTM STP 488*, American Society for Testing and Materials, 1971, pp. 3–14.

ABSTRACT: The Advisory Group for Aerospace Research and Development (AGARD), a NATO committee, engaged in an interlaboratory study of high temperature creep testing facilities and techniques. The program utilized factorial design and analysis. Nimonic 105 was tested at 900 C by 18 volunteer laboratories. Preliminary results have permitted statistical evaluation of interlaboratory variability and of the significance of some testing and material variables which affect creep results.

KEY WORDS: creep tests, high temperature tests, creep properties, creep rupture strength, mechanical properties, nickel alloys, statistical analysis, normal density functions, analysis of variance

The Advisory Group for Aerospace Research and Development (AGARD) is a NATO committee which, acting through the Working Group on High Temperature Testing of the Structures and Materials Panel, is conducting a modest interlaboratory evaluation of the variability of creep results and some of the factors which contribute to this variability. Although this interlaboratory program has not been completed, the responsible AGARD authorities have granted permission for this exposition of the nature of the program and of the results of a preliminary evaluation. The keen interest expressed in this program by the ASTM Joint Committee on Effect of Temperature on the Properties of Metals is appreciated. Their interest is not surprising since the points of reference

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of the AGARD working group are aligned closely with major objectives of ASTM. These points of reference are

1. To improve the specification for and competence in the determination of mechanical properties of high temperature materials in the NATO nations.

2. To prepare a "best draft" specification through consultation with NATO centers for selected tests and then to distribute standard supplies to testing laboratories for application of these standards. Through comparison of results and discussion of experience, revised specifications and technical reports will be issued.

Origin of Program

The variability of creep and creep rupture times is excessive when compared directly to the variability of some other mechanical properties such as tensile strength. In fact, the basic logarithmic nature of this variability apparently is still not as generally realized as it should be. Consequently, it is often difficult to compare creep results from different laboratories, to evaluate the relative performance of different materials, and to specify minimally acceptable creep properties economically. The problem becomes progressively more serious as operating temperatures increase.

In the past two decades many interlaboratory programs have been concerned with creep properties up to temperatures of about 700 C. Some of these programs have been planned on a massive scale; however, few have been designed and analyzed statistically. Fewer still have produced significant results proportional to the effort involved. Accordingly, the Working Group on High Temperature Testing of the Structures and Materials Panel of AGARD decided that implementation of a controlled interlaboratory program involving testing conditions currently encountered in superalloy technology would be desirable. They stipulated that the program should attempt to be exploratory, rather than exhaustive, and should be a preliminary study that could be expanded if necessary. They further suggested that the testing be limited to about the equivalent of two 100-h and two 1000-h tests at 900 or 950 C per laboratory. This scale of effort was expected to attract response from enough laboratories to achieve a useful collective response. The number of responses was good except, perhaps, from the United States. The program attracted three Belgian, four French, three German, one Italian, one Dutch, five English, and two American laboratories.

Objectives

Within the prescribed limitations an interlaboratory program was planned with stated primary, secondary, and ultimate objectives. The primary objectives are to compare the performances of laboratories and to assess interlaboratory variability quantitatively. Such information is essential for specification and design purposes, acceptance testing, and development of new materials. The testing program is designed specifically to attain these primary objectives. The secondary objectives are allowance for the creation of a "reserve" supply of calibrated testing blanks, which will be useful for extension of the program, and the identification, semi-quantitative if not quantitative, of sources which contribute to interlaboratory, intralaboratory, material, and residual or uncontrolled random variability.

It also might be said that a major objective of the program is to apply available elementary statistical techniques to a creep rupture program in order to derive quantitative information for the more general application of statistical techniques in industry. However the objectives are stated, the program seeks to contribute to the attainment of the ultimate objectives of improved laboratory techniques, improved laboratory performance, and improved specifications.

Basis of Design

A full factorial experimental design was selected because it is simple, uses all of the data for maximum "hidden" replication, and is completely flexible. The flexibility has permitted the presentation of this preliminary evaluation and, perhaps more importantly, permits laboratories to enter or leave at any time, without jeopardizing the program. This latter feature also means that, in the future, coupons from the calibrated reserve may be used to evaluate modifications to test procedures at any of the participating laboratories. The results of these future tests may be compared to all previous results reported to AGARD, because in full factorial designs every test result is used in the calculation of the effect of each variable. More efficient but less flexible designs generally require a good prior estimate of the variances involved in the program. Reliable variance estimates were insufficient to risk using a more specialized experimental design.

The application of a factorial design to stress rupture data obtained over a range of stress levels is made possible by analyzing the log transform of the stress rupture time ($\log t_r$) and by selecting stress levels spaced at unit intervals of log stress. The log t transformation makes the variances homogeneous over a range of stress levels. Unit intervals of stress make the design orthogonal and thereby reduce experimental error and simplify the analysis.

Conventional statistical design and analysis is based on normal, that is, gaussian, distributions. A criterion for normality of data is that a cumulative distribution plot of the data be linear. Figure 1 is such a cumulative distribution plot of 131 stress rupture times (the total number available). The relationship is acceptably linear except for two abnormal results at the lower extremity. The data in Fig. 1 were the results of stress rupture

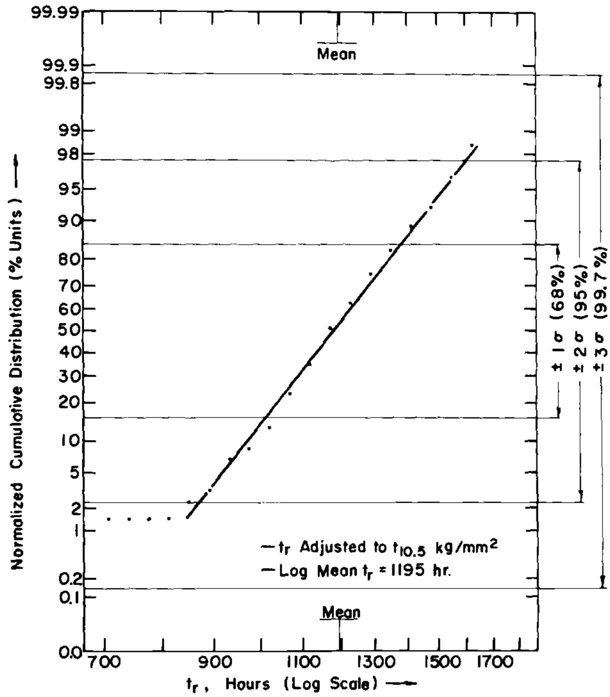

FIG. 1—*Cumulative distribution curve showing that the log stress rupture times for the 131 available tests have an acceptable gaussian distribution, except for two tests at the lower extremity. The stress rupture results represent the five fixed experimental stress levels. However, the results were, where necessary, adjusted to the σ_5 level of stress.*

tests, taken over five stress levels, which have been transformed by a regression equation to a common stress.

In the program the only fixed, independent (controlled) variables were log stress (log σ), laboratories (18), material (Nimonic 105), and temperature (900 C). This means that the results of the program cannot be generalized to other stress levels, to nonparticipating laboratories, to other materials, or to other temperatures unless the necessary information is available.

The test material was marked systematically and cut into test bar coupons. These carefully identified coupons were assigned at random for each replicate for each laboratory. All other variables also were randomized where possible. The experimental error of the program could have been reduced by controls, such as machining all specimens at one facility, providing precalibrated thermocouples from a common supply, and monitoring the precision and accuracy of instrumentation for measuring and controlling the temperature. However, these refinements were avoided deliberately, because the program was designed to study realistic data which represented the routine performance of the participating laboratories.

Testing Program

The laboratories were offered two designs designated model A (mandatory) and model B (preferred). Model B incorporates model A but is more extensive.

The mathematical equation which describes model A is

$$X_{ijkm} = \mu + A_i + B_j + AB_{ij} + C_k + AC_{ik} + BC_{jk} + \epsilon_{m(ijk)}$$

where

X_{ijkm} = experimental result or response of the *ijkm*th test,
μ = true mean of all test results,
A_i = laboratory effect (fixed, qualitative variable) ($i = 1$ to 18),
B_j = stress level effect (fixed, quantitative, independent variable) ($j = 1, 2$ for two stress levels, σ_2 and σ_5),
C_k = replication effect ($k = 1, 2$ for replicates) (the difference between two replicates of $ij = 36$ tests each),
AB_{ij} = interaction effect of laboratories and stress levels (not expected to be significant),
AC_{ik} = interaction effect of laboratories and replicates (not expected to be significant),
BC_{jk} = interaction effect of stress level and replicates (not expected to be significant),
m = number of tests per unit cell ($m = 1$), and
$\epsilon_{m(ijk)}$ = random residual effect, which includes effects due to intralaboratory variables and material inhomogeneity.

The experimental pattern and analysis of model B are analogous to those of model A, except that there are five stress levels (σ_1 to σ_5) of 20.3, 17.2, 14.6, 12.4, and 10.5 kgf/mm^2, respectively. The loads were spaced at increments of 18 percent to set the stress at suitable intervals on a log scale, with nominal levels of stress rupture life at 35, 100, 240, 500, and 1000 h.

The total testing time for model A, based on two 100-h and two 1000-h tests was estimated to be 2200 h. The total testing time for model B, based on two tests at each of the five stress levels was estimated to be 3750 h. Actual stress rupture times averaged about 20 percent longer.

Recommended Procedures

The participating laboratories were free to use any testing procedures or equipment. However, recommendations were offered based on the International Organization for Standardization documents ISO/R204–1961 and ISO/R206–1961. Basically, these recommend (a) turned specimens with a minimum diameter of 4 mm (0.157 in.) and a gage length five times as large as the initial diameter; (b) machining in graded passes and finishing by surface grinding; (c) shock-free loading with a load accuracy better than ±1 percent; (d) strain measurement to an accuracy of not less than ±0.1 percent of the gage length; (e) loading into a hot furnace, heating to 893 C in 1 to 2 h, and a soaking time of 4 to 5 h to reach the prescribed temperature and temperature gradient; and (f) strictures on temperature.

The importance of achieving good temperature control was emphasized, although procedures for calibration and checking thermocouples and instrumentation were not detailed. Adherence to the ISO recommendations requires that temperature be measured with a sensitivity of ±0.5 C, that variation of the indicated temperature at any particular point on the gage length not exceed ±2 C, and that the indicated temperature variation along the gage length not exceed 3 C during the test. In principle, the accuracy of the temperature measurement should be ±0.5 C, achieved by careful calibration of thermocouples and instrumentation along with scrupulous technique.

Minutely detailed reports were requested from each laboratory so that significant performance differences and similarities might be associated with individual laboratory techniques or equipment. Hopefully, some of these associations would be unassailable while others would suggest the direction of specific supplemental testing necessary to clarify apparent or suspected associations.

Material Composition and Processing History

The test material is the nickel alloy, Nimonic 105, donated by Henry Wiggin & Company Limited, Hereford, England. The heat analysis (weight percent) yielded 20.0 Co, 14.45 Cr, 4.85 Mo, 4.60 Al, 1.21 Ti, 0.10 Zr, 0.0045 B, 0.15 C, 0.15 Fe, <0.15 Si, 0.03 Mn, 0.01 Cu, 0.0011 Pb, and <0.001 Ag.

A 3000-lb heat of virgin raw material was induction melted and cast into many small ingots by the Durville process. Five of these ingots were extruded into rectangular bars which were cold rolled about 8 percent to the final dimensions of 1⅛ by ⅝ in. An identified length of 120 in. was cut from the leading end (ingot bottom) of each extrusion. These bars were heat treated together as follows: 4 h at 1150 C, air cooled; 16 h at 1050 C, air cooled; and 16 h at 850 C, air cooled.

Material Evaluation

Henry Wiggin & Company Limited evaluated the Nimonic 105 ultrasonically and removed blanks from each end of the five bars for hardness traverses, tension tests at 900 C, and nominal 100-h stress rupture tests at 900 and 950 C. The values of ultimate tensile and yield strength at

TABLE 1—*Inhomogeneity of nominal 100-h stress rupture life of Nimonic 105 test material (20 tests).*

Source of Variation	Standard Deviation, s	
	log	%
Bar to bar	0.047	11.4
End to end	0.035	8.3
Residual	0.035	8.3
Combined	0.068	16.9

NOTES

1. s is an estimate of the true standard deviation.
2. $s^2 = \dfrac{\Sigma(\bar{X} - X_i)^2}{n - 1}$ is an estimate of the true variance.
3. s^2(Combined) = s^2(Bars) + s^2(Ends) + s^2(Residual).

0.2 percent offset formed statistically homogeneous groups with low standard deviations.

The stress rupture tests showed statistically significant differences between bars at both test temperatures and between ends at 950 C but not at 900 C. This was of considerable interest, because the program does not permit a meaningful evaluation of the stress rupture inhomogeneity, which is inherent in the material, until the program has been virtually completed.

The root mean square residual log and percent standard deviations of the twenty stress rupture evaluation tests at Wiggin are given in Table 1. These were calculated by analysis of variance or by calculation of components of variance from the "fixed" expected mean squares used in the F test of significance. Components of variance usually are only of interest in random model experiments. In fixed model experiments such as this one, there are no real components of variance; however, if the F test is statistically significant, the treatment mean squares of the significant fixed factors are mathematically equivalent to variances and may be considered as such for the fixed conditions.

Stress Rupture Time in Hours versus Log Hours

The 20 Wiggin stress rupture results used to assess the homogeneity of the Nimonic 105 were analyzed by analysis of variance (ANOVA) of stress rupture time (t_r) in hours and in log hours to illustrate how the results are influenced by using the log transform. This is illustrated by referring to the results shown in Table 2.

The use of log hours, as expected, reduces the residual standard deviation, s. Also the log standard deviation may be converted directly to a percent standard deviation using standard log tables or a slide rule. This

TABLE 2—t_r *in hours versus log hours.*

Statistic	Dependent Variable, t_r	
	in hours	in log hours
Mean time...........................	92.2	antilog 1.9777 (90.5 h)
s.....................................	11.1 (12%)	antilog 0.0347 (8.3%)
95% confidence limits of the mean......	92.2 ± 7.5	90.5 ± 5.1 to 90.5 ± 4.8

conversion is possible because log scales are percent scales. Any unit interval on a log scale corresponds to a unit percent interval anywhere on the log scale.

The use of log stress rupture time rather than stress rupture time changes the confidence limits in three ways. Specifically, the use of the log transform lowers the mean, reduces the confidence interval, and makes the confidence interval asymmetrical in terms of hours.

Interlaboratory Results

Although the preliminary experimental results have been evaluated quantitatively under the five following subheadings, they only can be discussed in a summary fashion for this report. The final AGARD report will be available in the spring of 1971. ASTM has expressed an interest in reprinting the final report as a Special Technical Publication and AGARD has granted permission for this to be done.

When the model A and B testing has been completed, the results will be unrandomized to permit a systematic analysis of variance of material macroinhomogeneity. If, for example, significant bar-to-bar variability is detected, then the whole set of results will be adjusted to eliminate the effect of this material inhomogeneity. It is possible then that analysis of variance of this adjusted data will, for example, affect the Duncan ranking of the laboratory means. It may, in any event, significantly improve the sensitivity of the analysis of variance.

Standard Deviation of Dependent Variables

A major concern of the program was to derive quantitative measures of the variability of the dependent or measured stress rupture values. This has been done by calculating the log standard and percent standard deviation of rupture time and time to 2.0, 1.0, 0.5, and 0.2 percent deformation. Some of the variables—total deformation (time zero), elongation, and reduction of area—are distributed normally without a transformation. The statistic $100V$ (V is Pearson's coefficient of variation) expresses linear standard deviations as percent values; therefore, all of the standard deviations were compared as percent values.

The residual standard deviation of stress rupture time and time to 2.0 percent deformation was about 12 percent. For times to 1.0, 0.5, and 0.2 percent deformation the standard deviations increased progressively to excessive values. It is evident that the measurement of times of deformation up to and including 0.2 percent must be improved if they are to have any reliable meaning and to improve the measurement of times at greater deformations. This does not apply to all of the laboratories but it does apply to the ISO recommendations on strain measurement. Some of the laboratories used extensometers with an accuracy of 10^{-3} percent or better, and their times to the stated deformations were less variable.

Range of Laboratory Means

The mean responses of the laboratories to the dependent variables showed significant differences. They indicate that some of the laboratories should be concerned about their current performance. They also indicate why there is often a lack of confidence in accepting or comparing creep results from other laboratories.

Duncan Ranking, Model A, t_r

Having established, by an analysis of variance, that there is a significant difference between laboratories, the Duncan multiple range test was used to group the laboratories into statistically homogeneous groups. Other techniques are available, but they may produce different groupings. Experience with evaluating mechanical test data has shown that the grouping is best done by the Duncan test or by fitting confidence intervals to the means of each laboratory. For the preliminary analysis the Duncan test was used exclusively. The different multiple range and multiple F tests which may be used give different results because they assign different probabilities to the risks of committing type I and type II errors. A type I error is committed by saying that an effect exists when it does not; a type II error by saying that an effect does not exist when it does.

By using the Duncan test, the laboratories were ranked into overlapping groups. The laboratories which had the highest mean stress rupture times, ranking 1, 2, 3, and 4 in terms of mean stress rupture time, were the only ones to use Chromel-Alumel thermocouples. These base metal thermocouples were calibrated periodically by use of precious metal thermocouples, since it is known that Chromel-Alumel thermocouples may drift positively and thus give indicated temperatures which have a negative bias, resulting in abnormally long stress rupture lives. These results suggest that supplemental testing is necessary to clarify the influence of this association.

Other associations which possibly may contribute to high values are surface finish and use of subsize bars in vacuum. The laboratory which had the lowest mean stress rupture time used hollow test bars and reported a bias of +2 C. This temperature bias is equivalent to −9 percent deviation in t_r.

Duncan Ranking, Model B, t_r

The seven laboratories which had completed the model B program were grouped into two distinct and widely separated groups. Five statistical outlier results may have contributed to lowering of three of the means. It is significant that four of these outliers were traced to the leading end of bar No. 3. In addition, two high outliers were traced to the leading end of bar No. 5.

Log Variance Estimates of t_r

At this stage it was possible, with some conjecture, to estimate and sum interlaboratory variance, s^2(Interlab), heat-to-heat variance, s^2(Heats), and residual variance, s^2(Residual), so as to estimate the total variance, s^2(Total), for individual stress rupture tests. While the quantitative accuracy of this estimate may be gross, it is a useful exercise to help identify where significant improvement may be achieved most readily. At the very least, it indicates the uncertainty of the contribution from some of these sources of variance.

Classification of Sources of Variability

A consideration or even a listing of sources contributing to s^2(Interlab), s^2(Heats), s^2(Residual), s^2(Material), and s^2(Intralab) might be useful in understanding the nature of these variances, evaluating their influence, and exercising more effective control of them. Interlaboratory variability is associated largely with techniques and equipment which produce consistently biased results or excessive variability. Some of these sources may be

Thermocouple drift, as suspected in participating laboratories using Chromel-Alumel
Calibration of thermocouples
Accuracy of the whole system used in temperature measurement and control (For example, three of the AGARD laboratories reported a sensitivity of ±0.5 C for their thermocouples but ±2.0, 2.25, and 4.5 C for their whole system.)
Technique of attaching thermocouples
Location of thermocouples
Furnace design

Furnace atmosphere
Loading techniques
Axiality and alignment
Test bar design
Test bar preparation

Sources of heat-to-heat variability are more difficult to identify and to evaluate. Some of the possible contributory sources are

Differing production processes from company to company or within companies
Differing fabrication processes
Competence
Quality control procedures
Reputation
Market conditions
Experience
Type of alloy
Compositional and other variations from heat to heat
Macroinhomogeneity from bar to bar and within bars

Residual variability is controlled variability of a random nature. It may be considered traceable to intralaboratory factors and to random micro and macroinhomogeneity of the material being tested. In short, the residual variability is more or less common to all laboratories for a given material. The intralaboratory factors may, despite this common link, have some superimposed unique or characteristic qualities depending on the operating personnel, their experience, the type of equipment, and the condition of the laboratory.

Summation

Whether or not this program achieves its objectives to the satisfaction of AGARD remains to be seen. Certainly the program has been well received by creep laboratories within the NATO countries and the participants have offered admirable cooperation. It would be premature to conclude this preliminary evaluation with quantitative conclusions which may not be in agreement with the final report expected in the spring of 1971; however, the following general conclusions summarize the current progress and status of the program.

The interlaboratory variability of 18 voluntary laboratories has been or will be assessed quantitatively. Tentatively, significant interlaboratory differences have been detected. The results of this evaluation should guide some of these laboratories to significantly improved performance.

The variability of almost 140 stress rupture tests has been analyzed rationally and systematically to be available as a basis for the design of more efficient experiments. These could be planned to study some of the experimental variables which were identified as contributing or possibly contributing to the variability of creep rupture tests.

Some of these contributing factors are being evaluated in small supplemental programs at individual laboratories.

A small calibrated reserve is available for further testing.

Statistical design and analysis should be applied more extensively to creep programs to improve their effectiveness and lower their cost.

Acknowledgments

We express our appreciation to the Structures and Materials Panel of AGARD for the opportunity to be associated with this program and to present this report. We also acknowledge the competent guidance and effective support of the Working Group on High Temperature Testing under its chairman, F. Niordson.

This report does not refer specifically to the literature. We wish to recommend two publications—*Proceedings of the Joint International Conference on Creep*, sponsored in 1963 by ASME, ASTM, and IME and published by the IME, and *High-Temperature Properties of Steels*, being the Proceedings of a Joint BISRA and ISI Conference in 1966 and published as ISI Publication 97—for favorably influencing the conduct and interpretation of this program. For example, in ISI 97 the work of T. Prnka and Foldyna from Czechoslovakia and J. H. Gittus and C. E. Crook from the United Kingdom Atomic Energy Authority is based on the statistical analysis of log stress rupture time.

On behalf of AGARD and the participating laboratory, thanks are extended to Henry Wiggin & Company Limited for donating and pretesting the Nimonic 105 and for the advice and discussions with A. Duce.

Finally on our own behalf and for AGARD, we thank H. R. Voorhees and the ASTM Joint Committee on Effect of Temperature on Properties of Metals for their interest and invitation to participate in this symposium.

A. K. Schmieder[1]

Measuring the Apparatus Contribution to Bending in Tension Specimens

REFERENCE: Schmieder, A. K., "**Measuring the Apparatus Contribution to Bending in Tension Specimens,**" *Elevated Temperature Testing Problem Areas, ASTM STP 488*, American Society for Testing and Materials, 1971, pp. 15–42.

ABSTRACT: Methods are given for resolving measured bending strains into contributions from the following three sources: first, inaccuracies in the gages or their application; second, nonsymmetry of the specimen; and third, imperfections in the apparatus. The methods are applied to tests on tension specimens with three commonly used forms of grip ends. A method for correcting for inaccuracies in gage factors and gage misalignment is derived and applied. After correction by this method gage errors are insignificant. For the accurately machined, ½-in.-diameter specimen used, the specimen contribution to bending is small. The calculated apparatus contribution is most reproducible and significant when the load string has a minimum number of loose, threaded joints. The three specimens tested give bending strains well within the limit of ASTM recommended practices when the testing machine is in good condition.

A method also is explained for determining the maximum bending strain at any point in the reduced portion of the specimen. By this method it is shown that the maximum may be more than twice the value measured by using an extensometer or strain gages centered on the reduced portion.

KEY WORDS: bending, strains, tension tests, bend tests, elastic properties, loads (forces), measurement, strain gages, calibration

A task group organized to review ASTM Recommended Practices for Short-Time Elevated Temperature Tension Tests of Materials (E 21 - 66T) and for Conducting Creep and Time-for-Rupture Tension Tests of Materials (E 139 - 66T) agreed that some limitation should be retained on the amount of bending allowed during a tension test. However, the members decided that the limited data on the effect of such bending did not warrant the complexity of measuring the amount of bending during each

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test. Instead, it seemed reasonable to specify (1) a limit for the maximum amount of bending due to the apparatus and (2) a tolerance on the specimen dimensions which would limit the bending due to the specimen. The objective of this paper is to describe a method for evaluating the bending due to the apparatus and to apply the method to several typical specimens.

First, experiments to obtain data will be outlined. Then, the formulas will be presented. Finally, the experiments made to determine the applicability of the formulas will be described and discussed.

