Discussion

A. R. C. Markl.[4] The investigation summarized in this paper covers several phases of high-temperature piping design on which information has long been urgently needed, and this work accordingly merits close study by all concerned. The writer considers publication of the authors' high-temperature fatigue-test data particularly timely in view of current efforts of a task force under ASA Committee B31 directed at a revision of Chapter 3 of Section 6 of the ASA Code for Pressure Piping. The most controversial issue encountered by this group relates to the establishment of a suitable allowable range for the stresses caused in piping systems by thermal expansion. The stress range, including pressure and weight stresses, has been set tentatively at $1^1/_4$ times the sum of the hot and cold allowable stresses or S-values published in the code (for mildly cyclic conditions, where the number of cycles of complete stress reversal during the life of the system is not expected to exceed 2500); no agreement has been reached, however, as many opinions have been expressed to the effect that this limit is too high as that it is too low. The authors' test results should help to resolve this issue.

With this in view, the writer has devoted considerable effort to an attempt at the evaluation of the results of the authors' fatigue tests, specifically those conducted with the Schedule 160 austenitic mock-up where the more highly stressed elbow failed in the precise location and manner anticipated from the writer's room-temperature fatigue tests.[5]

As a first step, the equivalent stresses given in the first and last columns of the authors' Table 2 have been converted into maximum longitudinal stresses (assuming 8250 psi hoop stress, as predicted from the common formula for 2000 psi pressure). In a second step, these stresses as well as those given in the last four columns of Table 3 have been translated from the location of gage "e" (45 in. distant from the failed elbow, according to verbal advice from the authors) to the point of maximum stress near gage "m" (on the side of the elbow, at mid-point). This was done by multiplication with the ratio of lever arms, which are related approximately as 4 to 3. The results of these operations are shown in the first two columns of Table 6 of this discussion; the third column gives the differences between maximum and minimum stresses. The fourth and fifth columns give the identical information in terms of a constant mean stress and a superimposed variable stress. The sixth column, finally, gives the equivalent fully reversed stress as computed by a formula suggested by H. F. Moore for determining the endurance limit for complete reversal of stresses from any other given cyclic stress condition.

Since the test conditions were changed six times before failure was produced, the number N of cycles required to produce failure under a given fully reversed bending stress S cannot be stated directly; it becomes necessary to make an assumption for the interrelation between S and N. For this purpose, the writer has used the formula

$$SN^{0.2} = C$$

which has served him reasonably well to correlate his own room-temperature fatigue-test results on carbon steel. As a rough approximation, it also appears to fit limited test data on aluminum and stainless steel.

Applying this formula to the authors' test data, the constant is computed as $C = 183{,}500$, and with this the number of cycles to failure for the different stress conditions applied in the authors' tests become as shown in column 7; the relation between S and N is graphically shown in Fig. 30, herewith (which also includes S-N curves for room-temperature tests conducted by the writer in the research laboratories of his company). The ratios of the actual cycles N_a sustained to the predicted number of cycles N to failure, multiplied by 100 to give per cent, listed in column 9, provide a measure of the amount of fatigue life consumed in each separate run. It will be noted that the last test with its extremely severe stress conditions absorbed nearly three quarters of the useful life of the assembly. (This, incidentally, indicates that even a relatively large error in the assumption of the magnitude of the transverse stress present in the first two runs would exert a negligible effect on the evaluation.)

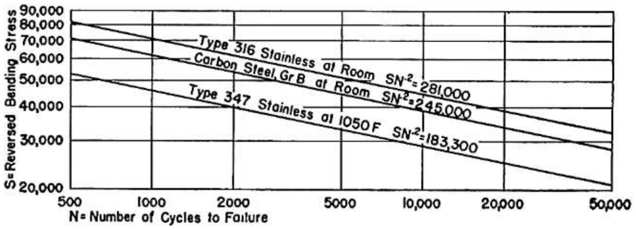

Fig. 30 S-N Curves

This completes the direct evaluation of the authors' tests. In a next step, an attempt is made to correlate this new information with the results of room-temperature fatigue tests conducted in the company research laboratories, and to ascertain whether the fatigue behavior of different materials at different temperatures can be correlated with the actual physical properties of the material tested or the allowable stresses derived from the specification minima.