Recommended Testing Procedure

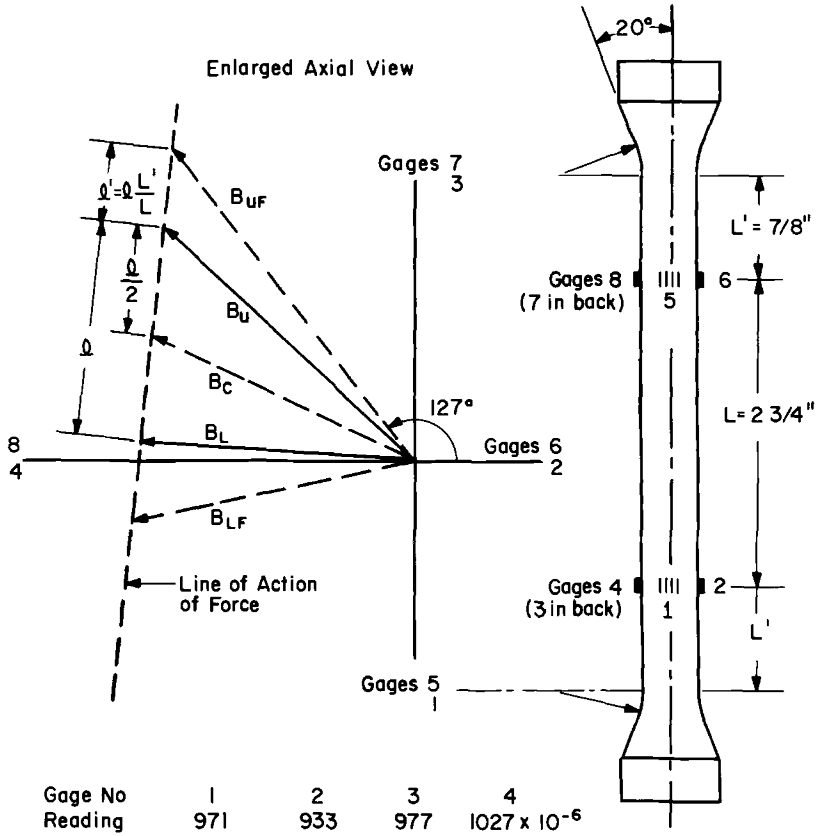
Following Jones and Brown [*1*][2] the use of a specimen of circular cross section with four gages equally spaced around the circumference is recommended. Foil or wire gages of the electrical resistance type are convenient. The gage should have a length equal to or shorter than the specimen diameter and should be oriented with the long grid elements parallel to the specimen axis.

The determination of maximum bending by the method described here requires a set of four gages at each of two longitudinal positions. For best accuracy these should be far apart but not close enough to the fillets to be influenced by their stress concentration. Placing gages one specimen diameter from the fillet tangent points will provide adequate separation. For arithmetical convenience the two gaged planes should each be an equal distance from the midlength, and this distance should be a fraction of the distance between fillet tangent points. For a specimen whose reduced portion is five diameters long, gages at the one-quarter and three-quarter points are especially convenient. All the gages should lie in either of two axial planes, the plane of the odd numbered gages being perpendicular to that of the even numbered gages, as shown in Fig. 1.

After the load string is assembled in the machine, each element should be marked along a vertical line so that its angular position may be retained through a series of loadings. Strain readings should be taken with the specimen loaded in one position, then again after the specimen is turned 180 deg about its axis and loaded, and finally again after it is returned to its original position. This sequence should be repeated enough times to evaluate the reproducibility of the readings. Five times in each position usually is adequate.

The strain gage indicator should be read before and after each loading. The difference between the indicator reading at load and the average of the zero readings should be recorded as the gage reading. The difference between successive readings at zero load is used in the analyses and therefore should be recorded.

[2] Italic numbers in brackets refer to the list of references at the end of this paper.



Gives: a = 2 B_L = 4.8% at 3° 40' from Gage 4 toward 3

Similarly: B_U is solved using gages 5, 6, 7, 8

Then by construction above:

Bending near upper fillet B_{UF} = 6.9% at 127°
Bending at center B_C = 5.1 %
Bending near lower fillet B_{LF} = 5.0 % at 192°

FIG. 1—*Solution for bending at any longitudinal position.*

It will be shown later that the component analysis is valid only for stable joints, that is, for joints that give approximately the same direction of maximum bending during repeated loadings in the same position. The stability can be evaluated readily if the gage readings are arranged in two tables, one table for each specimen position, with one column for each of the eight gages. Any large shift in the direction of bending during successive loadings in the same positions is made readily observable by placing parentheses around the highest reading from each set of four gages and underlining the next highest reading. If all the highest readings are in one column, the load string has sufficient stability to make component analysis worthwhile. Sufficient stability also is indicated if all the largest and next largest readings appear in only two columns. An example of typical data marked in this way is shown in Appendix IV.

Readings which differ by less than two percent between maximum and minimum in a set of four should be excluded from this test for stability.

Presentation of Formulas

First, a method for normalizing the strain gage readings is suggested; then, the well known formulas for calculating bending at one longitudinal position are reviewed. Next, the bending at two longitudinal positions is used to calculate bending strain at any longitudinal position. It will be shown that the maximum value occurs near the fillets and that the value at midlength is equal to that determined by the extensometer measurements used by earlier investigators [2]. Finally, several methods of dealing with gage inaccuracies are presented. A similar derivation may be used to obtain formulas for any specimen whose cross section has an axis of symmetry; however, the formulas shown here apply only to specimens of circular cross section.

Maximum Bending Strain at One Longitudinal Position

It is shown in Appendix I that the strain at the axis of the specimen is the average of the strains at the two ends of any diameter:

$$\epsilon_O = \frac{\epsilon_A + \epsilon_C}{2} = \frac{\epsilon_B + \epsilon_D}{2}$$

where ϵ = strain, subscript O refers to the specimen axis, subscripts A and C refer to locations at the ends of one diameter, and subscripts B and D refer to locations at the ends of another diameter in the same cross-sectional plane.

If strain gage measurements are substituted in the formula above, it usually will be found that a different strain at the specimen axis will be obtained from each opposite pair of gages, thus the equality will not be satisfied. Assuming that the discrepancy is due to inaccuracies in strain measurement, the strain gage readings will be given a different symbol than the strain. The best estimate of strain at the specimen axis is

$$\epsilon_O = \frac{g_A + g_C + g_B + g_D}{4} \qquad (1)$$

where g = strain gage reading.

Let bending strain at a specified location be defined as the difference between the strain at that location and the strain at the specimen axis. Appendix I shows that the bending strains at opposite ends of a diameter are equal in magnitude but opposite in sign. However, when strain gage readings are substituted into this definition, using the strain at the specimen axis as defined above, it usually will be found that the bending strains from opposite gages are not equal and opposite. Since this relationship is fundamental in the derivation of the formulas, it should be enforced by applying a gage adjustment to the bending strains before proceeding with the remaining calculations. A simple and, in most cases, adequate adjustment is to decrease the larger absolute value and increase the smaller absolute value by one half the difference of the absolute values. The adjustment added to one pair of opposite gage readings will have the same magnitude and opposite sign as that added to the other pair. An algebraic restatement is shown below. The equalities can be demonstrated by simply substituting the expression above for ϵ_O in the following formulas.

By definition,

$$\left.\begin{array}{ll} b_A' \equiv g_A - \epsilon_O & b_B' \equiv g_B - \epsilon_O \\ b_C' \equiv g_C - \epsilon_O & b_D' \equiv g_D - \epsilon_O \end{array}\right\} \qquad (2)$$

where b = bending strain and the prime indicates the value before it is normalized, that is, when $b_A' \neq -b_C'$ and $b_B' \neq -b_D'$.

By definition, let

$$a \equiv \frac{b_A' + b_C'}{2} \qquad (3)$$

where a = the gage adjustment.

By definition, let

$$\left.\begin{array}{ll} b_A \equiv b_A' - a & b_B \equiv b_B' + a \\ b_C \equiv b_C' - a & b_D \equiv b_D' + a \end{array}\right\} \qquad (4)$$

Then, $b_A = -b_C$ and $b_B = -b_D$ and the bending strains are normalized and suitable for use in any of the following formulas. An example of one adjustment of gage readings is shown in Appendix IV.

In general, the maximum value of bending strain anywhere around the circumference will not be at a gage location. Nonetheless, the position of the maximum value can be located and its magnitude determined by simple formulas if the gage positions are restricted to four, all equally spaced around a circumference. That restriction will apply to all the following formulas. It is shown in Appendix I that then the direction of the position of maximum bending strain and its magnitude are given by

$$\tan\theta = \frac{b_N}{b_L}; \qquad b = \frac{b_L}{\cos\theta} \qquad (5)$$

where θ = central angle between point of maximum bending strain and gage L, measured from L toward N (subscript L refers to gage with the largest bending strain; subscript N refers to the gage with the next largest bending strain) and b = magnitude of the maximum bending strain at the longitudinal position of the gages.

It is customary to state maximum bending strain as a percentage of strain at the specimen axis, or

$$B = \frac{b}{\epsilon_O} \times 100 \qquad (6)$$

for B = maximum percent bending strain at the longitudinal position of the gages. Hereafter, the terms "bending" and "eccentricity" without modifiers will be used to indicate the largest value at any position around the circumference at that longitudinal location. The adjective "maximum" will be added to indicate the largest value anywhere on the cylindrical surface of the specimen.

Maximum Bending Strain Considering All Longitudinal Positions

A convenient way of quantitatively describing the bending action on a tension specimen is in terms of the radial distance from the centroid of a cross section of the specimen to the line of action of the applied force. This vector quantity will be called the eccentricity. Jones and Brown [*1*] have shown that, when stress is proportional to strain,

$$e = \frac{BR}{400} \qquad (7)$$

where

e = magnitude of maximum value of eccentricity and
R = radius of the specimen cross section.

Since the maximum percent bending, B, and the eccentricity, e, are proportional, either quantity may be used in constructions such as Fig. 1.

Neglecting the pull of thermocouple wires, extensometer attachments, and gravity, the specimen is loaded only by two force systems, one at each grip. For static equilibrium, the resultants of each of these two systems must be equal, opposite, and colinear. Since two points determine the location of a line, determination of eccentricity at two different longitudinal positions is sufficient to locate the line of action of the force relative to the axis of the specimen. These two eccentricities can be calculated by the method of the preceding section applied to each of the two sets of four gages. Then the eccentricity at any longitudinal position can be determined by the construction shown in Fig. 1. Bending deflection of the specimen axis is assumed to be insignificant in a graphical solution, so the axis of the specimen appears as a point in the axial view. The construction is based on two geometric theorems. The first theorem states that three or more parallel planes divide all straight lines passing through them into parts such that the ratios of the lengths of the parts of one line are equal to the ratios of the corresponding parts of all other lines. The second theorem states that the ratios of the lengths of the parts of any straight line are equal to the corresponding ratios of the projections of those parts on any plane.

It is apparent from inspection of Fig. 1 that, for straight specimens, the greatest bending strain occurs near the fillets, unless it is uniform along the length of the reduced portion.

The graphical solution shown in Fig. 1 is convenient for demonstration but is not suitable for machine calculation. When curvature of the specimen axis is neglected, eccentricity and therefore bending strain vary linearly along any element of the cylindrical surface. Knowing the change in bending strain per unit length between the gages, the change at any position can be calculated readily and the method of the preceding section applied to the four calculated values of bending strain at the new position. An example of this numerical method is shown in Appendix IV.

Bending Strain Measured by an Extensometer

The term "extensometer" is used here to denote an instrument attached to the uniform portion of the specimen at two points in an axial plane, the distance between these points being several times the diameter of the gage length. The term "strain gage" refers to a grid cemented to the specimen so that its longer members are parallel to the specimen axis, their length being less than one specimen diameter.

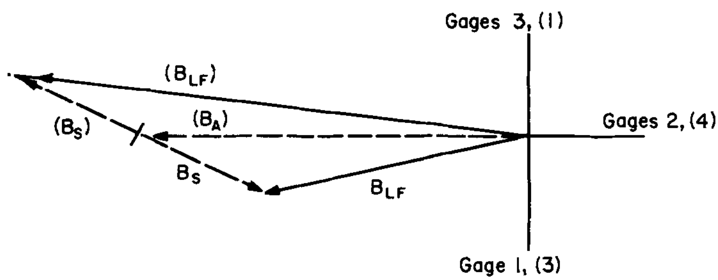
Appendix II shows that the average strain measured by each of these instruments is equal to the strain at the center of its gage length; therefore, the preceding formulas are applicable, without modification, to extensometer readings, but with the following limitation on the significance of the results. Inspection of diagrams such as Fig. 1 makes it apparent that a centrally located extensometer can read only the maximum strain in the gage length if the eccentricity is everywhere the same. At the other extreme, it will read zero bending strain when the eccentricities at each end of its gage length are equal and opposite. Therefore, readings from centrally located extensometers and strain gages may give little information on the maximum value of bending in the reduced section.

Apparatus Contribution to Eccentricity

Consider two parts held in relative orientation by compressive contact at the interface surface, for example, a nut and a bolt. If there is an angle or a space between the axis of the bolt and that of the nut, one or both have imperfect threads. To separate the total misalignment of the axes into the contributions of the nut and of the bolt, the nut may be held and the bolt turned. If the axis of the bolt is not displaced, only the nut is said to contribute to the misalignment. If movement of the axis of the bolt generates a cone (when the lead is subtracted) or a cylinder, the bolt is said to contribute to the misalignment by the half-angle of the cone or the radius of the cylinder. Obversely, by turning the nut, its contribution may be measured. If, on being turned back, the axis of the bolt (or nut) does not follow the same path, the joint is classified as unstable and the analysis presented below does not apply. Data analogous to turning a bolt in a nut may be obtained during an axiality test on a tension specimen by applying the loading procedure previously described.

The solution for the apparatus contribution to eccentricity is based on the assumption that the eccentricity at one longitudinal location consists of two components, namely the apparatus component and the specimen component. The specimen component rotates relative to the machine as the specimen is turned while the apparatus component is stationary. The magnitudes of both are assumed to be independent of specimen position. The vector components may be solved for by two methods. If the maximum percent bending already has been measured at a longitudinal position for the two angular positions, the most direct method is to use the graphical construction shown in Fig. 2.

Appendix III derives the following alternative method of solving for the components of bending without graphical construction. If the bending strains for a single gage at the two positions in space are averaged, the resulting value is due to the specimen eccentricity. If all gages are similarly treated, the averages may be used in the formulas of the preceding section



Values for Second Position in Parenthesis

B_{LF} from Figure 1

(B_{LF}) from similar solution after specimen has been turned 180°

$(B_A) = B_A$ = Apparatus contribution to bending solved by construction above = 7.3% at 180°

$-(B_S) = B_S$ = Specimen contribution, solved similarly = 2.5% at 24° from Gage 2 toward 1

FIG. 2—*Solution for apparatus and specimen components of bending.*

to find the specimen contribution to bending. If, instead of the average, one half the difference is used, the apparatus contribution is solved by the same formulas. An example in Appendix IV illustrates this nongraphical method.

Relative Gage Factors

Normally, if a specimen is loaded in the elastic range five times, with the same force, the readings taken at zero load between load applications will be slightly different. Let the difference between the successive readings at zero load be called the "reading error." The gage adjustment described previously usually will be approximately equal to this reading error. When this occurs no further correction of the gage readings is worthwhile.

On the other hand, if one gage of the eight deviates 25 percent or more in a reading from the other seven gages when the specimen is loaded in several angular positions, the readings of that gage should be discarded. The strain at the specimen axis can be calculated from the other pair of the set and the bending strain at the position of the defective gage taken equal and opposite to the bending strain of the opposite gage.

When the gage adjustment is several times the reading error, yet no one gage is clearly defective, then one of two corrective procedures may be applied. Relative gage factors may be calculated from the axiality test and used to identify the defective gage, or the gages can be calibrated by a bending test and the calibration factors applied to each gage. Both procedures will be described below.

The basic operation for determining the relative gage factor for two gages is to test each gage in two positions where a relationship between the strains is known. For a tension load string which is stable, it is convenient to use readings with the specimen turned 180 deg between loadings, since these same readings are used to determine the apparatus contribution to bending.

Let the gage factor be defined by

$$\epsilon_n = C_n g_n$$

where C_n = the gage factor of gage n. The sum of strains on opposite sides of the specimen must be the same for the same pair of gages in both positions. Or, stated algebraically,

$$C_1 g_{1a} + C_3 g_{3c} = C_1 g_{1c} + C_3 g_{3a}$$

and, rearranging terms,

$$\frac{C_3}{C_1} = -\frac{g_{1a} - g_{1c}}{g_{3c} - g_{3a}} \quad \text{(8)}$$

where subscripts 1, 2, 3, 4 refer to equally spaced gages around the specimen; subscripts a, b, c, d are positions which remain fixed in space as the specimen is turned, gages 1, 2, 3, 4 being at a, b, c, d, respectively, during the first loading and at c, d, a, b during the second loading.

Considering the diameter perpendicular to the first in the same way,

$$\frac{C_4}{C_2} = -\frac{g_{2b} - g_{2d}}{g_{4d} - g_{4b}} \qquad (9)$$

Now, considering both diameters during one loading,

$$C_1 g_{1a} + C_3 g_{3c} = C_2 g_{2b} + C_4 g_{4d}$$

Substituting for C_3 and C_4 from Eqs 8 and 9,

$$\frac{C_2}{C_1} = \frac{g_{1a} + \dfrac{C_3}{C_1} g_{3c}}{g_{2b} + \dfrac{C_4}{C_2} g_{4d}} \qquad (10)$$

To complete the set relative to gage 1,

$$\frac{C_4}{C_1} = \frac{C_4}{C_2} \times \frac{C_2}{C_1} \qquad (11)$$

In principle these formulas permit the calculation of relative gage factors from any set of data. As explained below, in practice their usefulness is limited by the accuracy of the data and the amount and direction of bending. As the apparatus contribution of bending approaches zero, so do the numerators and denominators in Eqs 9 and 10. Since these differences are nearly equal numerically, they may have large percentage errors when the percentage errors in the strains are small. Because the relative gage factors usually range from 0.9 to 1.1, the numerators and denominators should be at least 20 times as large as the estimated reading error for the relative gage factor formulas to be used quantitatively. However, the formulas may be used qualitatively in the following way.

Usually at least three of the four pairs of opposite gages on a specimen will have sums that lie within the reading error from their average. These pairs can be used to calculate the strain at the specimen axis. Then it is reasonable to assume that only one gage of the one deviant pair is in error. This gage can be identified by formula 8 or 9. For example, if the sum of the readings of gages 1 and 3 is smaller than the other sums and C_3/C_1 is greater than one, gage 3 is reading low. Unless a calibrating test is made, the gage should be regarded as defective, its readings discarded, and the calculation completed as described previously.

In the example above, if the bending strains at the deviant pair approach the reading error in magnitude, the specimen should be turned so that the deviant pair is in the plane of maximum bending and the test repeated. Several tests should confirm the identity of a defective gage before its readings are disgarded.

Since the bending during most tension tests is small, a separate bending test without tension usually is a much more accurate way of obtaining relative gage factors. A convenient way of making the bending test is to grip one end of the specimen in the collet of a lathe or indexing head and attach an extension rod to the other end. The rod should be chosen to produce a strain of about 10^{-4} due to its own weight. Then the specimen is turned until one strain gage reads as it did before the specimen was placed in the collet. The collet is then turned 90 deg to bring the gage to the upper part of the circumference. By dead weight load hung at a circumferential groove in the extension rod, the strain is increased to approximately the value used during the axiality test, care being taken not to exceed the proportional limit of the specimen at locations nearer the collet. The strain indicator is read and the dead weight removed. This process is repeated for each of the gages on the specimen. The relative gage factor for each gage is obtained by dividing the reading of that gage by the reading of one gage arbitrarily chosen as the reference gage. Noting the angular position when the strain gage indicates zero strain measures the relative angular position of the gages. The errors in longitudinal placement are proportional to the differences in gage factors when the specimen is retested with the other end in the collet. The average of the relative gage factors from each grip position should be used for each gage.

Returning to the axiality test, the corrected gage readings are obtained by multiplying the gage reading by the relative gage factor. Even after this correction, bending strains from opposite gages will not be equal and of opposite sign, therefore, it is necessary to normalize by applying the gage adjustment described earlier. After correction the adjustment is usually less than the difference between successive readings at zero load.

Description of Tests

The experiments were designed to test the joint stability of various forms of grips and the applicability of the formulas dealing with apparatus contribution to bending. The tests were made on three tension specimens with different forms of grip ends; namely, threaded ends, buttonheads, and taperheads. Their ends and reduced portions were finished by grinding on center to assure symmetry. The reduced portion of each specimen was 0.5 in. in diameter and 4.5 in. in length. All three specimens were made from the same bar of 12 weight percent chromium steel, which had a yield strength greater than 60 ksi. Foil gages of ⅛-in. gage length and 120-ohm resistance were cemented at the ends of perpendicular diameters in two planes 2.75 in. apart and symmetrical about the midlength of the reduced portion.

The load string which joined each end of the specimen to the machine crosshead consists of a coupling, a load bar, and a spherical nut seated in a tapered block which rests in the wedge box of the crosshead. The couplings for the threaded specimen and for the buttonhead specimen are threaded to the load bars. Except for this additional threaded joint, the load string is similar to that sketched in ASTM Specifications for Tension Testing of Metallic Materials (E 8 - 68). All thread fits are loose. The same pair of spherical nuts and tapered blocks were used during all tests, while the same pair of load bars was used for the threaded specimen string and for the buttonhead specimen string.

The couplings and load bars used with the threaded and buttonhead specimens were standard, purchased parts. The taperhead load string was designed and made locally. The specimen and a grip are shown disassembled in Fig. 3. The specimen grip ends are similar to those described by Babilon and Traenker [3] except that the included angle of the taperhead is 40 deg. The grips also are different in that they have no moving parts. When assembled for use the grip plate is bolted tightly to the pull rod.

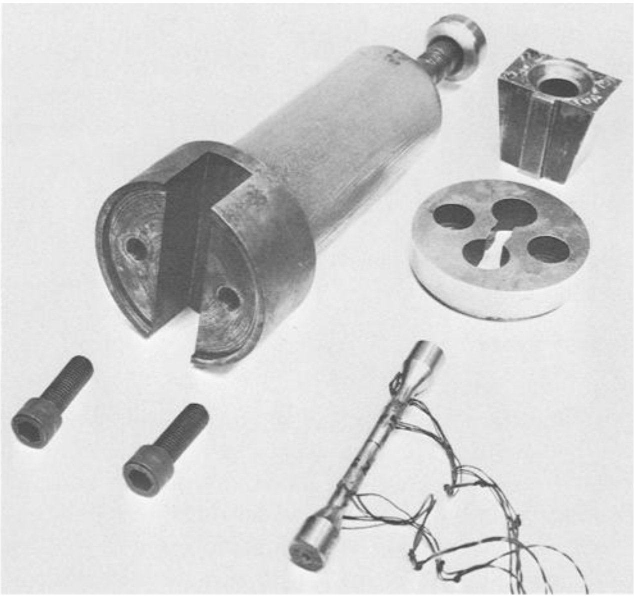


FIG. 3—*Disassembled taperhead grip and specimen.*

The specimen head is inserted into one of the two outer holes of the grip plate, and the reduced portion is moved through the slot to the center hole which has a conical seat for gripping the head.

With one exception, all tests were made using a 60,000-lb-capacity, hydraulic, universal testing machine. The series on the taperhead specimen was repeated with a second, similar machine that had seen much more service. Before these tests were made, the spherical nuts at the ends of the load strings were lapped to their seats. These seats were lubricated periodically during the tests with way oil of about SAE 30 viscosity. The backlash eliminators were adjusted to keep the lower crosshead nuts bearing on the lower surface of the supporting screw threads at zero load. The zero-load strain gage readings were taken with the load string hanging freely from the upper spherical nut. The specimen was not aligned or moved manually after being connected to the couplings. The loading sequence recommended earlier was applied, the maximum force being 6000 lb during each loading. The tests on any one specimen were made consecutively on the same day, but tests on different specimens were separated by periods (days) during which the machine was used in other loading programs.

After the results of the axiality tests were calculated, some additional testing seemed appropriate. The results from the buttonhead specimen indicated that one or maybe more gages were defective, so that specimen was tested as a beam to provide data for calculating the relative gage factors. Further testing, after the first series, also was stimulated by the observation that the maximum percent bending was about twice as large during the first loadings as during subsequent loadings. To learn more about this, four dial indicators were placed to measure the tilt of one crosshead relative to the others. Then tests similar to those above were made under the following conditions:

(*a*) Tapered blocks tight in crosshead.

1. Elevator screws not moved between loadings.
2. Elevator screws moved between loadings.
3. Backlash eliminators manually depressed between loadings.

(*b*) Tapered blocks pushed loose before first loading.

(*c*) Axiality test alternated with tension test to failure on specimens requiring more than 30,000 lb of force.

Results of Tests

The different specimens are compared in Table 1. The upper portion of the table shows the magnitude and direction of percent maximum bending for repeated loadings in the same position. The end of the specimen with the larger sum of bending values for the five loadings was chosen for presentation. For the bending near the fillet, the larger of the two values

TABLE 1—*Comparison of specimens and machines.*

Specimen ends	Threaded		Button		Taper		Taper	
Machine	1		1		1		2	
Threaded joints in load string	6		4		2		2	
Maximum Percent Bending for Successive Loadings, Same End								
Loading 1	22.2 at 164 deg		22.8 at 121 deg		21.9 at 355 deg		12.6 at 185 deg	
Loading 3	2.1 at 61 deg		3.9 at 113 deg		6.8 at 342 deg		6.1 at 179 deg	
Loading 5	4.0 at 248 deg		2.2 at 155 deg		3.7 at 305 deg		6.0 at 188 deg	
Loading 7	2.2 at 59 deg		3.4 at 16 deg		15.1 at 333 deg		3.5 at 206 deg	
Loading 9	6.6 at 110 deg		2.4 at 6 deg		3.4 at 337 deg		5.4 at 192 deg	
Range of last four	189 deg		149 deg		37 deg		25 deg	
Average Percent Bending for Ten Loadings, Excluding First								
Midlength of specimen	2.7		4.4		4.4		7.7	
Near fillet[a]	6.7		8.7		6.6		9.2	
Ratio of above	2.5		2.0		1.5		1.2	
Averages of Measures of Accuracy, microstrain								
Gage position	Upper	Lower	Upper	Lower	Upper	Lower	Upper	Lower
Gage adjustment	0.5	2.2	13.5[b]	0.8	1.0	2.4	1.5	2.8
Difference at zero load	2.3	1.8	2.0	2.4	2.2	2.2	5.2[c]	3.4[c]
Average Strain at Specimen Axis, before Adjustment, microstrain								
Gage position	Upper	Lower	Upper	Lower	Upper	Lower	Upper	Lower
Gages 1/3 and 5/7	972	974	973	975	973	968	981[c]	975[c]
Gages 2/4 and 6/8	973	969	946[b]	974	974	973	979[c]	974[c]

[a] The larger of the two end values represents each loading.
[b] Not used in calculations, reading from defective gage discarded.
[c] Different strain gage indicator used with second machine.

from the ends of the specimen was used to represent each loading except the first. The first loading was excluded because the value was markedly different owing to an easily remedied testing condition which will be described later.

The middle section of Table 1 shows the measures of accuracy which determine the method by which the gage readings should be normalized. Since the adjustment is about equal to the strain difference at zero load for all except the upper gages on the buttonhead specimen, the adjustment by difference splitting was used. For the exceptional set of gages, formula 9 gives $C_4/C_2 = 0.92$; therefore, if only one gage is considered to be in error, either gage 4 reads high or gage 2 reads low. Since the bottom section of Table 1 shows the sum for gages 2 and 4 to be low, gage 2 is assumed defective and its readings are discarded. On the other hand, the beam test for relative gage factors gives $C_2/C_1 = 1.0481$, $C_3/C_1 = 0.9873$, and $C_4/C_1 = 0.9993$. This shows that, while gage 2 is the least accurate, gage 3 also should be corrected. To test the validity of these factors, the average strain at the specimen axis for a pair was multiplied by the average of the factors for that pair. Corrected in this way, the two pairs gave values which agreed within one microstrain, indicating that the factors are accurate.

The tests made to explain the unusual behavior during the first loading showed that the tilting of the crossheads was small, about 1/4000 rad from zero to 6000 lb. The direction of specimen bending did not correlate with the direction of tilting. It was found that the high reading could be reproduced only by the first loading following the loosening of the tapered blocks from the machine heads after they had become stuck due to regular tension tests at high load levels. If they were left stuck, the high bending reading did not occur. Once loosened, the blocks did not stick in spite of the 6000 lb of force applied during this last series of tests. During the first three series the block behavior was not noted; thus the sticking may, have reoccurred at random intervals. This would be a likely explanation of deviant values appearing in the tables, for example, the seventh loading of the taperhead specimen in machine 1.

Looking again at the upper portion of Table 1, we see that the threaded specimen had the largest variation in direction of bending for successive tests while the taperhead specimen had the least. This same relationship also is observed for the components of bending in Table 2. Therefore, the specimen joints, in order of decreasing stability, are taperhead, buttonhead, and threaded.

Table 2 shows that the apparatus component of bending has a preferred direction. This preference for the 0 or 180-deg direction is most clear in the tests of the taperhead specimen but is detectable in the tests on the other specimens as well. The 0-180-deg direction is approximately perpendicular to the long direction of the machine crossheads and is the direction

TABLE 2—*Apparatus and specimen components of maximum bending during successive pairs of loadings.*

Loading	3 and 4	5 and 6	7 and 8	9 and 10	Averages by	
					Tests[a]	Gages[b]
			Threaded Specimen in Machine 1, Upper Fillet[c]			
Apparatus	8.3% at 216 deg	2.5% at 190 deg	2.1% at 225 deg	2.6% at 223 deg	3.9% at 214 deg	1.8% at 75 deg
Specimen	5.0 at 130	3.0 at 86	0.8 at 331	5.8 at 46	3.6 at 58	4.1 at 204
			Buttonhead Specimen in Machine 1, Lower Fillet[c]			
Apparatus	1.3 at 352	9.7 at 166	4.3 at 159	6.3 at 224	5.3 at 183[d]	5.2 at 2
Specimen	2.1 at 245	5.3 at 312	2.7 at 280	3.0 at 54	3.3 at 313	1.2 at 350
			Taperhead Specimen in Machine 1, Lower Fillet[c]			
Apparatus	5.8 at 344	3.5 at 310	11.5 at 336	3.3 at 328	6.0 at 332	6.1 at 339
Specimen	1.1 at 4	0.3 at . . .[e]	3.6 at 327	0.1 at . . .[e]	1.3 at 346	1.3 at 352
			Taperhead Specimen in Machine 2, Lower Fillet[c]			
Apparatus	9.6 at 168	7.0 at 174	7.2 at 186	6.2 at 180	7.5 at 177	7.3 at 180
Specimen	3.7 at 332	1.9 at 305	4.0 at 349	1.7 at 306	2.8 at 323	2.5 at 336

[a] Average of values to the left.
[b] Four gage readings at each position averaged, result calculated once.
[c] End which had the higher value of apparatus component tabulated.
[d] Average of three with largest machine component.
[e] Angle considered inaccurate at magnitudes less than 0.5 percent.

of the plane of the forces of contact between the wedge pockets and the tapered blocks at the upper and lower end of the load string.

Discussion

Previously Developed Formulas

The relationship of one plane to another is determined by the distance between them at three points. Therefore, accepting the usual assumption that plane sections remain plane during tension and bending, strain measurements at three different positions around the circumference of a tension specimen are necessary and sufficient to establish the strain at all positions around the circumference. Morrison [2] presented formulas for finding the maximum value, given strains at three equally spaced positions. He used a three-element extensometer which could be removed and recalibrated readily, therefore, three sensing elements were adequate.

Most recent investigators have found it convenient to use electrical resistance gages which are cemented to the specimen and cannot be removed for calibration. These are inexpensive, easily applied, sensitive, and easily read. They sometimes, however, are damaged during installation or by repeated use. The resulting error is perhaps large enough to be important but too small to be obvious, a disadvantage which can be largely overcome by using four gages instead of the necessary three. The redundant gage provides a measure of the accuracy of the strain readings. The four-gage arrangement also has the advantage of permitting the testing to continue after one gage becomes inaccurate. The formulas for finding maximum strain also are simpler for four equally spaced gages.

Jones and Brown [1] derived formulas for finding maximum strain given readings from four equally spaced gages. Their formulas give exactly the same value as that presented here. A different derivation and form are used here for the following reasons:

(*a*) To obtain an explicit measure of gage accuracy.

(*b*) To determine the direction and magnitude of the bending strain.

(*c*) To show that the derivation does not require that stress be proportional to strain and that the results are valid even if plastic strain or creep occurs.

The method for determining bending strains at other longitudinal positions, given values at two positions, neglects the curvature of the specimen axis. A formula derived by Penny et al [4] was used to calculate the magnitude of the error introduced by this assumption. They considered the case of equal eccentricity at each end of the specimen. This is the case giving the largest bending deflection for a given eccentricity. For a typical specimen, with the length of the reduced section equal to five diameters, at a tensile stress equal to 1/1000 of the modulus of elasticity, curvature of the specimen axis reduces the eccentricity at the center to 0.97 of that at the ends. For the objectives and accuracy of most of these tests, that small change in eccentricity can be neglected.

Magnitude of Percent Bending for Three Grips Tested

The ASTM recommended practices allow no more than a 15 percent difference in strains at opposite ends of a diameter as measured by an extensometer. That corresponds to 7.5 percent bending at the midlength of the specimen. The second section of Table 1 shows that for the three grip tests the bending was, on the average, well within that limit.

Inspection of Fig. 1 shows that the bending at midlength differs from that near the fillets if the values near the upper fillet differ from those near the lower fillet in magnitude, direction, or both. Comparing the ratios in the second section of Table 1 to the number of loose, threaded joints listed at the top of the table shows that the difference in bending from end to end of the specimen varies with the number of loose, threaded joints in the load string. The first section of Table 1 indicates that the direction of bending during successive loadings also varies in the same way. Both of these factors indicate that adjustment or correction to reduce bending would be more complicated in a load string with many loose joints.

Apparatus and Specimen Contribution to Bending

The separation of bending into a component due to the apparatus and a component due to the specimen was introduced to obtain more accurate information about the apparatus, which could then be used to guide changes intended to reduce bending. These tests indicate that the analysis is useful in that respect but, on the other hand, that it also can give misleading results when used indiscriminately. Examples of useful and of misleading information will be given below, and then the means of identifying the data suitable for analysis will be discussed.

An example of useful information is the observation that the apparatus component of bending generally is oriented in the direction of the forces, from the wedge boxes. This would suggest that the wedge boxes are important sources of bending. Further support for this opinion arises from the fact that the same load string used in the second machine, with more worn wedge boxes, gave greater bending. As stated in the results section, the high value of bending during the first loading also was associated with the sticking of the tapered blocks. On the basis of these tests we plan to modify the load string by having the spherical nuts at the ends of the load string seat in flat plates bearing on the horizontal surfaces of the crossheads, thus eliminating the use of the tapered blocks and the possibility of unbalanced friction forces at the wedge pockets.

An example of misleading information from the component analysis is the result that the apparatus contribution was less for the threaded specimen load string than for the others and that the specimen contribution was greater for this specimen than for the others. Since the threaded specimen was machined accurately with ground threads, its contribution to bending would be expected to be about the same as the taperhead specimen instead of several times as large as indicated by the results. Other tests on similar threaded specimens show little specimen contribution when they are used in load strings with tight threads.

To separate those data that are suitable for component analysis from those that are not, use may be made of the repeatability of the measurements in each of the two positions. All these data have about the same proportionate difference between repeated measurements when the magnitude of bending is compared for the different load strings. Therefore, the magnitude is not a good discriminator. However, the ranges of direction of bending are significantly different, that for the taperhead specimen being only about one fifth of that for the threaded specimens. The stability of direction can be estimated adequately before making calculations if the strain gage readings are tabulated as suggested previously. As the magnitude of the bending approaches the error in strain measurement, the indicated direction of bending becomes random. Therefore, it seems advisable to discard direction values when the bending is less than 1 percent. Comparison of the last two rows of Table 2 indicates that the strain readings for successive runs may be averaged without significant loss of information if the direction stability meets the test above.

The specimen contribution to bending is small in the tests in which it has directional repeatability and, therefore, probable significance. For this reason the distinction between bending measured on the specimen and the apparatus contribution may seem belabored. However, in other tests involving small diameter specimens we have found the specimen contribution to be about half of the apparatus contribution. In these cases analysis to determine sources of bending is aided significantly by separation of the bending into specimen and apparatus contributions.

Choice of Load Strings

The taperhead load string is clearly the most stable when judged with respect to the range of direction of bending. This does not imply that the other joints cannot be made just as stable. For example, the threaded joints between the couplings and load bars may be eliminated easily by combining two pieces into one. Tightly fitted threads made on centers, as well as accurately machined buttonhead specimens, have both been shown to provide good axiality of loading [*1*]. However, the taperhead specimen is much less expensive to manufacture in quantity to the tolerance required for good axiality [*3*]. This is especially true when the material must be machined by grinding.

In the case of poor quality of machining, a load string consisting of many loosely fitted components would be expected to give a lower average value of bending than a tight, stable load string. This expectation is based on the fact that the loose fits can assume a large number of positions and tend to hang in a straight line. On the other hand, the random fits of a loose string would permit little reduction in bending with improved machining quality. These tests indicate that a stable load string can be made to have small reproducible bending by the following method. The source of bending in a stable load string can be identified by simply testing with each component in two reversed positions, other components of the load string being kept in one angular position. Once identified, the faulty component can be corrected or replaced. This presupposes the elimination of all slipping at the crosshead and of any significant tilting of the crossheads during loading. The former appears to be a matter of properly maintaining or eliminating the tapered blocks, while the latter appears to already exist in hydraulic machines with properly adjusted backlash eliminators.

Accuracy of Strain Gages

The lower portion of Table 1 shows that the gage adjustment required to satisfy the theoretical relationship between strains at various locations is about equal to the reproducibility of the reading at zero load. Since the latter may be considered reading error, no further calibration is required. Moreover, since both the average adjustment and the average reading error are well under 1 percent, the error in bending strain due to strain measuring errors is probably also less than 1 percent bending. This method of estimating error from the adjustment is inadequate when one gage of each pair is equally defective. Since four pairs are used such a coincidence is highly unlikely. The lower section of Table 1 shows that, in general, the averages of opposite gages agree closely. Nonetheless, if a specimen is to be used for repeated axiality tests, it would seem worthwhile to calibrate the gages by a bending test, even when the gage adjustment is small.

Conclusions

1. The contributions of the apparatus and the specimen can be separated readily if the load string is stable but not otherwise.
2. Component analysis should not be applied unless the direction of bending for five successive tests falls within a 90-deg range, excluding tests with less than 1 percent bending.
3. Determination of the apparatus contribution to bending is helpful in guiding improvements to reduce bending.
4. Carefully machined tension specimens with threaded ends, buttonheads, or taperheads have bending well within the present ASTM specifications when tested at 0.001 strain with good, commercial quality grips used at one tenth of machine capacity.

5. Tests made using load strings with loose joints show greater variation in the direction of eccentricity in the specimen, but not necessarily greater amounts of eccentricity, when compared to tests made with load strings without loose joints.

6. The effect of variations in strain gages and of small misalignments in the applied gages can be compensated for readily by relative gage factors obtained from a bending test on the specimen.

7. The tapered blocks frequently used in the wedge boxes of crossheads are a potential source of large bending strains. They should be maintained carefully. Sticking after a high load tension test is an indication of an undesirable condition that should be corrected.

Acknowledgments

Most of these thoughts evolved during a series of meetings held during 1968 and 1969 to review ASTM Recommended Practices E 29 and E 139. The organizer of the task group and each member made valuable contributions based on intimate experience with the problem of controlling bending during tension tests. Those who contributed in this way were H. R. Voorhees, chairman, Subcommittee on Test Methods, Joint Committee on Effect of Temperature on the Properties of Metals; W. F. Carew; J. F. Chard; R. B. Corbett; W. F. Domis, C. H. Faix; L. F. Galasso; W. Paquin; and G. W. Stickley. The care of J. Semczuk in making the measurements contributed substantially to this work. I am grateful to them for their help.

APPENDIX I

Direction and Magnitude of Maximum Bending Strain at One Longitudinal Position

A formula will be derived for calculating the highest value of the bending strain at one circumference of a tension specimen, given strain measurements at four equally spaced points around that circumference.

Assume that plane sections remain plane during stretching and bending. Also assume that the axial elements can be considered to remain straight and parallel when their axial length is less than one diameter. Then an unloaded section of the specimen in the form of a right circular cylinder of unit height deforms, when loaded, to a truncated right circular cylinder. The difference between the original lengths of the elements and their final lengths is the strain. This is shown schematically in Fig. 4 with the cylinder turned so that the axial plane through the element with greatest strain is parallel to the paper. Let the strain at the surface, ϵ, be considered equal to the sum of the strain at the axis, ϵ_0, plus a component of magnitude b, called the bending strain. In order to avoid confusion with the numbered gaged positions, the subscripts l (for largest), n (for next largest), t (for third largest) will be used to identify the measured strains. Relationships usually established as exercises in geometry will be stated without proof.

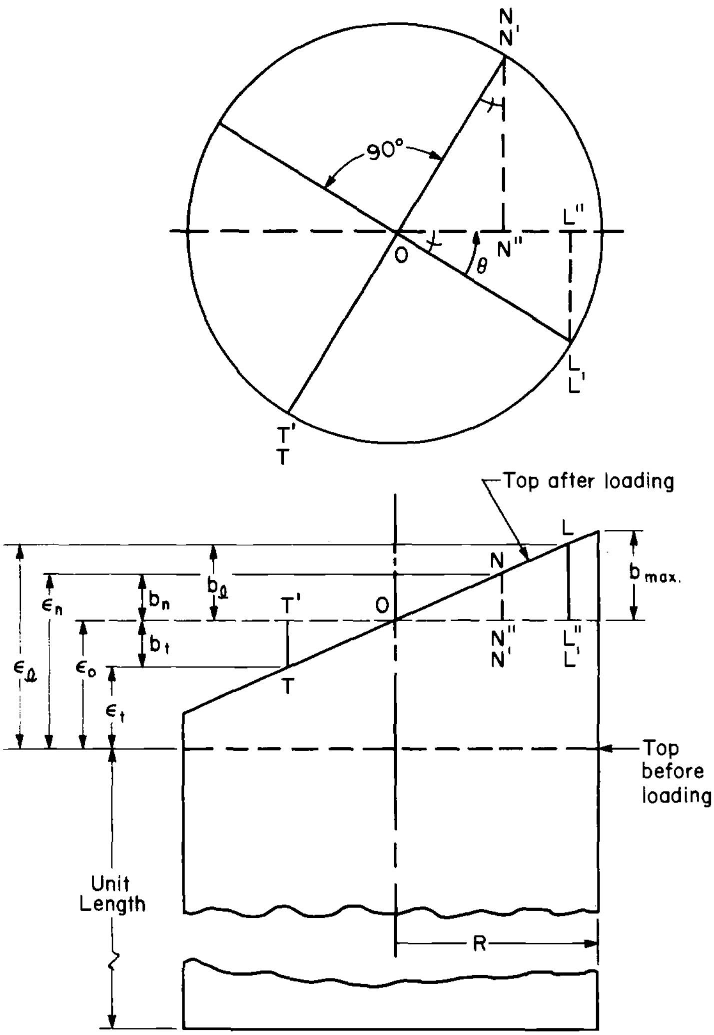


FIG. 4—*Schematic diagram of tensile plus bending strain.*

In the radial view, right triangles $OT'T$ and $ON'N$ are equal, so

$$T'T = N'N \quad \text{or} \quad b_t = -b_n$$

By definition

$$\epsilon_t = \epsilon_0 - T'T$$

and

$$\epsilon_n = \epsilon_0 + N'N$$

Adding,

$$\epsilon_t + \epsilon_n = 2\epsilon_0 + N'N - T'T$$

or

$$\epsilon_0 = \frac{\epsilon_t + \epsilon_n}{2}$$

Therefore, the strain at the axis is equal to the average of the strains at the ends of any diameter.

Considering lines in a cross-sectional plane, right triangles $ON''N'$ and $OL''L'$ are equal and

$$ON'' = L''L'$$

$$\tan \theta = \frac{L''L'}{OL''} = \frac{ON''}{OL''}$$

In the radial view, triangles $ON''N$ and $OL''L$ are proportional, so

$$\tan \theta = \frac{N'N}{L'L} = \frac{b_n}{b_l}$$

Considering the same views and triangles again,

$$\frac{LL''}{b_{\max}} = \frac{OL''}{R} = \cos \theta$$

or, rewritten,

$$b_{\max} = \frac{LL''}{\cos \theta} = \frac{\epsilon_l - \epsilon_0}{\cos \theta} = \frac{b_l}{\cos \theta}$$

APPENDIX II

Relationship between Average Strain and Local Strain

In general, the eccentricity along an extensometer gage length varies in both magnitude and direction, resulting in a variation in strain. It will be shown that the measured average strain is equal to the strain at the center of the gage length.

First courses in strength of materials show that, when strain is proportional to stress, the strain at the location where the axial plane in the x direction cuts the surface is

$$\epsilon_x = \epsilon_0 + b_x$$

$$= \frac{P}{\pi R^2 E} + \frac{4P}{\pi R^3} e_x$$

where

P = the axial component of the applied force,
R = the radius of the cross section,
E = the modulus of elasticity of the material, and
e_x = the rectangular component of the eccentricity in the x direction at the longitudinal position being considered.

The average strain read by an extensometer in that plane would be

$$\bar{\epsilon}_x = \frac{P}{\pi R^2 E} \left[1 + \frac{4}{R} \frac{1}{L} \int_0^L e_x dL \right]$$

where L = the gage length of the extensometer. The integral can be solved by picturing the line of action of the force projected as in Fig. 1 and by using the proportionality between its projected length and the specimen gage length.

Let k be defined as

$$k \equiv \frac{L}{F} = \frac{dL}{dF}$$

where $F =$ the length of the projection of that portion of the line of action of the force which lies within the gage length. Then

$$\frac{1}{L}\int_0^L e_x dL = \frac{k}{L}\int_0^F e_x dF$$

The integral to the right is recognized as the first moment of a line about an axis perpendicular to the x axis and is, therefore, equal to the length of the line times the perpendicular distance from the axis to the centroid of the line:

$$\frac{1}{L}\int_0^L e_x dL = \frac{k}{L} F\bar{e}_x = \bar{e}_x$$

where $\bar{e}_x =$ the component of eccentricity at the midlength of F and therefore at the middle of the gage length as well. Substituting for the integral in the formula for strain over the extensometer gage length makes that formula identical to the first formula for strain at a point, in this case the point being at the center of the gage length of the extensometer.

APPENDIX III

Machine and Specimen Components of Eccentricity

Assume that the eccentricity at a gaged longitudinal position is the sum of two vector components, namely, the apparatus contribution and the specimen contribution shown in Fig. 2. Assume further that, if the specimen is turned, the specimen eccentricity vector turns likewise while the apparatus eccentricity remains unchanged. Then, the rectangular component of the eccentricity in the x direction is

$$e_x' = e_a \sin\alpha + e_s \sin\beta$$

$$e_x'' = e_a \sin\alpha + e_s \sin(\beta + 180)$$

$$= e_a \sin\alpha + e_s \cos\beta$$

where

$e_a =$ the apparatus eccentricity,
$e_s =$ the specimen eccentricity,
$\alpha =$ the angle between e_a and the x direction,
$\beta =$ the angle between e_s and the x direction,
Single prime indicates strain during loading in the first position,
Double prime indicates strain during loading after turning specimen 180 deg.

If during the first loading strain gage 1 is toward the front of the machine, then during the second loading strain gage 3 will be toward the front of the machine. Using the first formula from Appendix II, the strain toward the front of the machine for each loading is

$$\epsilon_1' = \frac{P}{\pi R^2 E}\left[1 + \frac{4}{R}\,(e_a \sin \alpha + e_s \sin \beta)\right]$$

$$\epsilon_3'' = \frac{P}{\pi R^2 E}\left[1 + \frac{4}{R}\,(e_a \sin \alpha - e_s \sin \beta)\right]$$

Adding these equations,

$$\frac{\epsilon_1' + \epsilon_3''}{2} = \frac{P}{\pi R^2 E}\left[1 + \frac{4}{R}\,e_a \sin \alpha\right]$$

Substituting $\epsilon = \epsilon_0 + b$ and $\epsilon_0 = \dfrac{P}{\pi R^2 E}$,

$$\frac{b_1' + b_3''}{2} = \frac{4P}{\pi R^3 E}\,e_a \sin \alpha$$

Noting that $b_1 = -b_3$, a more convenient form can be obtained:

$$\frac{b_1' - b_1''}{2} = \frac{4P}{\pi R^3 E}\,e_a \sin \alpha$$

where the right-hand term is the contribution to bending at gage 1 of the apparatus alone.

Similarly, if the equations above are subtracted rather than added, we find

$$\frac{b_1' + b_1''}{2} = \frac{4P}{\pi R^3 E}\,e_s \sin \beta$$

which is the strain due to the specimen eccentricity at the gage position toward the front of the machine. In the same way, strain due to the separated components of eccentricity may be found by taking the sum and difference of the two strain readings at each of the other three positions relative to the machine.

APPENDIX IV

Example of Numerical Solution for Components of Bending

Stability Criterion and Normalizing

For the gage positions in Fig. 1, the gage readings in microstrain are

Loading Number	Lower Gages				Upper Gages			
	1	2	3	4	5	6	7	8
1	963	864	975	(1086)	920	(1055)	1032	901
3	960	920	989	(1036)	924	1030	(1031)	926
5	975	924	978	(1039)	941	(1034)	1018	922
7	975	946	967	(1013)	950	(1017)	1011	948
9	974	943	976	(1020)	941	1005	(1012)	946

Parentheses indicate the highest strain reading in a set of four, an underline indicates the next highest. Since the stability criterion is met, the readings will be averaged and calculated only once.

Column average	969	919	977	1039	935	1028	1021	929
Set average, ϵ_0	976	976	976	976	978	978	978	978
Bending,[a] b'	−7	−57	+1	+63	−43	+50	+43	−49
Adjustment, $\pm a$	+3[b]	−3	+3	−3	0	0	0	−1
Bending, b	−4	−60	+4	+60	−43	+50	+43	−50

[a] Difference of column and set averages.

[b] $a = \dfrac{-7+1}{2} = -3.$

Solution for Percent Bending at Midlength

The bending strain at midlength on a particular element is the average of the two readings on the same element of the cylindrical portion of the specimen.

Gage numbers	5/1	6/2	7/3	8/4
Bending strain	−24	−5	+24	+5

$$\tan\theta = \frac{b_N}{b_L} = \frac{5}{24};\ \theta = 12 \text{ deg from gages 7/3 toward 8/4}$$

$$b = \frac{b_L}{\cos\theta} = \frac{24}{0.98} = 25$$

$$B = \frac{b}{\epsilon_0} \times 100 = \frac{25}{977} \times 100 = 2.6\% \text{ at 102 deg counterclockwise from gage 2}$$

Solution for Percent Bending at Fillets

The rate of change of strain along an element is the difference between the bending strain at the two gages divided by the distance between the gages. This rate multiplied by the distance from midlength to the fillet is the quantity added to or subtracted from the bending strain at midlength to obtain the bending strain at a fillet. If the lower gage reading is subtracted from the upper, then the distance to the upper fillet is positive and that to the lower negative. For distances of 4.5 in. between fillets and 2.75 in. between gages, the changes from midlength to fillets for the element through gages 1 and 5 are

$$\text{Change from midlength to upper fillet} = \frac{(-43-(-4))}{2.75} = \frac{4.5}{2}$$

$$= -32 \text{ microstrain}$$

Bending strain on element near upper fillet = $-24 + (-32) = -56$
Bending strain on element near lower fillet = $-24 - (-32) = +8$

The values for the other three gaged elements are obtained similarly, giving the following results:

Gaged element	5/1	6/2	7/3	8/4
Bending strain, upper fillet	−56	(+85)	+56	−85
Bending strain, lower fillet	+8	−95	−8	(+95)

The value of the percent bending at the lower fillet is then

$$\tan\theta = \frac{8}{95};\ \theta = 5 \text{ deg from gages } 8/4 \text{ toward gages } 5/1$$

$$b = \frac{95}{\cos 5} = 96;\quad B = \frac{96}{976} \times 100 = 9.9 \text{ percent at } 185 \text{ deg}$$

Solution for Components of Bending at Lower Fillet

The even numbered loadings were made with the specimen turned 180 deg from the position of the odd numbered loadings. Substituting the strain readings from the even numbered loadings into the calculation process above, values similar to those above are obtained for the second position. The values for both angular positions for the longitudinal position near the lower fillet are tabulated below.

Gages on element	5/1	6/2	7/3	8/4
5/1 toward front	+8	−95	−8	+95
5/1 toward back	+18	+149	−18	−149
Average	+13	(+27)	−13	−54
One half the difference	−5	−122	+5	(+122)

Using the average values above, the component of bending due to the specimen is found to be

$$\tan\theta = \frac{13}{27};\ \theta = 26 \text{ deg from gages } 6/2 \text{ toward } 5/1$$

$$b_s = \frac{27}{\cos 26} = 30$$

$$B_s = \frac{30}{976} \times 100 = 3.1 \text{ percent at } 334 \text{ deg counterclockwise from gages } 6/2$$

Using the values of one half the difference, the component of bending due to the apparatus is found to be

$$\tan\theta = \frac{5}{122}; \ \theta = 2 \text{ deg from gages } 8/4 \text{ toward } 7/3 \text{ when } 5/1 \text{ are toward the}$$

front of the machine

$$b_A = \frac{122}{\cos 2} = 122$$

$$B_A = \frac{122}{976} \times 100 = 12.5 \text{ percent at } 178 \text{ deg counterclockwise from gages } 6/2$$

when 5/1 is front

The data for Figs. 1 and 2 and this example were taken from the test on the taper-head specimen in machine 2. The results in this Appendix differ from the values in Table 2 because all ten loadings were used in the example whereas only the last eight were used previously. A substantial difference in the calculated magnitude but only a 2-deg difference in direction result from including the first two loadings.

References

[1] Jones, M. H. and Brown, W. F., Jr., *ASTM Bulletin*, ASTBA, Jan. 1956, pp. 53–60.
[2] Morrison, J. L. M., *Proceedings*, Institution of Mechanical Engineers, PIMLA, Vol. 142, Nov. 1939-March 1940, pp. 193–223.
[3] Babilon, C. F. and Traenkner, H. A., *Proceedings*, American Society for Testing and Materials, ASTEA, Vol. 64, 1964, pp. 1119–1127.
[4] Penny, R. K., Ellison, E. G., and Webster, G. A., *Materials Research and Standards*, MTRSA, Vol. 6, No. 4, Feb. 1966, pp. 76–84.

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Axiality Measurements on Fifty Creep Machines

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ABSTRACT: Three types of creep machines were tested with a normal load string and specimen. One of these and one of a fourth type also were tested with a single rod replacing the normal load string. On the average, each type has bending strains within the ASTM allowable limits when tested at high stresses. The load strings with the higher temperature ratings also have the larger bending strains. For most machines the percent bending increases as the tensile stress decreases. Since the higher temperature tests usually are made at lower stresses, the two relationships above indicate that most of the tests at temperatures over 1800 F (1000 C) will have bending strains exceeding the allowable limits. Most of the nonaxiality of loading appears to be due to loose threads or machining imperfections in the couplings. The contributions to bending of crossed knife edge connectors, specimen imperfections, and strain gage inaccuracies are found to be small.

KEY WORDS: tension tests, tensile testers, test equipment, tensile stress, strains, strain gages, loads (forces), creep properties, bending, bending stress, elastic deformation

The existence of large bending stresses in specimens nominally loaded in tension was noted by McVetty in 1928 [*1*].[2] In 1939 Morrison [*2*] reported in detail an investigation of this subject and concluded that much of the scatter in tension test results was due to variations in the amount of bending. With this long history in view, it is surprising to find limited information in the literature and in testing machine manufacturers' bulletins on the bending stresses induced by standard testing machines during application of nominally tensile loads. The most comprehensive

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[2] Italic numbers in brackets refer to the list of references at the end of this paper.

collection that has come to our attention is that of Isaksson [3]. He reports bending stresses measured on three commercial machines and on four specially designed machines. In each case tests on only one machine of each type were reported. Primarily, the paper will discuss the variation in bending stresses when a typical specimen is loaded successively by eight or more creep machines of the same design. As a secondary objective, we will determine the contribution to bending of the various parts of the loading system.

Procedure for Testing

All machines of the same type were subjected to at least one similar series of tests, but some types of machines were tested in several ways while other types were tested in only one way. Nonetheless, some elements of the testing procedure were common to all tests. These common elements will be described first. All specimens were of circular cross section and had threaded ends machined by grinding. The threads were of ½-in. diameter with 13 threads per inch. Except as noted, the same specimen was used in all machines of the same type. The specimen was of the standard form for normal testing in machines of that type, except for one specimen whose entire load string consisted of a single rod. Specimens were of steel. Loads were chosen to produce only elastic strains in the reduced portion of the specimen.

Foil electrical resistance gages were used to measure strain. Gages numbered 1, 2, 3, and 4 were spaced equally around the circumference of the reduced portion of the specimen as suggested by Jones and Brown [4]. But, instead of one set of four gages in the midlength, two sets were used, gages 5, 6, 7, and 8 being directly above 1, 2, 3, and 4, respectively, during the tests. Both sets were equidistant from the midlength of the reduced portion. If during a test series one of the eight gages failed, the series was continued with that specimen, since three gages at any one longitudinal position are sufficient to determine bending strain. If two gages failed, however, the specimen was no longer used.

Before loading any machine a line was drawn down the load string on the surface nearest the operator. The specimen was turned to position gages 1 and 5 along this line. This orientation of all parts of the load string was maintained during all loadings in that machine unless otherwise noted.

Four types of machines were tested for axiality of loading. All were machines of good commercial quality designed for routine testing. They are described in Table 1.

Tests on Type A Machines

The ten machines of type A were tested more extensively than any of the others despite the fact that several different tests were made on each machine. In the first series crossed knife edge connectors were used to

TABLE 1—*Description of machines tested for axiality of loading.*

Description	Type			
	A	HTR	B	CR
Capacity, kips (kN)	6 (27)	5 (22)	6 (27)	10 (44)
Load bar diameter, in. (mm)	0.75 (19)	0.75 (19)	0.98 (25)	0.50 (13)
Load string length, in. (m)	43 (1.1)	42 (1.1)	54 (1.4)	44 (1.1)
Specimen reduced portion				
Diameter, in. (mm)	0.253 (6.4)	0.253 (6.4)	0.253 (6.4)	0.357 (9.1)
Length, in. (mm)	1.80 (46)	1.80 (46)	1.80 (46)	7.0 (178)
Load string connectors[a]				
Top	E&C or CKE	CKE	CKE	MC
Bottom	Same	UJ	Same	SN
Rated temperature, deg F (deg C)	2000 (1100)	2000 (1100)	1800 (980)	1600 (870)
Couplings	Cast threads, finished by tapping	None tested	Machined	Machined
Number of machines tested	10	20	8	12
Years of service	0	0	2	15

[a] The attachment joining the machine draw head to the load bar:
E&C indicates eye and clevis
CKE indicates crossed knife edges
UJ indicates universal joint
MC indicates machined clevis
SN indicates spherically seated nut

attach the load bars to the machine heads. Strain readings were taken at five equal increments of load to 4800 lb (21.35 kN) corresponding to a stress of 96 ksi (663 MN/m^2). To separate their contribution from that of the other parts of the loading system, the specimen and couplings were turned 90 deg counterclockwise between each of the five successive loadings. Thus, the gages were in the same position relative to the machine during the first and fifth loadings. The five loadings then were repeated with only the specimen turned. This test series was made on one machine of type A.

The second series of tests was made several weeks later on all machines. It was the same as the first series except that strains were read at loads of only zero and 4800 lb. The third series was similar to the second except that the crossed knife edge connectors were replaced by eye and clevis connectors.

During the fourth series of tests, five machines randomly selected from the ten were used to load the one-piece load string. This is a single rod with the same diameter and length as the normal load string, but with a reduced portion of the same dimensions and location as the reduced portion of the specimen. Only one set of four strain gages was used. These were located at the midlength of the reduced portion. A load of 1500 lb (6.67 kN) was applied, the gage read, and then the load removed. This sequence was repeated three times. Then the load string was turned 180 deg and the 1500-lb load similarly applied three times. In contrast to all of the other tests the specimen was not turned between successive loadings. Tests were made first using the crossed knife edge connectors and then repeated using the eye and clevis connectors. During these tests the sum of the strains read on one pair of opposing gages did not equal that of the other pair; therefore, the gages were calibrated by the bending method described by Schmieder in another paper in this publication (pp. 15–42).

Tests on Type HTR Machines

The tests on the type HTR machines were similar to the last series on the type A machines. The same one-piece load string was loaded to 1500 lb, but to test each machine the load was applied and read only once with the specimen in the first position and only once more after the specimen had been turned 180 deg. This change was made because the tests on type A machines showed that the strain readings on successive loadings of the one-piece load string were the same within the accuracy of reading. The machines were not tested with a normal load string and specimen, because these parts are similar on the type HTR and on the type A machines and presumably would load the specimen to about the same bending.

Tests on Type B Machines

The tests on the type B machines were similar to the second series on the type A machines in that the same form of specimen and the same maximum load were used. But the loading sequence was changed: the load was applied to the specimen by each machine three times in succession. Between each loading the specimen was turned 90 deg and then turned back to its original position before reloading. As with the type A machines, one type B machine was used to load the specimen five times, the specimen turned 90 deg counterclockwise between each loading.

Tests on Type CR Machines

Type CR machines were tested by loading the specimen six times to 4500 lb (20 kN). This produced an average tensile stress of 45 ksi (310 MN/m^2) in the reduced portion. Between each loading the specimen was turned 180 deg, resulting in three loadings with gages 1 and 5 toward the front of the machine separated by three loadings with gages 3 and 7 toward the front of the machine.

Method of Calculation and Definitions of Terms

The formulas used to calculate the results and their derivations may be found in the companion paper in this publication (Schmieder, pp. 15–42). For convenient reference a literal description of the calculated quantities is given below.

Measured strain at a load is the difference between the strain indicator reading at that load and the reading with the specimen loaded only by the weight of the lower end of the load string (called zero load).

Measured bending strain at a given load is one half the difference between the measured strains at two gages at opposite ends of a diameter.

Tensile strain is the average of the measured strains at four, equally spaced points around the circumference of the specimen.

Raw bending strain is the measured strain minus the tensile strain.

Strain adjustment is one half the difference in the algebraic sum of the raw bending strains for one pair of two opposite gages. When solved for the other pair of gages of the set, this quantity must have the same magnitude but opposite sign.

Normalized bending strains are the sums of raw bending strains plus the strain adjustment. They are equal in magnitude but opposite in sign for each gage of any opposing pair.

Gage calibration factor is the factor by which the measured strain is multiplied in order to obtain a corrected reading. The factor is determined by a bending test of the specimen. It is applied here only if the strain adjustment is large compared with the difference in zero readings after successive loadings.

Bending strain is the maximum value at any circumferential position calculated from the normalized bending strains.

Bending strain at gages is the value at the longitudinal position of the gages.

Bending strain at midlength is the value at the midlength of the reduced portion of the specimen. It is equal to the value calculated from extensometer readings when the gage length is centered on the reduced portion of the specimen.

Bending strain near fillet is the larger of the values near the upper or lower fillet and is the maximum value anywhere on the reduced portion of the specimen.

Percent bending strain is 100 times the ratio of bending strain to tensile strain.

Specimen contribution refers to the vector component of bending strain which is attributed to the specimen. It is determined from successive loadings with the specimen only turned 180 deg between loadings.

Specimen and couplings contribution is obtained similarly by turning the specimen and couplings together.

Apparatus contribution is the vector which, when added to one of the above, has a resultant equal to the bending strain. It includes the contribution of the loading system, load bar connectors, load bars, and (except in one case) the couplings.

Machine contribution is the bending strain attributed to the loading system and load bar connectors.

Results

Magnitude of Percent Bending

The primary results of this investigation are shown in Table 2. Points which will be discussed later include

1. The average value of percent bending at midlength for each type of machine is less than 7.5 percent.
2. The percent bending at midlength is about one half the value near the fillet.
3. With a normal load string and specimen, the bending with crossed knife edge connectors (type A, series 1 and 2) is approximately the same as with eye and clevis connectors (type A, series 3).
4. The percent bending increases with the rated temperature of the machine.

TABLE 2—*Average and maximum values of percent bending for each series.*

Type	Rated Temperature, deg F (deg C)	Test Series		Average Percent Bending			
				at Midlength		near Fillet	
				Average	Maximum	Average	Maximum
A[a]	2000	1[b]		5.6	8.4	11.9	19.6
	(1100)	2[b]		8.4	18.0	13.8	27.4
		3[c]		7.8	23.0	13.3	24.5
			Avg	7.3		13.0	
CR[d]	1600	1		3.9	9.3	8.6	16.8
	(870)	2		4.5	7.8	9.3	13.3
		3		4.4	7.5	9.5	13.5
			Avg	4.3		9.1	
B[e]	1800	1		5.6	8.0	7.6	14.6
	(980)	2		4.4	7.8	8.9	19.6
		3		6.6	11.6	10.8	19.8
			Avg	5.1		9.1	

[a] Ten machines tested at 96 ksi (663 MN/m^2), 80 percent rated capacity.
[b] Crossed knife edge connectors used.
[c] Eye and clevis connectors used.
[d] Twelve machines tested at 45 ksi (310 MN/m^2), 45 percent rated capacity.
[e] Eight machines tested at 96 ksi (663 MN/m^2), 80 percent rated capacity.

Repeatability of Axiality Measurements

From a loading of a particular machine the maximum bending and its direction were calculated at the upper and at the lower set of gages. The second loading of the same machine usually gave different values for both magnitude and direction. The differences in percent bending value (the first minus the second or the second minus the third) are shown in Fig. 1. The ordinate is a probability scale of accumulated readings constructed so that any normal distribution will have all points on a straight line. It is apparent that the distribution is close to normal, especially for the type CR machines for which 96 values of difference were plotted.

Figure 2 shows a similar plot for the difference in direction of the percent bending vector during successive loadings. Values of direction were disregarded when the percent bending was less than one.

To simplify the figures, a mean line rather than the data points is shown for type B machines. The scatter of the data points about the mean line is approximately the same as for type A machines.

In contrast to the values above, which were obtained while using a normal load string, the values obtained while using the one-piece load

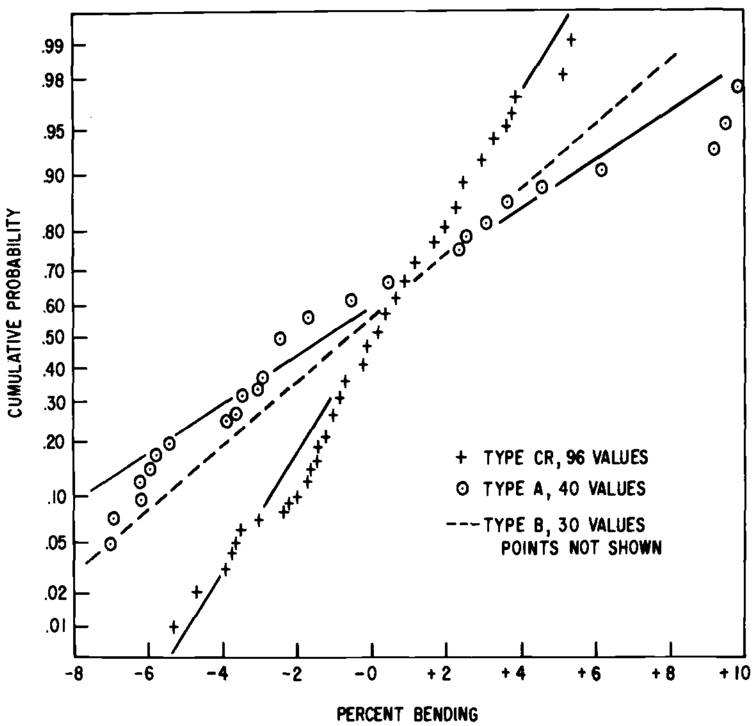

FIG. 1—*Difference between two successive measurements of percent bending at the gages.*

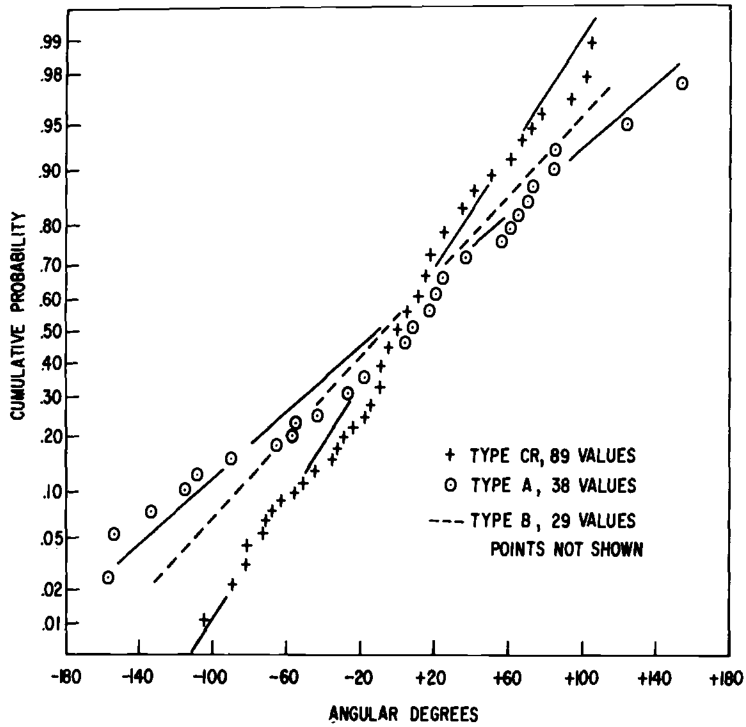

FIG. 2—*Difference in direction of bending at gages for two successive loadings.*

string showed greatly reduced differences between readings of the same gage during successive loadings. Repeated loadings on the five type A machines tested with crossed knife edges connectors afforded 80 differences whose magnitudes had an average value of 6.6 $\times$ 10^{-6} strain. Similar tests with eye and clevis connectors gave 10.1 $\times$ 10^{-6} strain. The difference between successive indicator readings at zero load had averages of 6.3 and 5.3 $\times$ 10^{-6}, respectively. Thus the difference in actual strain during successive loadings by the crossed knife edge connectors was too small to be evaluated by the strain measuring equipment used.

Variation of Measured Bending Strain with Tensile Stress

Figure 3 shows how the measured bending strain at the gages varies with load. For each machine the values shown are for the upper or the lower set of gages, depending on which gave the higher value at maximum load.

The four curves shown are representative of the curves for the other six type A machines as well. These curves show that the measured bending strain can increase, decrease, or remain approximately constant as the tensile stress on a specimen is increased. The average curve for the ten machines shows that the bending strain almost doubles as the stress is quintupled. On the other hand, the numbers beside the plotted points show that the percent bending is reduced to one third as the force is quintupled.

Comparison of Various Load String Connectors

Comparing the first two rows of Table 2 with the third row shows little change in average percent bending when the crossed knife edge connectors are replaced by the eye and clevis connectors. To eliminate from the comparison the effect of threaded joints in the load string, five

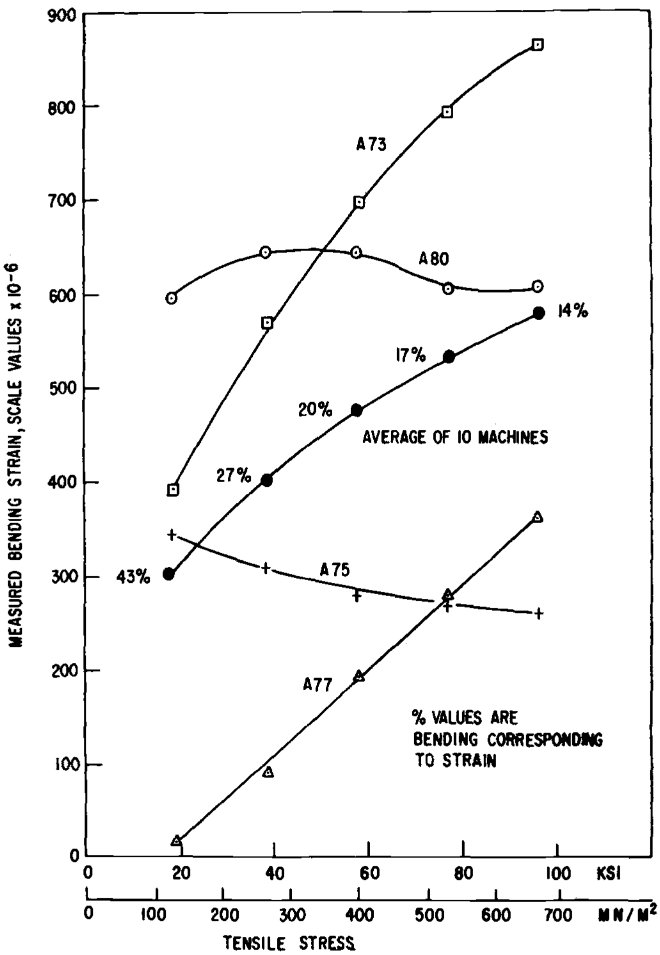

FIG. 3—*Variation of bending with tensile stress.*

TABLE 3—*Contribution to percent bending as measured by one-piece load string.*

Machine	Crossed Knife Edge		Eye and Clevis	
	Machine Contribution, %	Percent[a] Bending near Fillets	Machine Contribution, %	Percent[a] Bending near Fillets
A-78	1.7 (1)[b]	9.4 (1)	5.4 (2)	9.0 (1)
A-73	2.5 (2)	18.4 (5)	11.5 (3)	24.5 (5)
A-79	3.4 (3)	16.1 (4)	2.4 (1)	16.6 (3)
A-81	4.4 (4)	15.0 (3)	13.0 (5)	10.9 (2)
A-74	5.4 (5)	10.5 (2)	12.9 (4)	19.1 (4)
Average of type A	3.5		12.3	
Twenty HTR				
Maximum	4.3			
Minimum	0.3			
Average	1.8			

[a] Values from tests with normal, multipiece load string shown for comparison.
[b] Numbers in parenthesis are ranking.

of the ten type A machines were retested with a one-piece load string in place of the load string used in normal testing. Since the readings of each gage were nearly the same during the three loadings, the average of the three readings was used to calculate percent bending. The specimen contribution was eliminated by averaging two normalized bending strain readings at a given position relative to the machine frame, the specimen having been turned 180 deg between the two loadings.

The machine contributions to percent bending are listed in Table 3. The percent bending in normal specimens is shown for comparison. For the crossed knife edge connectors the latter value is the average of the two series, using the larger of the values for the upper or the lower fillet. Comparing the rankings of the machine contribution and the percent bending during normal testing indicates that the variation in the machine contribution has little, if any, effect on the bending of a specimen in a normal load string.

Estimates of Accuracy of Strain Measurement

Several indications, but not rigorous proofs, of the accuracy of strain measurements may be obtained by simple comparisons of the readings required for successive determinations of percent bending. These are summarized in Table 4.

One of the simplest checks of the stability and readability of the strain measuring system is the change in indicator reading at zero load, before and after each loading. This is called *zero drift*. Representative values of zero drift for each of the specimens used are shown in the upper section of Table 4. The results indicate that a zero drift of about 10×10^{-6} will occur once in ten loadings. The values for specimen 1, gage 1, show a large change in zero drift when the first ten loadings are compared to the last ten loadings. This change usually indicates impending failure of one gage. On each specimen strained over 3×10^{-3}, one gage failed before the tests were completed. No gages failed on the specimens strained to 1.5×10^{-3} or less.

A simple check on the uniformity of the gage factors and accuracy of gage placement is to compare the tensile strain as measured by one set

TABLE 4—*Estimates of accuracy from strain measurements during axiality tests.*

Specimen number	1[a]	2[a]	3[a]	4[a]	5[b]
Machines tested	10 of A	9 of CR	3 of CR	8 of B	5 of A and 20 of HTR
Difference in successive readings at zero load for ten loadings:[c]					
Gage 1					
Average	0(+8)*	−3(−1)	(+5)	(+3)*	−3(0)
Maximum	+15(+23)*	+1(+5)	(+16)	(+8)*	+9(+10)
Minimum	−7(−12)*	−7(−7)	(−2)	(−3)*	−17(−6)
Gage 5					
Average	+1(+3)	−2(−5)	(+3)	(+1)	...
Maximum	+18(+22)	+1(+4)	(+12)	(+9)	...
Minimum	−5(−10)	−9(−14)	(−3)	(−7)	...
Average of extremes	14	6	8	7	10
Difference in tensile strain as measured by two sets of gages during ten loadings:					
Average of ten	−9(+3)*	−1(+3)	(+1)	(−15)*	...
Maximum	+1(+14)*	+12(+8)	(+11)	(−1)*	...
Minimum	−20(−4)*	−13(−6)	(−6)	(−27)*	...
Average of extremes	10	10	8	14	...
Strain adjustment for each of ten successive loadings:					
Gage 1					
Average	0(−15)*	+2(+1)	(+1)	(+1)*	−21(−22)
Maximum	+8(+4)*	+5(+3)	(+4)	(+6)*	+17(+18)
Minimum	−8(−25)*	0(0)	(0)	(−4)*	−23(−27)
Gage 5					
Average	−2(−2)	−1(−1)	(0)	(−3)	...
Maximum	+2(+5)	+6(+5)	(+3)	(+1)	...
Minimum	−8(−6)	−8(−7)	(−3)	(−5)	...
Average of extremes	8	4	2	4	21

[a] Normal specimens with two sets of four strain gages.

[b] One-piece load string with one set of four gages.

[c] All tabulated numbers when multiplied by 10^{-6} are dimensionless strain. First refers to first ten loadings; number in parenthesis to last ten loadings. Asterisk indicates last ten loadings prior to failure of one gage of the set.

of four gages to that determined from the other set of four during the same load application. The center rows in Table 4 show this comparison for all specimens except specimen 5, the one-piece load string, which had only one set of gages. Again, a difference of 10×10^{-6} strain appears to occur about once in every ten loadings. This difference is at least partly a remeasurement of the zero drift discussed above.

Another check of the uniformity of gage factors and accuracy of gage placement is the strain adjustment which is added to or subtracted from each gage reading of a set in order to make the bending strains from opposite gages equal in magnitude. The strain adjustments are shown in the lower group of rows in Table 4. Except for specimen 5, the average extreme value appears to be about 5×10^{-6}. This is the value that would be required if all gages except one read accurately and if that one had an error of 20×10^{-6}.

The set which includes gage 1, specimen 1, showed a significant change in strain adjustment during the test series. Just after the final ten readings gage 3 failed. Assuming that the gradual failure of gage 3 caused the increase in adjustment, the percent bending for the two loadings when the adjustment was largest were recalculated omitting the gage 3 reading. For one loading the magnitude at the gage location changed from 12.3 to 13.1 percent while the direction changed 1 deg. In the other loading the corresponding figures were 3.4 to 4.4 percent and 21 deg. Thus, this measuring error does not change the general relationships indicated by the results.

In addition to the above indication of accuracy obtained by comparing the strain readings required for axiality measurements, several additional tests were made specifically to determine the contribution of gage errors and of specimen imperfections to the measured percent bending. Specimen 5 was chosen for gage calibration because the strain adjustment was consistently larger than the difference in readings at zero load. To calibrate the gages, the specimen was deadweight loaded as a cantilever beam. By turning the specimen each gage was placed first in the highest (maximum tension) position and then in the lowest (maximum compression) position. Since the force was the same during each loading, the change in strain from the tension to the compression position would be equal for all the gages if they all read correctly. Conversely, if they were not equal one may be assumed correct and a gage calibration factor determined for each of the other three. The gage calibration factors so obtained are

Gage	Calibration Factor
1	1.000
2	1.096
3	0.998
4	1.004

Applying these factors to the ten loadings in the type A machines reduced the gage adjustment to 0 average, +1 maximum, −2 minimum. The calculated machine contributions to bending shown in Table 3 are exactly the same whether or not the gage calibration factors are used. This test was included only to show the effectiveness of the gage calibration factors. By using the factors it is possible to separate the specimen contribution into two parts, the part due to the difference in actual strain on the opposite sides of the specimen and the part due to errors in reading the strains. For example, gages with the factors above, if placed on a perfect specimen loaded without bending, would indicate 4.5 percent bending at the gages calculated in the usual manner.

For comparison, the gage factors also were calculated from the axiality test data by Eqs 6, 7, 8, and 9 from the Schmieder paper. Since the accuracy of this method improves as the bending moment increases, the calculations were applied only to machines A-73, A-81, and A-74 with eye and clevis connectors. The calibration factors are listed below.

Gage	Calibration Factor for Machine			Average
	A-73	A-81	A-74	
1	1.000	1.000	1.000	1.000
2	1.253	0.913	1.198	1.121
3	1.025	0.963	1.040	1.009
4	0.912	1.110	0.965	0.996

It is apparent that the results of a single test calculated by this method could be misleading; however, the average of three machines clearly identifies gage 2 as the most inaccurate. The accuracy could be improved by not using the gage 2 readings or by applying the average factors to all gage readings. Without the calibration factors, the gage adjustment values for the three machines above ranged from −18 to −22. After applying the average factors above, the gage adjustment values ranged from −1 to +2.

Contribution of Specimens and Couplings to Bending

The values reported in Table 1 include the effects of specimen contributions to bending. To assure that these were not a major source of bending, the specimen contribution was evaluated at least twice for each specimen. This is done by loading the specimen in two positions, 180 deg apart, and using the average readings to calculate bending strain.

In the case of specimen 5, the gage calibration factors were applied; consequently, the specimen contributions to bending listed in Table 5 are

TABLE 5—*Specimen and coupling contributions to percent bending.*

Specimen Number	Machine Number	Thread Fit[a]	Apparatus Contribution	Specimen Contribution
1..........	A-80	tight	5.6% at 337 deg	0.6% at 182 deg
	A-80	tight	6.2 at 353	0.6 at 43
4..........	B-37	loose	12.4 at 278	6.9 at 81
	B-37	loose	11.2 at 176	3.8 at 257
2..........	CR-1[b]	medium[c]	9.5 at 130	3.4 at 90
	CR-1[b]	medium[c]	10.2 at 127	4.7 at 310
	CR-1[b]	medium[c]	11.9 at 136	1.5 at 320
2..........	CR-7[d]	medium[c]	3.2 at 200	0.6 at 324
	CR-7[d]	medium[c]	4.6 at 221	1.7 at 354
	CR-7[d]	medium[c]	2.1 at 249	5.3 at 324
3..........	CR-62[b]	loose[c]	7.2 at 212	1.0 at 158
	CR-62[b]	loose[c]	9.9 at 183	4.2 at 138
	CR-62[b]	loose[c]	8.0 at 233	5.1 at 144
3..........	CR-42[d]	loose[c]	4.9 at 87	3.4 at 181
	CR-42[d]	loose[c]	9.1 at 130	2.7 at 244
	CR-42[d]	loose[c]	11.9 at 139	2.6 at 266
5..........	ten type A machines[e]	solid	1.7 to 13.0%	10.2 to 12.1% at 219 to 225 deg
			Combined Specimen and Coupling Contribution	
1..........	A-80	tight	6.9% at 113 deg	11.1% at 309 deg
			7.2 at 130	9.0 at 320

[a] Refers to fit near reduced portion of specimen.
[b] Machine with largest average of percent bending at fillet for group.
[c] Pitch diameter of specimen 2 was 0.009 in. (0.2 mm) greater than that of specimen 3.
[d] Machine with smallest average of percent bending at fillet for group.
[e] Five machines tested with crossed knife edge connectors and then with eye and clevis connectors.

due to machining imperfections. For the others the listed values include gage errors as well as machining imperfections.

Specimens 1 and 4 were evaluated in one machine by loading in four positions, the specimen turned 90 deg between each loading. This resulted in two opposite positions for each gage and two determinations of the specimen contribution. Specimens 2, 3, and 5 were turned 180 deg between loadings, and six loadings were applied in each of several machines. The values for specimen 5 are percent bending at midlength. The others are percent bending near the fillet, where the bending strain was larger on the average.

The coupling contribution to bending was determined on machine A-80 by turning both specimens and couplings 90 deg between loadings. The results are shown at the bottom of Table 5. Since the values at the top of the table show the specimen contribution to be small, the coupling contribution is clearly large compared with the apparatus contribution.

Discussion

Our interest in this subject was stimulated by discussions of a task group charged with reviewing ASTM Recommended Practices for Short-Time Elevated Temperature Tension Tests of Materials (E 21 - 66T) and for Conducting Creep and Time-for-Rupture Tension Tests of Materials (E 139 - 66T). These practices specify that "nonaxiality should not exceed that which will produce a difference of 15 percent in elastic strain readings on opposite sides of the specimen when an extensometer is positioned to measure the maximum effect of nonaxiality." This limit corresponds to 7½ percent bending at midlength. The task group found little information available to answer the question of whether the allowable bending should be increased or decreased. The first part of this discussion will be directed toward this question.

Table 1 shows that on the average each type of machine met the ASTM specifications. On the other hand, in no case did all the machines of any one type meet the specifications. Another complicating factor is that both the type A and type B machines were tested for axiality at stresses higher than those used in normal creep and rupture testing. Figure 3 shows that at lower stresses the percent bending probably will be higher than the values shown in Table 1. Nevertheless, the number of machines meeting the specifications indicates that the 7½ percent bending limit is well chosen and not unduly restrictive. An exception to this generalization may be desirable in the case of tests over 1800 F (982 C), where cast couplings of difficult to machine metals are commonly used. These couplings were used with the type A machines. In this case 10 percent allowable bending at the midlength might be more practical.

Another question is whether the maximum bending which occurs near the ends of the gage length is not a more important variable than the average value which occurs at midlength. Table 1 shows clearly that these usually are different values, the maximum value being about twice the average value. The experimental studies found in the literature report only the average values [*3*, *4*, *5*]. The analytical studies [*3*, *5*, *6*, *7*] deal only with the case of bending in the same direction and of equal magnitude at both ends of the reduced portion. Millgren [*7*] mentions the general case of any orientation between the specimen axis and line of action of the force and calls it "nonmeasurable eccentricity." This lack of evidence as to the effect of nonuniform bending, combined with the practical difficulty of measuring and correcting the bending at two longitudinal positions, indicates that the question should be put aside temporarily. The question is raised here to emphasize the need for more experimental and analytical work on the effect of nonuniform bending on creep and rupture test results.

The comparison in Table 1 of tests with the crossed knife edge connectors to tests with the eye and clevis connectors indicates that the type of connector has little effect on bending. The ranking comparison in Table 2 indicates that the size of the machine contribution has little effect on the specimen bending. Table 5 indicates that the couplings were the dominant contributors to bending during the tests using type A machines. Therefore, it appears that the added expense of crossed knife edge connectors is not justified when testing with ordinary couplings with cast threads, finished by tapping. On the other hand, Table 2 shows the superiority of the crossed knife edge connectors when used with a load string without threads near the specimen. Jones and Brown [4] have shown that tightly fitted, precisely machined threaded joints contribute little to eccentricity; therefore, the superior axiality of crossed knife edge and ball-type connectors [4] can be utilized to advantage when a precisely machined, tightly fitted load string is used.

Figures 1 and 2 afford further evidence of the dominant role of the couplings in determining the amount of bending. Since repeated loading of the one-piece load string produced little differences in strain, the substantial differences shown in these two figures for normal load strings appear to be due to the presence of the couplings. It is interesting to note the similarity in the differences in the magnitude and the differences in the direction when the three types of machines are compared—type CR has the smallest differences between successive loadings, type B is intermediate, and type A has the greatest differences.

The average and maximum bending values in Table 2 rank the types in the same order and almost in the same proportions. This is again consistent with the finding that the coupling is the major contributor to bending as well as the major contributor to the differences in successive measurements. Table 1 shows that the rated temperature of operation follows the same ranking, the machine with the lowest temperature rating having the least bending and least difference in bending between successive loadings. The coupling is the major difference between machines with different temperature ratings. The highest temperature tests are made with couplings with cast threads, which have been finished by tapping with different size threads for the specimen and the load bar. The intermediate temperature tests use forged couplings with turned bores but with different size threads. The lowest temperature tests are made with forged couplings having a single thread for both specimen and load bar. These construction features would lead one to expect that the misalignment of the specimen thread and the load bar thread would vary as the temperature rating.

Because loose threads allow a greater range of chance misalignment, it would be expected that the calculated value for specimen contribution would vary as the degree of looseness. Contrary to this expectation, specimens 2 and 3 gave similar results. This is thought to be due to a loose fit of the couplings on the load bars, whose random factor masks the effect of the specimen-to-coupling fit.

The reported bending for a given machine contains a component due to error in strain reading and another due to inaccuracies in specimen machining. In some cases these may be large enough to mask the variable being investigated. For example, the contributions to bending due to gage and machining inaccuracies in the one-piece load string were several times as great as the apparatus contribution in the case of the crossed knife edge connectors. In general it seems advisable to isolate the specimen contribution before drawing conclusions regarding the apparatus.

Production of the one-piece load string requires a difficult machining operation in that a slender section in the center of a long bar must be made coaxial with the threaded ends. We have not made enough of these to judge whether this one is representative, but the observed bending can be caused by a 0.003-in. (0.08-mm) eccentricity [4], which might be expected to occur at the center of the 42-in. (1.1-m) rod. On the other hand, normal specimens with turned or ground threads are believed to contribute little to the measured eccentricity. This belief is based on the observation that, when the calculated value is reproducible, as for specimen 1 in Table 5, it is less than 1 percent. Conversely, when the calculated value is large it is not reproducible. The large values are thought to be due to differences in the alignment of the specimen axis with the coupling axis during the two loadings required by the calculation.

Even a perfectly machined specimen may contribute to the observed bending due to errors in measuring strain. An estimate of these errors can be obtained readily from quantities such as those listed in Table 4. These estimates indicate that the gaging error normally will be less than 10^{-5}, or the equivalent of ½ percent bending. Chance combinations of erroneous gages can prevent the identification of large errors by comparisons such as those in Table 4. For example, a similarly defective gage in each opposite pair will give an equal sum of opposite strains making that check ineffective. In the same way, equal numbers of equally defective gages in each set of four will give the same apparent tensile strain at each longitudinal position making that check also ineffective. To detect the occurrences of these chance combinations of defective gages, the specimen contribution and gage calibration factors can be obtained by turning the specimen 180 deg between loadings. However, this will give a reproducible, accurate value only if the joints in the load string are machined accurately and fitted tightly. The most reliable and accurate way of estimating the gage errors is to make a calibration by testing the specimen in bending only, as was done for specimen 5. This does not give an absolute calibration such as would be required for measurement of the modulus of elasticity, but it is adequate for axiality measurements, which require only a calibration of each gage relative to the others in the set of four.

The numbers on the average curve in Fig. 3 show that the percent bending decreases as the tensile stress increases. These numbers when plotted versus stress give a curve closely similar in shape to those previously published for commercial machines [4, 7]. It has been shown [5] that this shape can be obtained by calculation for the case of a specimen whose axis initially is displaced parallel to the axis of the load string. Except for machine A-77 the other curves shown in Fig. 3 also would, when converted to percent bending, give shapes similar to the average curve. In contrast, machine A-77 would have a percent bending which increased with stress. This anomaly, as well as other variations in the forms of curves in Fig. 3, is thought to be due to the interaction of several sources of bending which can be in canceling directions and can be dependent on force in different ways.

Conclusions

1. On the average, each of the three types of commercial machines tested induce bending stresses within the ASTM recommended limits when tested at high stresses.
2. The ASTM recommended limit on bending is well chosen in the sense that it is attainable by good quality commercial machines yet provides a stimulating challenge.
3. The higher the rated temperature of the load string, the greater the average percent bending and the larger the difference between successive tests.
4. Imperfections, including looseness, in the couplings are the most important source of bending stress during tests.
5. Simple load bar connectors may be used with couplings with cast threads without significantly increasing bending in the specimen.

References

[1] McVetty, P. G., *Proceedings*, American Society for Testing Materials, ASTEA, Vol. 28, 1928, pp. 60–79.
[2] Morrison, J. L. M., *Proceedings*, Institution of Mechanical Engineers, PIMLA, Vol. 142, Nov. 1939-March 1940, pp. 193–223.
[3] Isaksson, A., Bulletin 110, *Transactions of the Royal Institute of Technology*, TRITA, Stockholm, Sweden, 1957, pp. 29–31.
[4] Jones, M. H. and Brown, W. F., Jr., *Bulletin*, American Society for Testing Materials, ASTBA, Jan. 1956, pp. 53–60.
[5] Penny, R. K., Ellison, E. G., and Webster, G. A., *Materials Research and Standards*, MTRSA, Vol. 6, No. 4, Feb. 1966, pp. 76–84.
[6] Penny, R. K. and Leckie, F. A., *International Journal of Mechanical Sciences*, IMSCA, Vol. 10, 1968, pp. 265–273.
[7] Mellgren, A., "Measuring Accuracy in Creep Tests," Publication 125 (in English), Kimgl. Tekniska Högskolan, Stockholm, 1958.

DISCUSSION

R. J. O'Kane[1]—The Schmieder paper offers results on a number of creep machines sufficient to establish a measure of the scatter of such data. One way to achieve optimum performance is to reduce friction between the testing machine head and the load train coupling.

A marked improvement in alignment over a simple spherical seat like that shown in ASTM Specifications for Tension Testing of Metallic Materials (E 8 - 69) was achieved by Jones and Brown[2] by positioning a ball between two parts of a loading yoke. Recent tests by Satec Systems (Figs. 1 and 2) indicate that an alignment device with crossed knife edges (Fig. 3) can further reduce specimen bending, especially at low levels of loading. In these figures, percent bending refers to the ratio of the difference between maximum and minimum longitudinal surface stress on the specimen and the average axial stress, which bending increases directly with the eccentricity of load application. All specimens had a gage length of 2 in., but specimen diameters were 0.505 and 0.252 in. for the data of Figs. 1 and 2, respectively. In all instances, bending was measured by resistance strain gages mounted on the gage section.

To consistently achieve less than 10 percent bending at loads of 400 lb or greater, all elements of the load train, including the threaded end specimen, were machined with tolerances not greater than 0.0005 in. Care also was taken to ensure that the head of the load train pull rod was seated firmly in the coupling and that there were no burrs. Our conclusion that the crossed knife edge can reduce significantly the amount of misalignment contributed by the machine was supported by tests run purposely with the lower coupling displaced 1 in. from the center position. In no case was a significant change detected in the amount of bending. In one tester a special crossed knife edge alignment coupling was mounted directly to the specimen, achieving a mere 3 percent bending at a load of only 17 lb.

Present ASTM Specifications E 21 and E 139 recognize that different tests may have quite different percent bending strain due to chance orientation of a loosely fitted specimen. To assure proper alignment of each and every specimen requires that the percent bending be determined in place on the actual specimen prior to testing and that the test then be

[1] Vice president for sales, SATEC Systems, Inc., Grove City, Pa.

[2] Jones, M. H. and Brown, W. F., Jr., *ASTM Bulletin*, ASTBA, Jan. 1956, pp. 53–60.

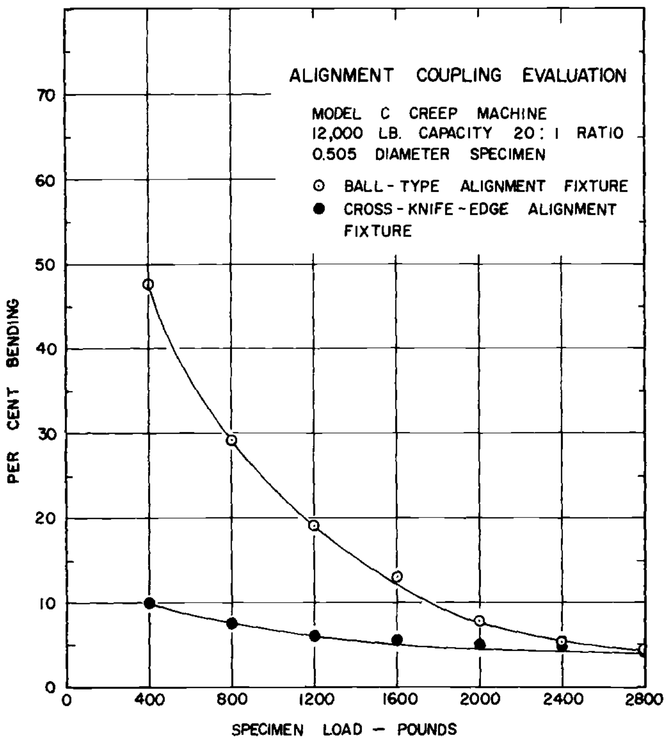
ALIGNMENT COUPLING EVALUATION
MODEL C CREEP MACHINE
12,000 LB. CAPACITY 20 : 1 RATIO
0.505 DIAMETER SPECIMEN
BALL - TYPE ALIGNMENT FIXTURE
CROSS - KNIFE - EDGE ALIGNMENT FIXTURE
PER CENT BENDING
SPECIMEN LOAD — POUNDS
0
10
20
30
40
50
60
70
400
800
1200
1600
2000
2400
2800

FIG. 1

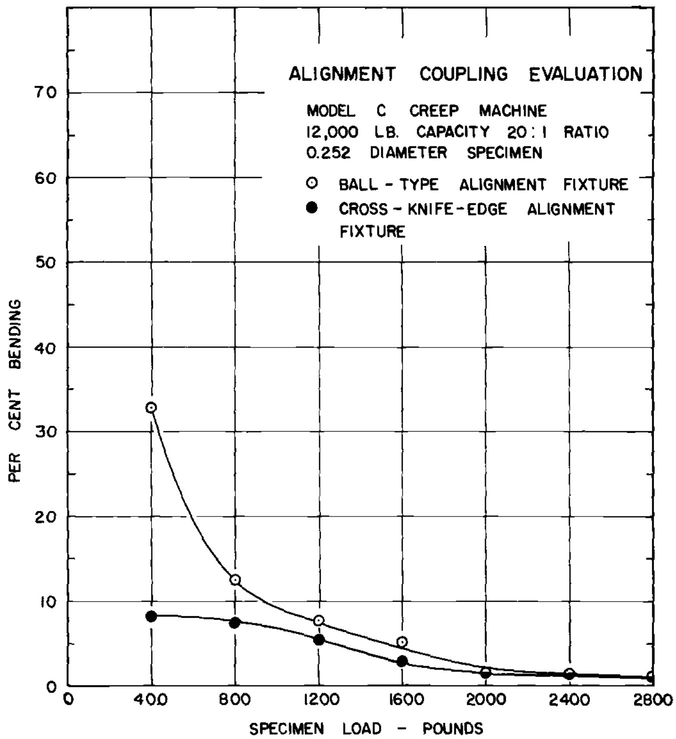
ALIGNMENT COUPLING EVALUATION
MODEL C CREEP MACHINE
12,000 LB. CAPACITY 20:1 RATIO
0.252 DIAMETER SPECIMEN
BALL - TYPE ALIGNMENT FIXTURE
CROSS - KNIFE-EDGE ALIGNMENT FIXTURE
PER CENT BENDING
0
10
20
30
40
50
60
70
0
400
800
1200
1600
2000
2400
2800
SPECIMEN LOAD - POUNDS

FIG. 2

conducted without further manipulation of the load train. A device has been developed which can be mounted onto the specimen to measure the percent bending and then removed without disturbing the alignment. Such devices can shed light on the significance of percent bending in tests under axial load and may lead to more meaningful future specification of alignment requirements.

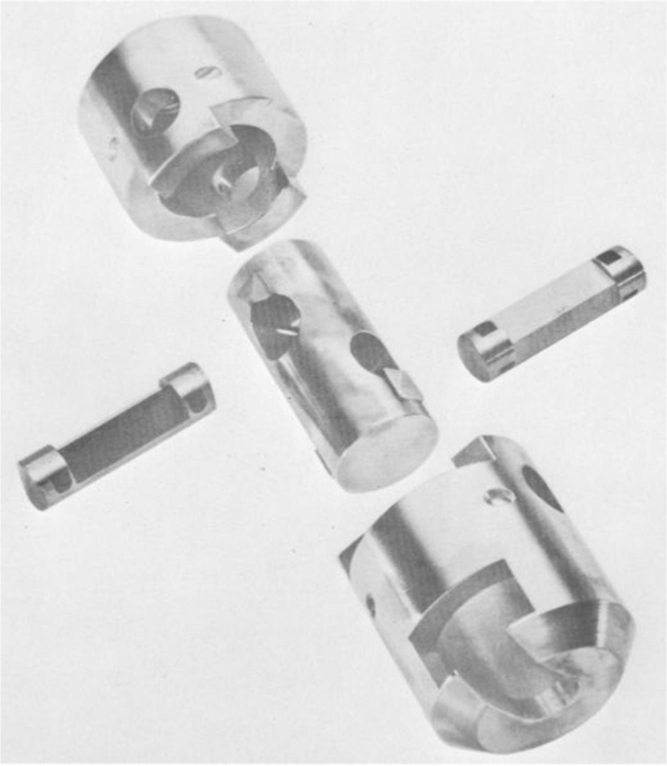

FIG. 3

H. R. Voorhees[1]

Apparent Lowering of Creep Rupture Life by Frequent Beam Leveling

REFERENCE: Voorhees, H. R., "**Apparent Lowering of Creep Rupture Life by Frequent Beam Leveling,**" *Elevated Temperature Testing Problem Areas, ASTM STP 488*, American Society for Testing and Materials, 1971, pp. 65–70.

ABSTRACT: Limited test results reported here and elsewhere suggest that frequent beam leveling may reduce observed rupture life under conditions where the specimen ductility is high. The magnitude of this effect is small compared to the influence of other factors.

KEY WORDS: creep properties, creep rupture strength, ductility, loads (forces), beams (supports), static loads, leveling, automatic control, fractures (materials), aluminum alloys, stainless steels

The possible influence of beam leveling on rupture time first came to mind when results were being examined from calibration tests of billets of Type 304 stainless steel from the ASTM specimen bank material. That steel had been carefully melted and rolled under direction of the Joint Committee on Effect of Temperature on the Properties of Metals to provide material with uniform properties. Four laboratories with long experience in creep rupture testing have performed the calibrations of all five billets of this steel offered to date through ASTM. Each uses its best practice; in all cases the specimen is heated overnight to 1325 F, then brought to 1350 F, and held 1 h at that temperature before the load is applied.

One of the four laboratories obtained essentially identical results for all five billets, with the average rupture times under 13,500-psi load ranging from 103 to 116 h for the different lots. In 1962 the overall average of results from the three remaining laboratories slightly exceeded those of the first, but 1968 found the pattern reversed. The only apparent procedural change during the 6-year interim was the introduction in two of the three laboratories of some testers with automatic beam leveling.

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The time period covered by this series of calibration tests admits possible unknown variations in specimen preparation or other details which could affect the results independently of the exact test machines used. Therefore, other data were sought for more reliable evidence.

The first applicable results found were from two pairs of tests on annealed aluminum alloy 1100, obtained in research sponsored by the Metal Properties Council (their Order 168-6). In each instance, one test was run under direct dead load; the other in a tester with frequent automatic leveling of the load beam.

Referring to the creep curves, Figs. 1 and 2, for either a 3500-psi load at 350 F or a 3000-psi load at 400 F, one sees that the initial creep was scarcely affected by beam leveling. If a difference did exist, the creep rate was faster under the direct load. However, at a later stage, where beam take-up became more frequent, the curve for the test with automatic beam leveling rose noticeably above the other curve. For the respective test conditions cited above, rupture life with frequent beam leveling was only 71 and 76 percent of the life under a dead load giving the same nominal stress.

Next, stainless steel from an uncalibrated billet of the ASTM specimen bank stock was tested at the calibration conditions (13,500 psi at 1350 F). The same individual tester (of well known American make) was used for all four tests, but for two of the tests the automatic leveling device was turned off and the beam was leveled manually only often enough to keep the end of the beam within about 1½ in. of its level position (prior calibration checks showed the lever ratio to stay well within 1 percent

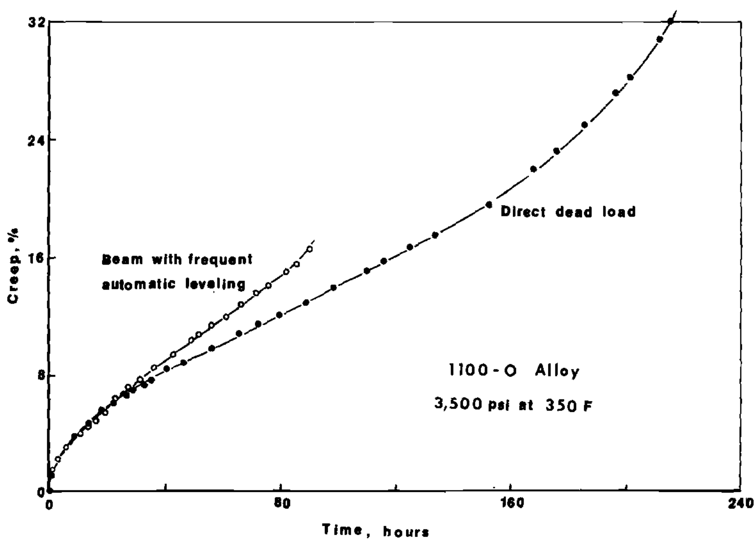

FIG. 1—*Creep curves for 1100-0 aluminum alloy, 3500 psi at 350 F.*

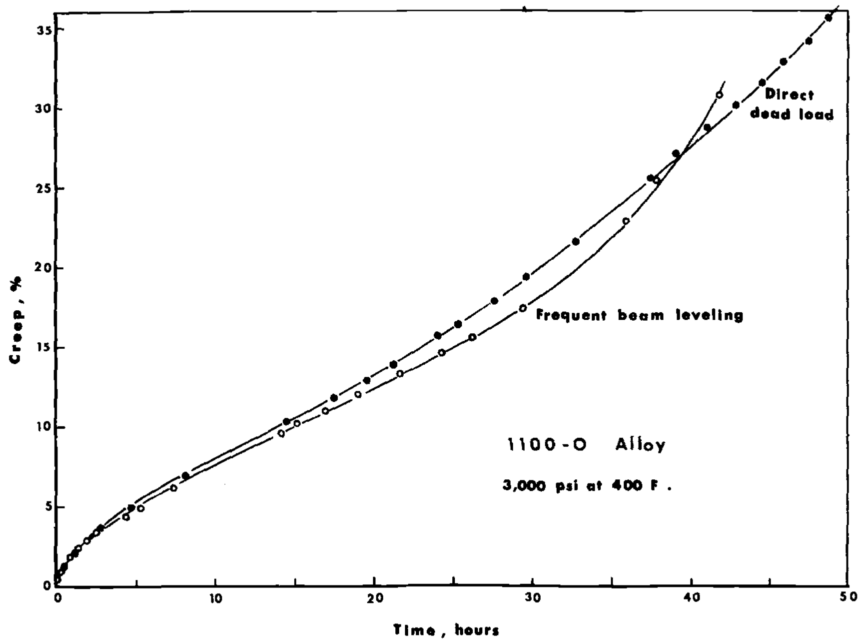

FIG. 2—*Creep curves for 1100-0 aluminum alloy, 3000 psi at 400 F.*

variation from that in the level position through this amount of movement). Under automatic control, the mechanism responded to a sufficiently small creep increment that the take-up device operated several hundred times before the specimen fractured.

Rupture life with automatic beam leveling (96.4 and 81.6 h) averaged 92 percent of those with four and five manual take-ups, respectively (93.4 and 100.0 h). Four other specimen blanks from this same billet were supplied to A. K. Schmieder for tests under more exact control and with determination of frequency of the beam leveling (see Discussion following).

A final, known set of data was that obtained by laboratory 10 of a cooperative creep testing program on Nimonic 105 alloy, set up by the AGARD Structures and Materials Panel. (The final report on that study is expected to be released about mid 1971.) Pairs of tests at 900 C (1652 F) under each of three stress levels were run in a tester of European make which features automatic beam leveling. Other pairs of tests at two other stress levels used a differently designed machine without automatic take-up.

The most meaningful comparison appears to be the ratio of rupture life obtained by laboratory 10 to the overall average of the five laboratories which performed this entire series of tests. For the conditions at which laboratory 10 used automatic leveling, its six tests had lives which averaged some 10 percent above the results for all laboratories; with manual beam leveling, its four results were only about 5 percent above the overall average at the stress levels involved.

These particular findings suggest that automatic beam leveling does not shorten rupture life and, perhaps, may even prolong the life. Note must be taken, however, that the rupture elongation for these tests was moderate (8 to 25 percent) compared to the elongations obtained with Type 304 stainless steel at 1350 F or for 1100-0 aluminum alloy at 300 or 400 F, so fewer beam levelings would be involved in a test.

Viewed as a whole, available results suggest that frequent automatic beam leveling may, indeed, reduce perceptibly observed rupture life under conditions where specimen ductility is high. But, the magnitude of this effect is small and probably of much less concern than other factors considered in the present symposium. Suppliers of test machines should perhaps look to the practicality of using a Geneva mechanism or some other device to reduce acceleration of the beam during a periodic take-up.

DISCUSSION

A. K. Schmieder[1]—This comparison was proposed by H. R. Voorhees and sponsored by the American Society for Testing and Materials. The specimen material was Type 304 stainless steel, billet 6C804-T3, from the ASTM stock called E139, Standard Unmachined Specimens for Calibrating Creep Testing Machines. The furnished blank was quartered by two longitudinal cuts. From each quarter a threaded specimen was machined with a reduced portion 0.253 in. in diameter and 1.25 in. in length.

The same creep machine was used to test all four specimens. The machine ordinarily is used with a power drive on the lower draw bar to level the lever. A switch on the lever actuates the motor whenever the lever leaves the horizontal position. The loading weights at the end of the 16:1 lever move 0.58 in./s when the motor is energized continuously; however, in normal testing, the motor is energized for a period much less than 1 s during each leveling operation. Two specimens were tested with the machine in its ordinary condition. An events recorder was used to mark a record whenever a leveling operation occurred.

For tests on the remaining two specimens the machine was modified in two ways. First, the motor was disconnected and a hand crank substituted. With this hand crank the loading weights were moved to the upper limit of their travel whenever the lower limit was approached owing to extension of the specimen. During manual leveling the crank was turned at approximately 1 rps, resulting in a velocity of the loading weights of 0.02 in./s.

Before the tests the machine was calibrated with a proving ring at the force used for these tests in order to establish the permissible range of motion of the loading weights. It was found that for a 4-in. range of downward motion of the weights the force varied smoothly from 100.8 to 99.3 percent of the nominal force (loading weight times 16). The second machine modification was made to reduce this variation. A weight of 4 lb was attached to the lever so that its center of gravity was 7.6 in. directly above the support fulcum with the loading weights at midrange. After this modification the variation in force during 4 in. of motion of the loading weights was less than 0.1 percent from the nominal value.

To measure the shock loading due to automatic leveling a wire resistance strain gage was attached to a specimen similar to those rupture tested.

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The gaged specimen was loaded at room temperature to the same stress as the rupture specimens. After loading, strain was recorded on an instrument with a linear response to 40 cps. Whether or not the leveling motor was operating, a vibratory strain was recorded whenever the specimen was loaded. The rough sawtooth record showed about 3 peaks per second when the motor was not running and about 1.5 peaks per second when the weights were being raised. The corresponding stress amplitudes were 1 and 1.5 percent of the applied stress. The first peak after the motor was started was of about the same height as later peaks, indicating that no measurable shock loading occurred owing to starting of the leveling motor.

The heating and loading procedure for the four rupture specimens was the same as that recommended when using specimens from the same source for machine calibrations, that is,

1. Hold overnight at 1325 F.
2. Raise to 1350 F and hold 1 h before loading.
3. Load to 13,500 psi.

The test results are

Specimen Number	Rupture Time, h	Elongation,[a] %	Reduction in Area, %	Number of Times Level Releveled
1............	107.9	44.0	46.0	2 (manual)
2............	116.0	44.3	45.0	2 (manual)
3............	109.0	47.6	42.0	143 (automatic)
4............	113.7	43.0	45.0	127 (automatic)

[a] Change in overall length divided by length of reduced portion between fillet tangent points.

These values show no significant difference in the results due to type of lever leveling.

H. R. Voorhees (author's closure)—Mr. Schmieder's tests provide a valuable addition to this study, particularly so because the steel tested and the procedures followed were intended to be identical. His results, like ours, show the effect of variation in type of beam leveling to be smaller than the scatter between some pairs of tests with the same beam leveling practice.

Perhaps of more interest is the fact that all of Mr. Schmieder's rupture times exceeded the longest time obtained in our four tests; his rupture times averaged to a value roughly 1.2 times as great as our average. This finding clearly reinforces the conclusion that other factors are more critical to test results than the type of beam leveling applied.

J. L. Korns[1]

Interlaboratory Program to Evaluate Present Pyrometric Practices in Elevated Temperature Testing

REFERENCE: Korns, J. L., "**Interlaboratory Program to Evaluate Present Pyrometric Practices in Elevated Temperature Testing,**" *Elevated Temperature Testing Problem Areas, ASTM STP 488*, American Society for Testing and Materials, 1971, pp. 71–78.

ABSTRACT: An interlaboratory program was organized by members of the ASTM-ASME Joint Committee on Effect of Temperature on the Properties of Metals, Subcommittee on Test Methods, to determine the consistency of temperature measurement that exists among laboratories conducting elevated temperature testing. An evaluation was made of one phase of the pyrometric practice applied, comparison of the temperature at the center of a tension specimen measured with a common reference thermocouple with the temperature of its surface using the method of thermocouple manufacture and attachment utilized by each participating laboratory. In order to simulate the normal heat losses experienced through conduction during elevated temperature testing, a typical specimen train was included. Additional clad Chromel-Alumel thermocouples, taken from a section of calibrated wire, also were provided for one time use only.

Each of the participating laboratories attached their thermocouples to the specimen and adjusted the temperature controller so that the attached thermocouples indicated exactly 1200 F (922 K). The emf of the common reference thermocouple then was measured using the same reference junction utilized in their normal practice. This procedure was then repeated using the calibrated thermocouples exposed for only one measurement.

The information that has been developed by the seven participating laboratories to date has shown more variation than was expected. The total range of reported temperatures for the common thermocouple was 11 F (6.1 K) and 18 F (10 K) for the single-use thermocouples.

KEY WORDS: temperature measurement, temperature measuring instruments, high temperature tests, tension tests, thermocouples, thermocouple pyrometers, nickel alloys, nickel-chromium alloys, stainless steels

[1] Metallurgist, The Greer Steel Co., Dover, Ohio 44622.

At the present time there are no established ASTM requirements controlling pyrometric practices to assure consistency of temperature measurement among laboratories conducting elevated temperature testing. The requirements that presently are placed on elevated temperature testing, ASTM Recommended Practices for Short-Time Elevated Temperature Tests of Materials and for Conducting Creep and Time-for-Rupture Tension Tests of Materials (E 21 - 66T and E 139 - 66T, respectively), are concerned with

(*a*) the limits of the temperature variation of the specimen from the indicated nominal test temperature, namely, ±3 F (1.7 K) up to and including 1800 F (1255 K) and ±5 F (2.8 K) above 1800 F (1255 K), and

(*b*) the limits of the indicated temperature variations along the gage length of the specimen, namely, ±5 F (2.8 K) up to and including 1800 F (1255 K) and ±10 F (5.6 K) above 1800 F (1255 K) for ASTM E 21 and ±3 F (1.7 K) up to and including 1800 F (1255 K) and ±5 F (2.8 K) above 1800 F (1255 K) for ASTM E 139.

It is recognized that the true temperature can vary more than the indicated temperature; consequently, all laboratories are obligated to keep this variation as small as is practical. ASTM offers only an awareness that good pyrometric practice is required to limit this variation to a negligible difference. Therefore, because of the importance of good pyrometric practice, it was felt by members of the ASTM-ASME Joint Committee on Effect of Temperature on the Properties of Metals, Subcommittee on Test Methods, that a program for evaluating the consistency of temperature measurement among laboratories conducting elevated temperature testing would provide some measure of the quality of the pyrometric practices now being applied.

Scope of the Interlaboratory Program

The task group appointed by the subcommittee was well aware of the difficulties involved in evaluating the pyrometric practices now being used by different laboratories and of the involvement necessary to establish the difference between indicated temperature and true temperature. Therefore, it was decided that the first approach should be to evaluate the present consistency of temperature measurement that exists among laboratories. This was accomplished by comparing the temperature at the center of a specimen, as determined by a reference thermocouple, and the temperature of the surface of the specimen, as determined by thermocouples manufactured and attached according to each laboratory's standard pyrometric practice. The task group recognized that many other potential sources of error in temperature measurement exist but decided to concentrate first on the errors associated with thermocouple manufacture and attachment.

The comparison was made at 1200 F (922 K) and was accomplished by using a specimen train comprised of threaded extension bars and grips and a standard 0.500 ± 0.010-in. (12.5-mm), round, Type 304 stainless steel tension specimen. The tension specimen contained a 0.130-in. (3.3-mm) axial hole drilled to the exact center of its gage length. One of the extension bars also contained a hole allowing the 0.125-in. (3.2-mm), clad Chromel-Alumel reference thermocouple to be inserted into the exact center of the tension specimen. Each of the laboratories then could produce and attach their thermocouples to the surface of the tension specimen, enabling comparable temperature readings to be made. Additional clad Chromel-Alumel thermocouples to be used only once also were supplied to the laboratories which participated. The additional thermocouples were included in the program to detect any deterioration of the reference thermocouple resulting from different immersion depths between laboratories and the thermal cycling required by the program itself.

Procedure for Interlaboratory Comparison

Each of the participating laboratories was asked to instrument the standard specimen with thermocouples according to their customary pyrometric practice. They were then to use the attached thermocouples to adjust the temperature of the standard specimen to exactly 1200 F (922 K). Tests were conducted, first, using the reference thermocouple provided for use by all laboratories and, second, using a new, single-use thermocouple also provided. The thermocouples for single use were all made from the same length of clad Chromel-Alumel wire and had been numbered in sequence according to position in the original wire.

The reference thermocouple for testing in common was inserted into the central hole until it contacted the bottom. A single set of connectors and a length of Chromel-Alumel lead wire were provided for connecting both the reference thermocouple and the surface thermocouples to their potentiometers. The same reference junction which was used to adjust the standard specimen to 1200 F (922 K) also was used for the reference thermocouple. One hour or longer, if necessary, was allowed for the standard specimen to reach thermal equilibrium. When each laboratory's thermocouple system indicated the standard specimen surface was exactly 1200 F (922 K), the emf output from the common reference thermocouple was measured. The thermocouple emf output then was reported on the basis of a reference junction temperature of 32 F (273 K). The procedure was duplicated for the single-use thermocouple selected from those provided.

Reproducibility of Reference Thermocouple

A procedure was devised to establish the stability of the reference thermocouple and to evaluate its reproducibility during the program. This procedure consisted of a comparison between the recognized temperature for the freeze point of pure aluminum and the indicated temperature of the reference thermocouple at this fixed temperature. By comparing these indicated temperatures before the interlaboratory program had begun and after its completion, an indication of the stability of the reference thermocouple could be determined and any change detected.

The purity of the aluminum used was 99.967 percent and the freeze point was 1220.6 F (933.3 K). The aluminum was placed in a graphite crucible and melted down in a typical stress rupture furnace. The standard 0.500 ± 0.010-in. (12.5-mm), round, Type 304 stainless steel tension specimen was placed in a second graphite crucible to protect the stainless steel specimen from the molten aluminum. The reference thermocouple then was inserted into the tension specimen so that it rested on the bottom of the hole. After the aluminum became molten, the second crucible containing the tension specimen and thermocouple was placed in the aluminum. Sufficient time was allowed to enable the system to reach thermal equilibrium at approximately 1240 F (944 K). The power to the stress rupture furnace then was shut off, and the cooling rate of the aluminum was followed using an 8686 Leeds and Northrup calibrated potentiometer with an ice bath reference junction. When an arrest was detected in the cooling rate, indicating the freeze point, the reference thermocouple indicated a temperature of 1219 F (932.4 K). The reliability of this thermocouple was checked by repeating several freeze point comparisons. These additional comparisons duplicated the 1219 F (932.4 K) temperature.

Calibration of Single-Use Thermocouples

A total of 25 thermocouples, each slightly over 20 in. long and numbered in sequence from 1 to 25, were manufactured by the Claude S. Gordon Co. from a single length of sheathed wires. Thermocouples 2, 13, and 24 then were calibrated by the Hoskins Manufacturing Co., who had made the original wire but were not the manufacturer of the thermocouples. This calibration was made over a temperature range from 200 F (366 K) to 2000 F (1366 K). The results of this calibration are listed in Table 1.

Discussion of Results

The results submitted by the seven laboratories participating in the program are reported in Table 2. The data have been compared using the difference between the thermocouples attached to the specimen by each participant and the two reference thermocouples. This method of comparison was used because no attempt was made to relate our observations to any absolute temperature standard other than the individual practice applied at each laboratory. Since these practices vary, the only meaningful comparison was the observed differences of the reference thermocouples.

TABLE 1—*Calibration results determined by Hoskins Manufacturing Co. for single-use Chromel-Alumel thermocouples.*

Thermocouple	Calibration Temperature, deg F	Error, deg F	emf, mV		
			32 F Reference	Standard Curve	Error
2	200	0	3.823	3.820	+0.003
	300	+0.5	7.006	6.091	+0.015
	400	+0.25	8.318	8.312	+0.006
	500	−0.5	10.551	10.563	−0.014
	1000	+0.5	22.270	22.255	+0.015
	1200	+3.0	27.052	26.985	+0.067
	1600	+3.0	36.265	36.195	+0.070
	2000	+2.5	44.960	44.909	+0.051
13	200	0	3.821	...	+0.001
	300	0	6.091	...	0
	400	−0.5	8.300	...	−0.012
	500	−1.5	10.531	...	−0.034
	1000	0	22.255	...	0
	1200	+3.0	27.053	...	+0.068
	1600	+3.0	36.260	...	+0.065
	2000	+2.5	44.960	...	+0.051
24	200	+0.25	3.826	...	+0.006
	300	+0.5	7.007	...	+0.016
	400	+0.25	8.318	...	+0.006
	500	−0.5	10.550	...	−0.015
	1000	+1.0	22.281	...	+0.026
	1200	+3.0	27.049	...	+0.064
	1600	+3.0	36.260	...	+0.065
	2000	+3.0	44.970	...	+0.061

McCausey[2] has shown the temperature measured at the surface of the specimen normally is lower than that at the center of the specimen when the depth of reference thermocouple immersion is on the order of 1.25 in. (31.8 mm). He also has shown that the magnitude of this difference is influenced by the depth of immersion in the isothermal zone. The attempt to calibrate the common reference thermocouple certainly was influenced by shallow immersion, and the result is that the technique can be used only to detect a change in this reference. We do not feel that the depth of immersion had a significant effect on the observed differences between laboratories, because the total variation in distance from specimen center to furnace top was 6 in. (15.2 cm) to 9 in. (22.7 cm) and no apparent correlation could be made between the magnitude or direction of temperature

[2] McCausey, R. J., The Detroit Edison Company, Detroit, Mich., "A Critical Examination of the Temperature Measurements of the ASTM-ASME Round Robin on Temperature Measurements in Creep and Stress-Rupture Testing," Appendix A of the Minutes of the 11 Dec. 1969 meeting of ASTM Committee E-20, Subcommittee IV, Section 6, Thermocouple Applications, Cincinnati, Ohio.

TABLE 2—*Reported millivolt readings and corresponding temperatures for the common reference and single-use thermocouples.*

Lab Code	Reported Common Reference Thermocouple Reading, mV	Corresponding Temperature, deg F	Reported Single-Use Thermocouple Reading, mV	Corresponding Temperature, deg F	Corrected Temperature, deg F	Difference between Columns 2 and 5, deg F	Difference between Column 2 and Average of Column 2	Difference between Column 5 and Average of Column 5
C	26.910	1197.0	26.740	1190.0	1187.0	10.0	5.1	10.3
J	27.035	1202.0	27.050	1203.0	1200.0	2.0	0.1	2.7
N	27.030	1202.0	26.995	1200.5	1197.5	4.5	0.1	0.2
O	26.960	1199.0	26.910	1197.0	1194.0	5.0	3.1	3.3
P	27.170	1208.0	26.170	1208.0	1205.0	3.0	5.9	7.7
R	27.070	1203.5	27.033	1202.0	1199.0	4.5	1.4	1.7
V	27.070	1203.5	27.040	1202.0	1199.0	4.5	1.4	1.7
Average	...	1202.1	...	...	1197.3	4.8	2.4	3.9

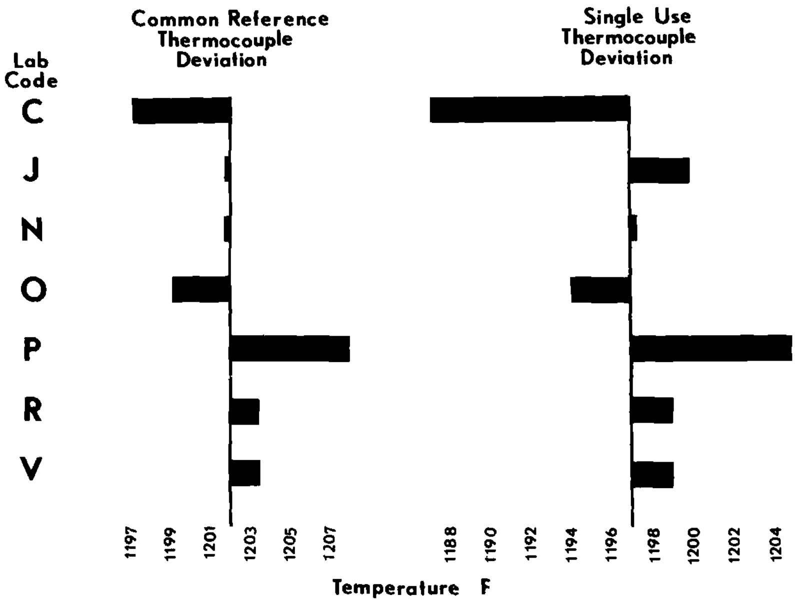

FIG. 1—*Temperature deviation of reference thermocouples from their average.*

difference and the depth of immersion. This is probably because the depth of the isothermal zone of the specimen is not related directly to the total depth of immersion in the furnace but depends on how the furnace is wound and how the specimen is insulated at the top.

The total range of observed differences for the seven participating laboratories between attached thermocouples and the common reference thermocouple was 11 F (6.1 K). The total range based on single-use reference thermocouples was 18 F (10 K). The indicated temperature, shown as deviation from the mean of all laboratories, is demonstrated by the bar graphs in Fig. 1. The agreement in the deviation from the mean between the single-use and multiple-use reference thermocouples is remarkably good. These data indicate reproducibility in the observed differences and show no gross deterioration in the common reference thermocouple.

It is encouraging to note that the average deviation from the mean temperature indicated by the multiple-use reference was only about 2.4 F (1.3 K). The average deviation for single-use references was 3.9 F (2.2 K).

Conclusions

1. The results of this interlaboratory program cannot be related to an absolute temperature scale, since each laboratory essentially determined

the indicated test temperature which may be influenced by variations in standard practice and equipment.

2. The program did demonstrate successfully that a significant difference can exist between laboratories when comparing the temperature of a common reference at the center of the specimen with the temperature measured by attached thermocouples at the surface.

3. It is my opinion that a uniform recommended pyrometric practice be included in ASTM Practices E 139 and E 21.

H. R. Voorhees[1]

Effect of Thermocouple Drift on Rupture Life at High Temperature

REFERENCE: Voorhees, H. R., "**Effect of Thermocouple Drift on Rupture Life at High Temperature,**" *Elevated Temperature Testing Problem Areas, ASTM STP 488,* American Society for Testing and Materials, 1971, pp. 79–81.

ABSTRACT: Rupture life of Waspaloy in four tests under 9050-psi stress at 1700 F nominal temperature tended to be longer when temperature was adjusted in accordance with the reading of a noble metal thermocouple than when Type K base metal thermocouples were used. Drift of the reading of the base metal thermocouples relative to that of an adjacent noble metal thermocouple did not exceed 3 F in tests of nearly 400-h duration.

KEY WORDS: temperature measurement, thermocouples, drift (instrumentation), accuracy, base metals, precious metals, creep tests, creep rupture strength, tension tests

As D. K. Faurschou already has pointed out,[2] cooperative studies sponsored by AGARD appear to have uncovered a statistically significant difference in rupture times found for Nimonic 105 at 900 C (1652 F), according to the type of thermocouple used. Laboratories using Type K base metal thermocouples, as a group, reported rupture times longer than the group using noble metal thermocouples.

Continuing AGARD tests seek to clarify the situation. Hopes had been that the portion of these tests being conducted by J. W. Freeman at the University of Michigan would be available now, but unforeseen delays have developed. Therefore, only findings from a few of my preliminary tests will be given here.

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[2] See AGARD preliminary report, pp. 3–14 this volume.

Under arrangements with Prof. Freeman, a piece of Waspaloy stock, in the form of a 2 1/8-in. round-corner square, was supplied by E. E. Renolds of Latrobe Steel Co., Latrobe, Pa. The hot rolled material was given the following conventional heat treatment:

(a) 1975 F, 4 h, water quench;

(b) 1550 F, 4 h, air cool; and

(c) 1400 F, 16 h, air cool.

A 4-in. length then was quartered, and each quarter was machined into a specimen having a gage section about 0.3 in. in diameter. Tests to rupture were made, all under 9050-psi nominal stress at a nominal temperature of 1700 F.

The bare bead of a thermocouple made from 18-gage Chromel and Alumel wires purchased from Hoskins Manufacturing Co., Detroit, Mich., was tied firmly to each end of the gage length of each specimen with Chromel wire. Direct radiation from the furnace windings to the bead was blocked by a ceramic shield tied in place over the thermocouple bead. In two of the four tests, the bead of a thermocouple of platinum and platinum-10 weight percent rhodium wires (0.015-in. diameter) was tied to the bead of one of the base metal thermocouples.

The calibration of the base metal thermocouples was taken from the manufacturer's data supplied with the spools of wire. The noble metal wires were calibrated at 1650 and 1830 F by Hoskins Manufacturing Co. against standard wire with properties traceable to the U.S. National Bureau of Standards. Correct respective thermal emfs for the base and noble metal thermocouples at 1700 F were determined to be 38.46 and 8.739 mV.

Cold junction correction for the base metal thermocouples was applied at the potentiometer by reference to the reading of a mercury-in-glass thermometer with its bulb at the potentiometer terminals. An automatic, electrical, cold junction compensator (Omega Engineering, Inc., Stamford, Conn., Model CJ) was used with the noble metal thermocouple.

Rupture test procedures followed usual practice, except that in the two instances where a noble metal thermocouple had been added temperature adjustments were made to keep the indication of that couple at the value that it had initially when the adjacent base metal couple read 1700 F (namely, 38.46 mV).

The tests used two commercial testers (Satec Corp., Grove City, Pa., Model M3); one specimen with and one without the added noble metal thermocouple were tested in turn in each machine. In agreement with the AGARD findings, rupture times tended to be longer when temperature was adjusted in accordance with the reading of the noble metal thermocouple, but the small number of tests and an overlap of scatterbands preclude unequivocal conclusions:

Observed Rupture Life, h, at 9050-psi Stress at 1700 F

Control by base metal couple		Control by noble metal couple
407.0		397.9
361.6		338.6
384.3	Average	368.2

The record of temperature readings during these tests indicates that for about 16 h of the second day, the temperature of the specimen which failed in 361.6 h was approximately 4 or 5 F above the aim temperature. This period outside the range of ±3 F permitted by ASTM Recommended Practice for Creep and Time-for-Rupture Tension Tests of Materials (E 139 – 66T) probably lowered the actual life slightly from what would have been obtained with better control. The other three tests stayed within recommended maximum tolerances, based on readings of the thermocouple used to monitor the temperature of the specimen.

In the two tests controlled on the basis of the readings of the noble metal thermocouple, a small but perceptible drift was observed in the output from the base metal thermocouples. The reading of the base metal thermocouple, relative to that of the adjacent noble metal one, varied no more than about 0.01 mV during the first week at the nominal test temperature of 1700 F. Even near the end of the test (397.9-h duration), the emf of the base metal thermocouple was only about 38.52 mV, equivalent to about a 3 F rise in indicated temperature. The relative drift of the base metal thermocouple in the test lasting 338.6 h was even smaller, but this time the change was in the opposite direction.

The most immediate conclusion is that calibration drift of 18-gage Type K thermocouples is remarkably small, even during several hundred hours at 1700 F. If the suggested correlation in the AGARD tests between rupture time and pyrometric practice is real, the tests reported here seem to indicate that factors other than calibration drift (for example, the degree of intimacy of contact between the thermocouple bead and the specimen or conduction of heat away from the bead by the thermocouple wires) may play the major role.

After the remaining AGARD tests have been completed and evaluated, this matter probably should be considered anew to determine whether present specifications for temperature control during creep rupture and tension tests are adequate.

R. M. Goldhoff[1]

A Method for Extrapolating Rupture Ductility

REFERENCE: Goldhoff, R. M., "**A Method for Extrapolating Rupture Ductility,**" *Elevated Temperature Testing Problem Areas, ASTM STP 488*, American Society for Testing and Materials, 1971, pp. 82–94.

ABSTRACT: The prediction of long time, elevated temperature properties of useful alloys is of great practical importance. Time-temperature parametric relationships for correlating short time strength and predicting the long time residual strength at temperature have been developed and verified in long time testing. Equal in importance to the residual strength, however, is the capacity for deformation prior to failure, and many applications would appear to be limited by this characteristic. The object of this paper is to present an approximate but useful method for correlating and predicting characteristic, smooth test bar ductility.

The essence of the method presented is to obtain data which contain rupture ductility in a form that leads to a consistent array which can be treated by parameter techniques. To this end the rupture elongation and rupture time are ratioed to form the average creep rate, which can be treated parametrically and, when combined with the usual stress rupture parameter, provides the elements of this simple technique.

Several sets of high temperature data are treated to illustrate the technique and compare the results with actual long time data. Some attempt is made to determine how well the method may be expected to work when few data points are available. Further, the method is adaptable to prediction of either elongation or reduction of area at rupture, and this too is illustrated in the text. It is suggested that the method, while approximate, can serve as a quality control tool and should, therefore, be useful to materials engineers.

KEY WORDS: ductility, deformation, elongation, creep properties, creep rupture strength, reduction of area, residual stress, stresses, failure, high temperature tests, alloy steels, stainless steels, quality control

The prediction of long time, elevated temperature properties of commercial alloys is a subject of considerable importance. A major undertaking over a period of many years has been the development of the time-temperature parametric relationship for correlating short time strength and predicting the long time residual strength at temperature. The usefulness of

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these techniques has been verified in long time, uniaxial, smooth bar testing in the laboratory.[2] The design engineer will find these methods directly useful in setting stresses. Equal in importance to the residual strength, however, is the capacity for deformation prior to failure. Many applications would appear to be limited by this latter characteristic. While the prediction of long time ductility at failure in elevated temperature rupture tests is not clearly applicable to design needs, it most certainly is useful to the quality control of materials that will be used in machine component service at high temperatures. A reliable method for correlating and predicting this characteristic smooth bar ductility is not currently available. The object of the work presented in this paper is to suggest such a method and show the results of its application.

Development of a Prediction System

The simple time-temperature parameter is a convenient and useful tool, but its effective use depends on several assumptions which may or may not be justified. Basically, its use for extrapolation depends on an array of data which are internally consistent and well behaved. Thus, a graphical representation of the data showing the interdependence of the test parameters produces curves which are smooth and continuous with a predictable trend to their extension. The usual stress rupture data sets, as shown in Fig. 1, meet these criteria. On the other hand, the ductility values corresponding to the sets of stress rupture data are seldom well behaved, as

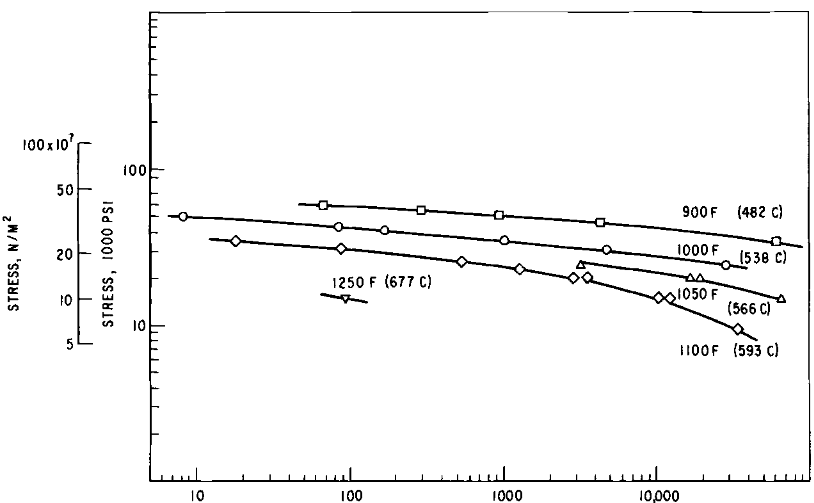

FIG. 1—*Stress rupture data for alloy 1, Cr-Mo-V.*

[2] *Time-Temperature Parameters for Creep-Rupture Analysis*, ASM Publication No. D8-100, American Society for Metals, Metals Park, Ohio.

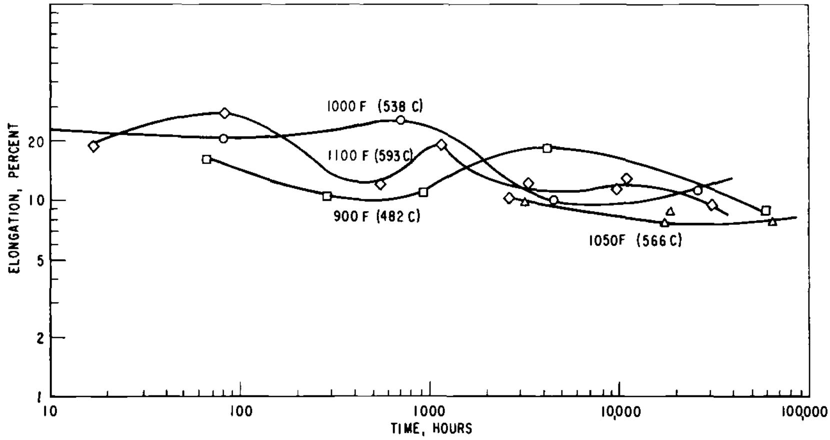

FIG. 2—*Rupture ductility data for alloy 1, Cr-Mo-V.*

shown in Fig. 2, and consequently are not amenable directly to time-temperature parameter correlation and extrapolation.

The essence of a method to combine rupture ductility into a data presentation which does have the useful characteristics described above was presented much earlier by Smith.[3] By the simple expedient of dividing the elongation at rupture by the time to rupture for a given test, the "average" creep rate is found. These data, when presented graphically, form a set

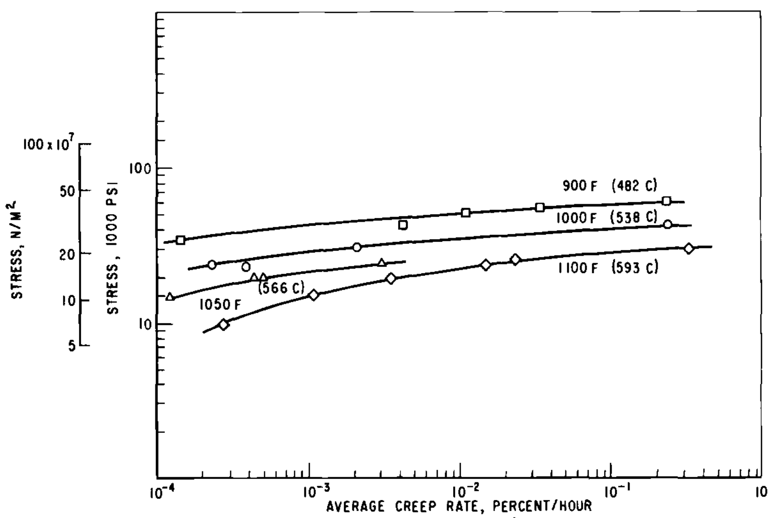

FIG. 3—*Average creep rate versus stress for alloy 1, Cr-Mo-V.*

[3] Smith, G. V., *Properties of Metals at Elevated Temperature*, McGraw-Hill, 1950, p. 151.

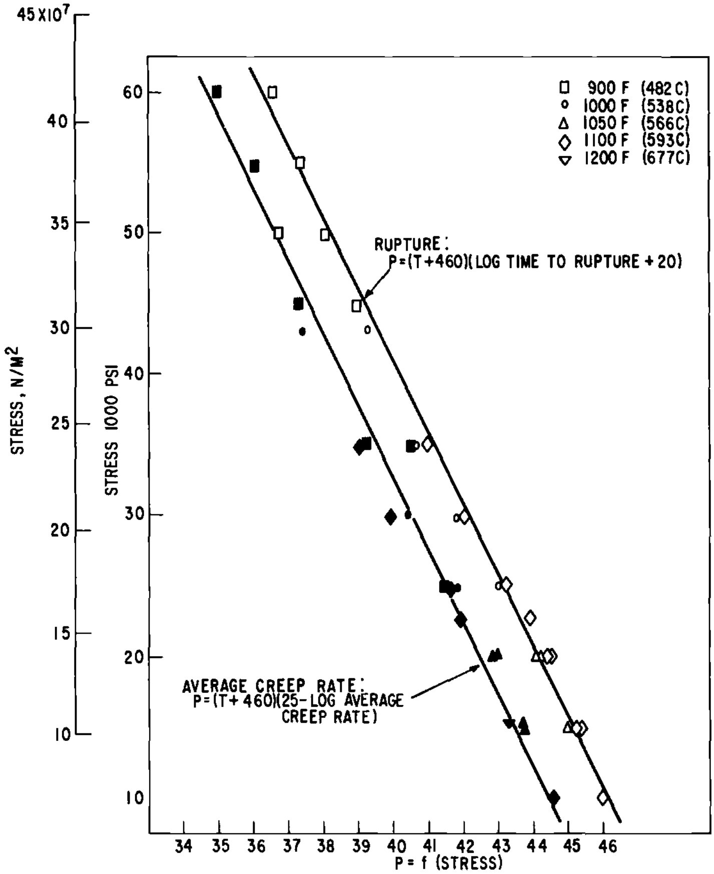

FIG. 4—*Parametric fitting of data for alloy 1, Cr-Mo-V.*

of curves analogous to the common stress versus minimum creep rate curves illustrated in Fig. 3. Although Smith suggested these curves were linear in semilogrithmic form and therefore simple to extrapolate, in the general case they are not. In this form the data can be treated by the time-temperature parameter such that prediction of elongation for times beyond the periods involved in the actual tests is possible. The data of Fig. 3 have been correlated parametrically in Fig. 4. For purposes of illustration, the simple Larson-Miller parameter has been used, though any other would serve the same purpose. Furthermore, for most of the sets of data to be discussed later the constant in the chosen parameter method has been optimized for both strength and average creep rate. Nonetheless, for the illustration of the method it suffices to adopt an average value of the constant as was done in Fig. 4.

The suggested method for calculating the trend of rupture elongation with time is as follows:

1. Raw data are correlated and analyzed using a computer programmed to fit polynomial equations describing the relation between stress σ and the Larson-Miller parameter P based on time to rupture and on average creep rate. The equations have the form

$$\sigma = A + BP + CP^2 + \cdots + DP^N \qquad (1)$$

where $P = (T + 460)$ [log(time to rupture) + 20], for T = temperature in deg F and rupture time in hours, and A, B, C, and D are constants.

$$\sigma = A_1 + B_1P_1 + C_1P_1^2 + \cdots + D_1P_1^M \qquad (2)$$

where $P_1 = (T + 460)$ [25 − log(average creep rate)], for T = temperature in deg F, average creep rate in percent per hour, and A_1, B_1, C_1, and D_1 are constants.

2. Select times at which the elongation is to be evaluated, and for the temperature in question compute the Larson-Miller parameters and thence, using Eq 1, the corresponding stresses.

3. The stresses found in the previous step now are used in conjunction with Eq 2 to compute the average creep rate parameter values for the times and temperature chosen previously.

4. From the rate parameters computed in the previous step the average creep rates corresponding to the chosen times and temperature can be determined using the Larson-Miller rate parameter, P_1.

5. Finally, the relationship

$$\text{average creep rate} \times \text{time to rupture} = \text{rupture elongation} \qquad (3)$$

can be applied to compute the rupture elongation, ϵ_r, corresponding to each selected time to rupture, t_r, for the chosen temperature T.

It must be noted that this technique is sensitive to data handling. Manual methods tend to give poor and unreproducible results. For this reason, computer programs which accepted raw rupture data (σ, t_r, ϵ_r, T) and presented computed elongations for a chosen temperature and selected times were used. This technique is useful *only* under these conditions.

Data

Four sets of reasonably complete, long time data were chosen to represent a variety of commercial alloys. These were

1. 1Cr-1Mo-¼V steel (heat treated to low strength and high rupture ductility)
2. 1Cr-1Mo-¼V steel (heat treated to high strength and low rupture ductility)
3. 304 stainless steel
4. Inco 718

TABLE 1—*Data for the alloys analyzed.*

Temperature, deg F	Stress, psi	Time to Rupture, h	Elongation, %	Reduction of Area, %
	Alloy 1: 1Cr-1Mo-¼V			
800	30 000	5 330.0	...	...
	15 000	5 380.4	...	...
900	60 000	67.4	16.1	62.6
	55 000	291.3	10.5	53.5
	50 000	957.0	11.0	67.5
	45 000	4 305.6	18.7	65.5
	35 000	60 376.1	8.7	44.0
1000	50 000	8.1	23.9	70.0
	43 000	83.1	20.3	70.0
	40 000	167.2	...	...
	35 000	1 023.8	...	...
	35 000	638.0	9.7	67.3
	30 000	4 663.0	9.8	71.0
	25 000	28 614.9	11.3	40.2
	25 000	29 713.6	6.8	34.4
1050	25 000	3 204.7	9.8	72.5
	20 000	17 359.8	7.6	54.4
	20 000	18 710.8	8.8	33.3
	15 000	63 870.0	7.8	35.2
1100	35 000	17.6	19.8	72.0
	30 000	86.7	28.5	79.0
	25 000	540.6	12.2	77.0
	23 000	1 258.2	19.5	76.4
	20 000	3 307.8	12.0	65.0
	20 000	2 824.8	10.1	74.0
	15 000	10 302.8	11.2	62.0
	15 000	11 055.2	12.5	52.5
	10 000	32 785.0	9.2	70.4
1250	15 000	92.7	45.4	88.0
	Alloy 2: 1Cr-1Mo-¼V			
900	82 000	975.0	4.0	15.3
	78 000	3 581.0	2.1	9.5
	70 000	9 878.0	1.5	4.0
1000	80 000	7.0	5.8	4.1
	75 000	17.0	9.0	2.6
	68 000	213.0	4.8	16.0
	60 000	1 493.0	1.8	4.0
	56 000	2 491.0	1.3	...
	49 000	5 108.0	1.3	...
	43 000	7 390.0	2.1	4.0
	38 000	10 447.0	1.0	3.0
1100	70 000	1.0	9.0	46.0
	60 500	18.0	3.8	16.0
	50 000	167.0	2.0	5.5
	40 000	615.0	1.3	5.0
	29.000	2.220.0	1.5	8.0
	22 000	6 637.0	...	...
1200	4 000	19.0	5.0	14.0
	30 000	102.0	7.0	12.0
	25 000	125.0	6.0	22.0

TABLE 1—*Continued.*

Temperature, deg F	Stress, psi	Time to Rupture, h	Elongation, %	Reduction of Area, %
1350..................	20 000	3.7	16.0	78.0
	15 000	8.9	14.0	78.0
	10 000	31.8	13.0	77.0
	Alloy 3: 304 Stainless Steel			
1200..................	37	1.0	35.0	...
	31	3.7	24.0	...
	28	11.3	20.0	...
	17	308.0	18.0	...
	14	1 002.0	13.0	...
	12	3 074.0	16.0	...
1300..................	30	0.35	28.0	...
	22	3.3	21.0	...
	18	13.3	17.0	...
	12	185.0	20.0	...
	10	614.0	17.0	...
	8	1 978.0	16.0	...
	5.8	8 159.0	11.0	...
1400..................	22	0.47	32.0	...
	17	2.3	26.0	...
	14	6.4	26.0	...
	7	740.0	19.0	...
	6	1 112.0	15.0	...
	5	3 430.0	14.0	...
1500..................	15	0.32	27.0	...
	11	4.4	25.0	...
	9.5	5.8	19.0	...
	8.2	26.7	23.0	...
	6	133.0	19.0	...
	5	277.0	13.0	...
	4	1 092.0	13.0	...
	Alloy 4: Inco 718			
1000..................	158 000	27.8	16.2	23.0
	150 000	133.2	7.0	19.0
	145 000	256.0	4.8	19.0
	140 000	814.9	3.4	25.0
	134 000	1 731.0	2.6	28.0
	124 000	8 473.3	2.68	17.0
	118 000	21 523.6	3.36	23.4
1100..................	135 000	28.2	3.9	13.0
	130 000	62.0	4.0	28.0
	123 000	151.9	4.1	28.0
	117 000	367.5	4.7	22.0
	105 000	2 327.6	4.2	17.0
	94 000	10 606.2	4.1	18.0
	86 000	32 990.7	6.5	16.3
1200..................	115 000	10.6	4.3	13.0
	108 000	30.8	3.2	19.0
	96 000	150.0	5.4	17.0
	87 000	747.2	7.0	17.0
	78 000	3 131.5	7.1	19.0
	68 000	7 263.2	3.1	19.0
	63 000	10 232.3	8.1	31.0

TABLE 1—*Continued.*

Temperature, deg F	Stress, psi	Time to Rupture, h	Elongation, %	Reduction of Area, %
1300	86 000	18.0	10.2	24.0
	76 000	70.5	8.1	22.1
	68 000	182.7	14.6	32.6
	60 000	476.8	7.1	29.3
	55 000	808.0	7.5	26.0
	44 000	2 870.7	18.3	34.0
	37 000	6 048.0	8.7	33.0

The actual sets of data are shown in Table 1. Alloys 1, 2, and 4 were tested in the Materials and Processes Laboratory of the General Electric Co. The data for alloy 3 can be found in the *Report on Elevated-Temperature Properties of Stainless Steels, ASTM DS5-S1*.

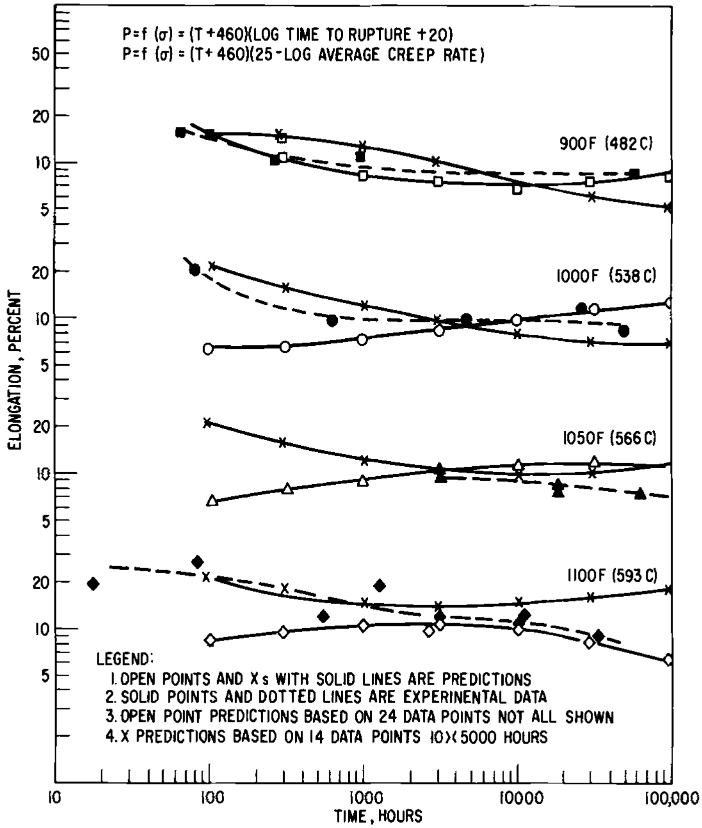

FIG. 5—*Comparison of actual and calculated rupture elongation for alloy 1, Cr-Mo-V.*

Results and Discussion

The four sets of rupture data were treated by the method outlined above and are shown in Figs. 5–8. Here the ductility data (elongation at rupture), as calculated and experimentally observed as a function of exposure time, are compared. On each figure the form of the parametric method used and the associated constant are shown. For alloys 1, 2, and 4 the Larson-Miller method applies for both the strength and rate correlations, while alloy 3 makes use of a linear-type parameter. In all cases, between 24 and 28

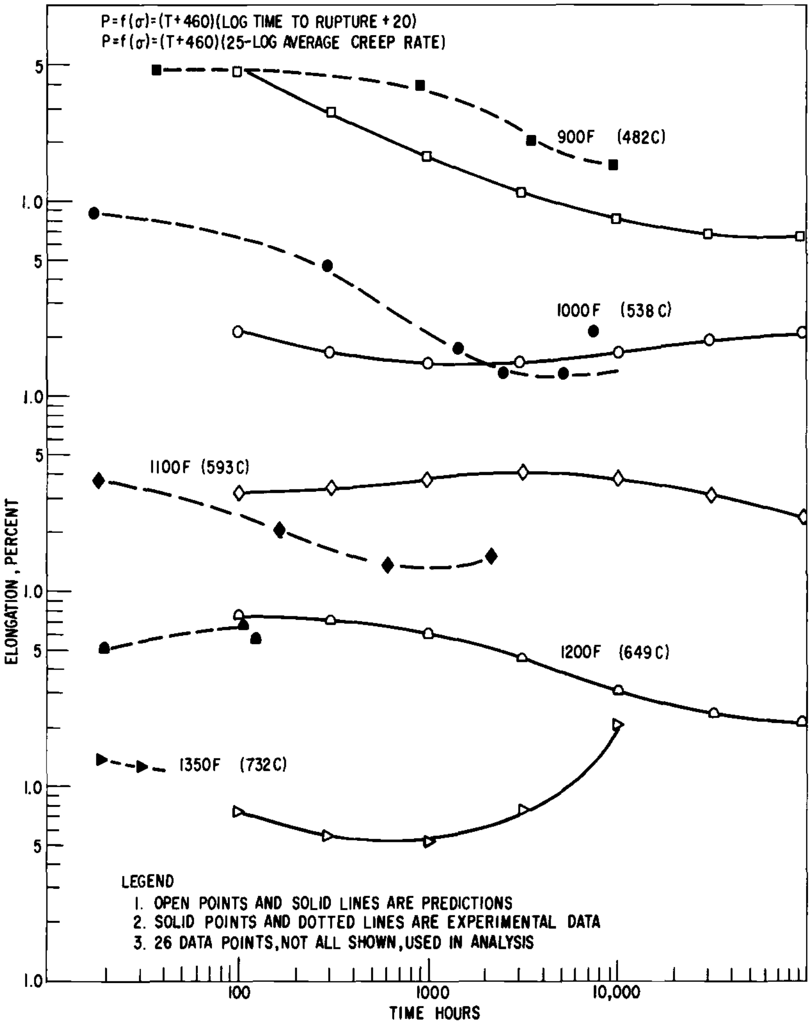

FIG. 6—*Comparison of actual and calculated rupture elongation for alloy 2, Cr-Mo-V.*

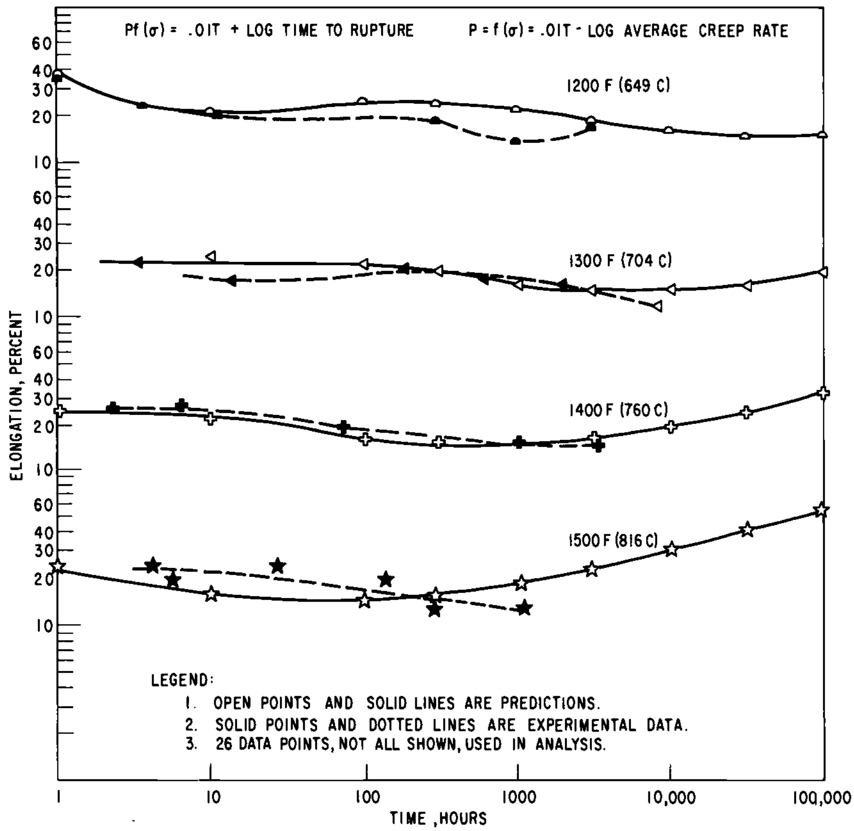

FIG. 7—*Comparison of actual and calculated rupture elongation for alloy 3, 304 stainless steel.*

data points were available. In the first instance all the points were used to correlate and predict the results shown. Under these conditions the predictions are reasonable when compared with experiment and indeed seem capable of reproducing the contours associated with the smoothed experimental data.

Realistically, the method must handle short time data from which extrapolations to the longer times are then made. In Fig. 5 for alloy 1 the analysis was conducted on the basis of 14 data points which encompassed data from 10 to 5000 h. The predictions are, within reason, in agreement with the general observation that the fewer number of fitted points predict the shorter time ductility more accurately whereas the fitting with all the data points tends to give better predictions at longer times. Considering the nature of scatter in this experimental quantity and

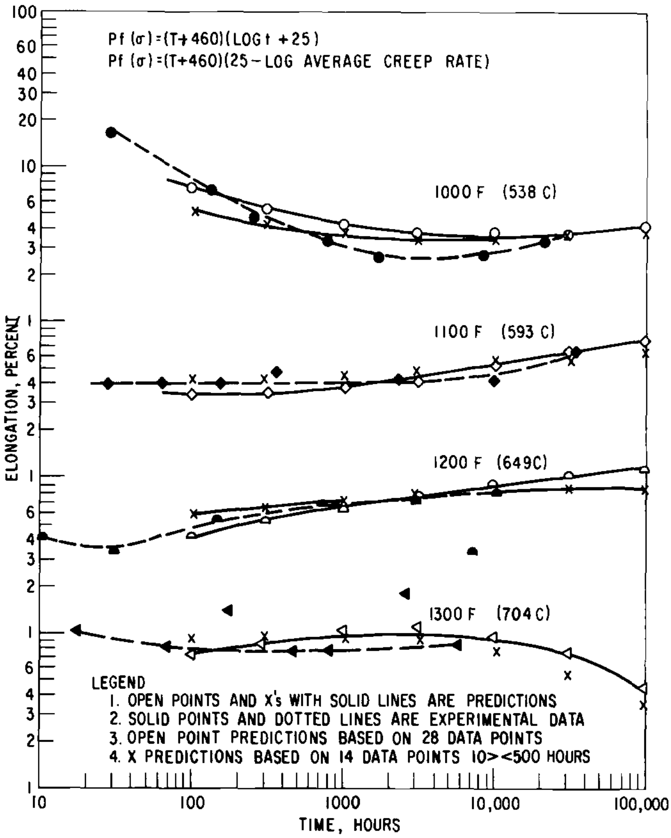

FIG. 8—*Comparison of actual and calculated rupture elongation for alloy 4, Inco 718.*

the use to which it will be put, the differences shown generally are not significant. Further, the results might be improved considerably if a more exact fitting of the data sets was to be used. On the other hand, referring to Fig. 8 for alloy 4, when the number of data points used to fit the parameters was halved to include data from 10 to 500 h, the predictions nearly coincide with those of the fitting using the total number of data points. This again is a reflection of the parameter fit with regard to both correlation and extrapolation of the data.

Still another possibility with the suggested method is extrapolation of reduction of area (*RA*) data. Here the most plausible technique is to convert the actual reduction of area values to true strain through the equation

$$\epsilon_{TR} = \frac{RA}{100 - RA} \qquad (4)$$

where ϵ_{TR} is true strain. The data thus obtained are treated exactly as suggested, but an additional step is necessary to convert calculated true strain values back to reduction of area values. Comparisons of actual and calculated values of reduction of area as a function of time are shown for alloys 1 and 2 in Figs. 9 and 10. Once again the predictions are reasonable.

Summary

Modern, high temperature materials selection has become highly influenceable by the concern for deformational ability. Many critical problems appear to be limited by this factor, and compromises with strength are required for materials in many components. The problem area dealt with in this paper is how to estimate ductilities at times comparable with those necessary for design strength predictions, in some industries 10^5 h. While the technique outlined and applied is admittedly qualitative, the reader can judge, in light of what has been said above, that it can be a useful quality control tool for materials engineering, and I propose it only in this light. Viewed in this way, the obtaining and reporting of ductility values (both elongation and reduction of area) in high temperature tests assumes importance. Added to this, then, is the need for adherence to quality standard test practices so that the best possible data can be obtained.

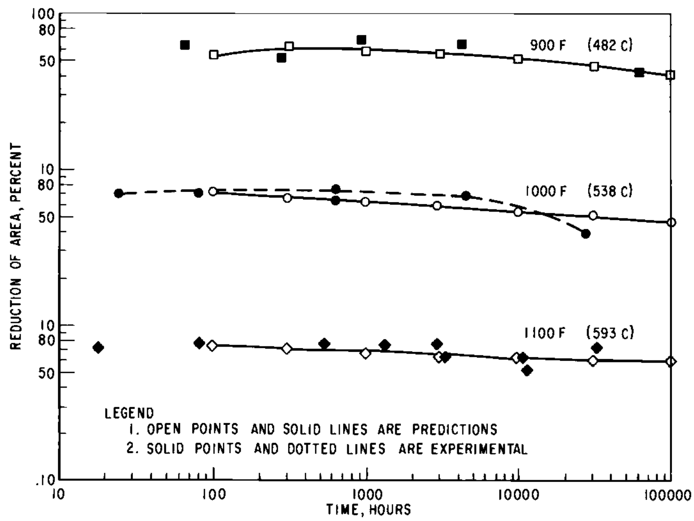

FIG. 9—*Comparison of actual and calculated rupture reduction of area for alloy 1, Cr-Mo-V.*

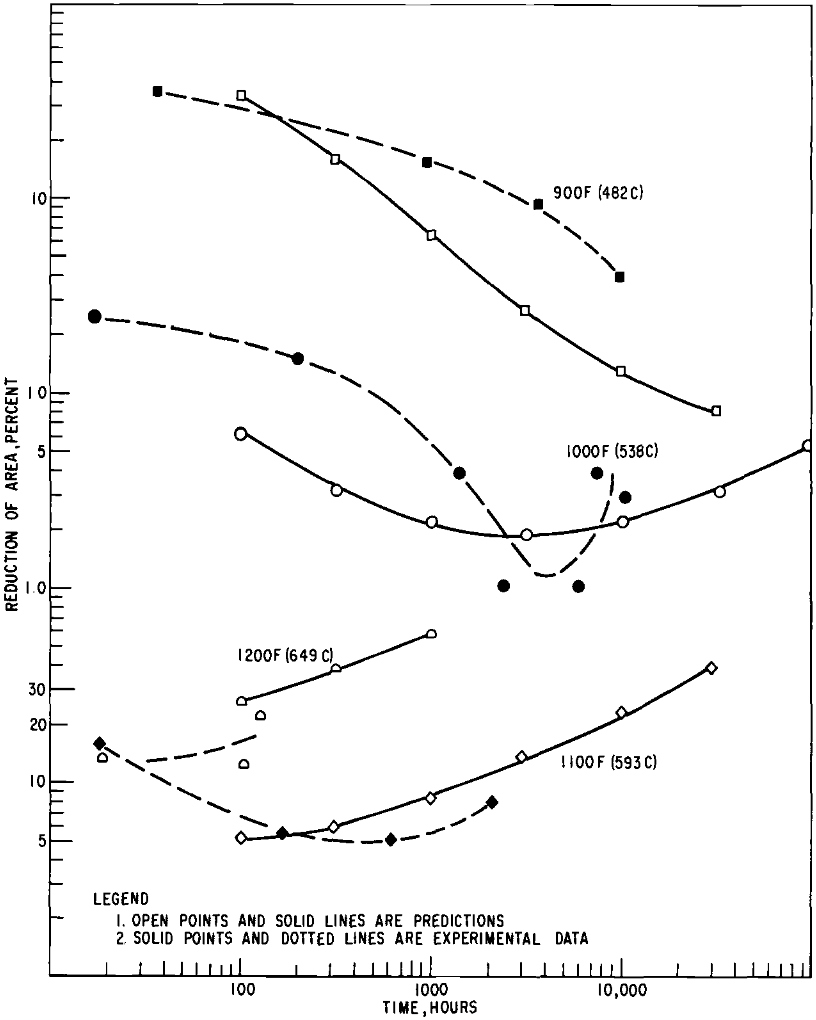

FIG. 10—*Comparison of actual and calculated rupture reduction of area for alloy 2, Cr-Mo-V.*

M. M. Paxton[1]

Elevated Temperature Tensile Grips for Tubing*

REFERENCE: Paxton, M. M., "**Elevated Temperature Tensile Grips for Tubing,**" *Elevated Temperature Testing Problem Areas, ASTM STP 488*, American Society for Testing and Materials, 1971, pp. 95–99.

ABSTRACT: A technique has been developed for conducting elevated temperature tension tests on a 4-in. tubular specimen. The new technique, utilizing commercially available compression fittings, has been used successfully for over 200 elevated temperature tension tests. The technique complies with all pertinent ASTM standards. Tension tests on tubing can now be performed in a manner comparable to procedures used for conventional solid specimens.

KEY WORDS: high temperature tests, tension tests, tubing, stainless steels, nuclear fuel cladding

A method for gripping tubing during elevated temperature tension testing has been developed. Normal industrial practice for the tension testing of tubes requires specimens 36 in. in length; this extreme length is needed to allow gripping in the cold area outside the testing furnace and, thus, results in considerable tube waste. The test section usually is taken from a zone of the tube free from temperature gradients, and extensometer rods are used to provide strain measurement. A survey of various present gripping methods further revealed no practical techniques for gripping short lengths of tubing (less than 4 in.) at elevated temperatures.

Consequently, a technique for gripping tubes utilizing compression tube fittings has been developed for the extensive tension testing required to qualify tubing for Fast Flux Test Facility fuel cladding applications. Tubular cladding specimens approximately 3 in. in length were cut from annealed Type 304 and Type 316 seamless stainless steel tubing of 0.250-in. outside diameter and 0.218-in. inside diameter. The specimens were fitted

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* This paper is based on work performed under U.S. Atomic Energy Commission Contract AT(45-1)-1830.

with a snug fitting metal plug, Fig. 1, following the specifications given in ASTM Methods and Definitions for Mechanical Testing for Steel Products, Supplement II (A 370 - 68). The plugs were fabricated from commercially available ¼ by 0.083-in. wall tubing (hollow plugs are required to enable the compression fitting to deform the specimen and plug). The specimen and its plugs then were placed into a ¼-in. tube to tube union, and the ferrules were set by tightening the nut at least 1¾ turns past finger tight. The specimen was fitted with an adaptor (tube to ⅛-in. female pipe thread), inserted into a suitable load train, Fig. 2, and tension tested.

More than 200 elevated temperature (800, 1000, 1200, 1400, and 1600 F) tension tests have been performed on a hard beam tension machine using this method of gripping, with nearly 100 percent success. A comparison of data obtained using the cold grip procedure and hot grip procedures is contained in Table 1. Both methods produce comparable results.

Room temperature alignment was verified using strain gages placed at 0, 90, 180, and 270 deg on a full tubular specimen. The variation in elastic strain on opposite sides of the specimen was less than 6 percent, well within the limits recommended by ASTM Recommended Practice for Short-Time Elevated-Temperature Tension Tests of Materials (E 21 - 66T).

Initial tests performed on 20 percent cold worked Type 316 stainless steel fuel cladding specimens, 0.230-in. outside diameter by 0.200-in. inside

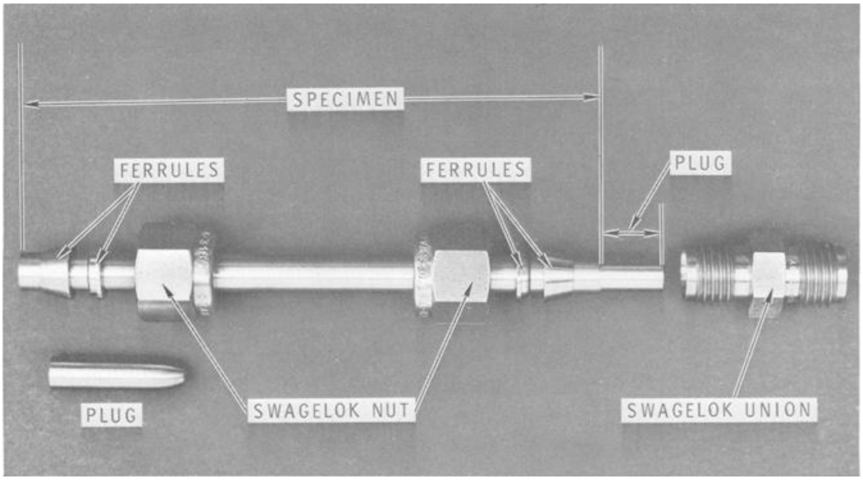

FIG. 1—*Unassembled, full tubular specimen and associated hardware.*

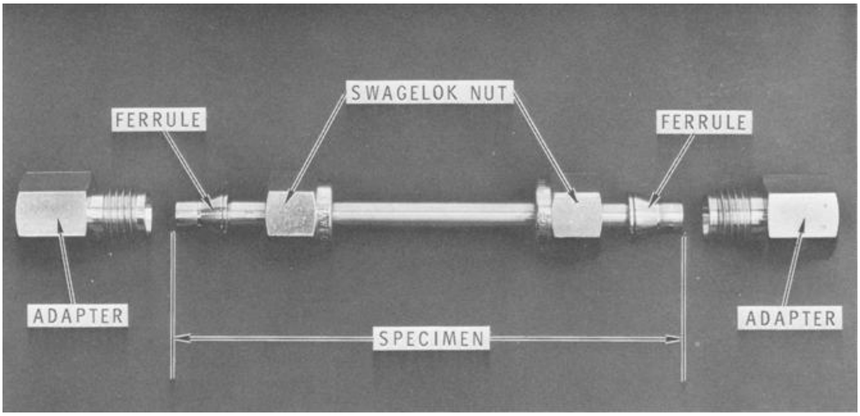

FIG. 2—*Assembled, fuel tubular specimens ready for testing.*

diameter, resulted in slippage at 800 and 1000 F. This was eliminated by burnishing approximately $\frac{5}{8}$ in. from the ends of each specimen with a small hand file.

Total elongation of the specimen is obtained readily by measuring the entire specimen before and after testing. The specimen gage length is the distance between the plugs as defined by ASTM A 370. A few, conventional, uniaxial creep-to-rupture tests were performed utilizing this gripping technique, from which it was determined that the procedure worked satisfactorily.

This method of gripping offers a simple means to conduct elevated temperature tension tests on full tubular specimens in a manner comparable to procedures used for solid specimens. In general, the test data obtained are more precise than those obtained with 36-in. specimens. In addition, the technique is readily adaptable to environmental testing if necessary.

TABLE 1—*Comparison of hot grip and cold grip tension data on annealed Type 304 stainless steel tubing.*[a]

Grips	0.2% Yield Strength, psi	Ultimate Tensile Strength, psi	Total Elongation in 2 in., %
Hot.............	23 300	74 600	32.0
	24 200	76 300	37.0
	24 600	74 750	34.0
Cold............	23 100	72 700[b]	32.0
	24 100	71 400	38.0
	25 000	71 200	27.0

[a] Tests performed at 1000 F.

[b] Tests conducted at a different laboratory; difference likely due to variations in testing technique and temperature control.

DISCUSSION

H. R. Voorhees[1]—The suggested use of flareless tube fittings appears to offer a simple, economical way to get a firm axial pull on thin tubing. Some of our experience with such fittings in more usual applications may be useful in tension testing as well. To my knowledge, three sources exist for flareless fittings. Although components are not interchangeable between manufacturers, all three designs feature a pair of precision ground ferrules, which press into the outer tube wall and against the inside of the fitting body to grasp the tubing and provide continuous lines of sealing around the tube. Fittings are offered in a variety of alloys and plastics in nominal sizes which are multiples of a 1/16-in. tube diameter. Tolerances vary, but a gas-tight seal usually can be made when the actual tube size ranges from 0.005 in. less than to several thousandths of an inch larger than the nominal dimension.

For zirconium alloy tubes tested at 752 F under internal pressure, Type 316 stainless steel provided needed strength and resistance to oxidation. Uniform success was obtained when a close match existed between the tubing and fitting diameters, despite the much lower coefficient of thermal expansion of the tube material. Paxton found a need to tighten his fittings 1¾ turns instead of the 1¼ turns recommended by the manufacturer. We find that if a gas-tight seal is not obtained at or slightly over the degree of tightening specified by the fitting maker, the leakage seldom can be stopped by further tightening of the nut (When the tubes are slightly undersize, we first tighten the nut until the tube no longer turns freely inside the ferrules under light finger pressure and then apply the recommended number of turns to achieve the seal.).

Some of the tubes tested differed from all stock sizes of fittings, requiring a thin sleeve pressed onto the tube to bring its outside diameter up to the next fitting size. A gas-tight seal could be obtained in fewer than half of our attempts. Even when the initial seal was satisfactory, some sleeves (and, therefore, the fitting) slid off the end of the tube at high internal pressure. Failures of this type were minimized by using a sleeve of annealed material with thermal expansion properties matching those of the tube, and with the final ¼ in. or less of the tube filled by a piece of thick walled

[1] Materials Technology Corp., Ann Arbor, Mich. 48107.

ferritic steel tubing. This will expand the end of the tube slightly when the assembly is heated.

If more than a few tubes of given, nonstandard diameter must be tested, use of custom fittings of special size should result in lower total cost. Some special sizes already are made; for example, one source supplies fittings for tubing 0.425 in. in diameter, a size which has been adopted for certain nuclear power applications. Fittings also can be purchased in some metric sizes.