Pertinent data are given in Table 7 of this discussion. It will be noted that the ratios between the constant C and particularly

TABLE 6 EVALUATION OF LIFE OF 160-SCHEDULE AUSTENITIC MOCK-UP UNDER FULLY REVERSED BENDING

Col.	1	2	3	4	5	6	7	8	9
Test run	Max stress, S_{max}	Min stress, S_{min}	Stress range, $S_{max} - S_{min}$	Constant stress, $S_c = 1/2\,(S_{max} + S_{min})$	Variable stress, $S_v = S_{max} - S_c$	Equivalent fully reversed stress, $S = 1/3 S_c + S_v$	Fatigue life under fully reversed stress,[a] $N = C^5/S^5$	Actual cycles sustained, N_a	Life consumed in specific test run, $100\ N_a/N$, per cent
1	+31650[b]	+17300[b]	14350	24475	7175	15300	248200	4104[b]	1.6
2	+38900[b]	+20450[b]	18450	29675	9225	19100	81900	3966[b]	4.8
3	+25600[c]	−14300[c]	39900	5650	19950	21800	42300	4000[c]	9.5
4	+29600[c]	−17300[c]	46900	6150	23450	25500	19300	878[c]	4.6
5	+35500[c]	−18900[c]	54400	8300	27200	30000	8560	527[c]	6.1
6	+38100[c]	−23600[c]	61700	7250	30850	33300	5080	3730[c, d]	73.4
									100.0

[a] Based on relation suggested in H. F. Moore's, "Textbook of the Materials of Engineering," McGraw-Hill Book Company, Inc., New York, N. Y., 1941, p. 57.
[b] From authors' Table 2; equivalent stresses converted to max longitudinal stresses assuming 8250 psi transverse stress corresponding to 2000 psi internal pressure, and multiplied by 1.33 to convert from location e to location m.
[c] From authors' Table 3; stresses converted to location e by multiplication by 1.33.
[d] Failed at end of this run.

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[4] Chief Research Engineer, Tube Turns, Inc., Louisville, Ky.

[5] "Fatigue Tests of Welding Elbows and Comparable Double-Mitre Bends," by A. R. C. Markl, Trans. ASME, vol. 69, 1947, pp. 869–879.

TABLE 7 COMPARISON OF FULL-SCALE FATIGUE-TEST DATA AND EVALUATION IN TERMS OF PROPOSED RULES FOR CHAPTER 3 OF SECTION 6 OF CODE FOR PRESSURE PIPING ASA B31.1

Material	Carbon steel, grade B	Stainless, type 316	Stainless, type 347
Temperature, deg F	Room	Room	1050
Average ultimate tensile strength U, psi	76200	?	58000
Average yield point or yield strength Y, psi	47600	?	44000
Allowable stress per ASME Boiler Code S', psi	15000	18750	13100
Factor C in formula: $SN^{0.2} = C$	245000	281000	183500
Ratio C/U	3.21	—	3.16
Ratio C/Y	5.14	—	4.17
Ratio C/S'	16.3	15.1	14.0
Average stress range, psi, to produce failure under reversed bending in 2500 cycles, $R_a = 2C/2500^{0.2}$	102000	118000	76800
Allowable stress range for 2500 cycles or less per current proposal by ASA Task Force to revise Chapter 3 of Section 6 of ASA B31.1, 1.25 times sum of hot and cold S-values in ASME Boiler Code or $R = 2.5S'$ for constant temperature	37500	46875	32750
Safety factor in terms of stress $= R_a/R$	2.72	2.52	2.35
Safety factor in terms of life $= (R_a/R)^5$	149	101	71

TABLE 8 MAXIMUM STRESSES DURING THERMAL-SHOCK TESTS

	80-schedule, 6-in. pipes		160-schedule, 6-in. pipes	
Tensile stresses at inner pipe wall, psi	Ferritic	Austenitic	Ferritic	Austenitic
Longitudinal thermal stress	26000	19000	32000	32000
Circumferential thermal stress	26000	19000	32000	32000
Longitudinal pressure stress	3000	3000	3610	3610
Circumferential pressure stress	6000	6000	7220	7220
"Equivalent" stress	30400	23600	37600	37600

the ultimate tensile strength and the code allowable stress are quite consistent for the three markedly different conditions for which data are available. This consistency provides justification for continuing the practice of relating thermal-expansion stress ranges to allowable pressure stresses or S-values.

To illustrate the point more clearly, the average stress range required to produce failure in 2500 cycles has been computed and compared with an allowable stress range based on a rule which would permit 1.25 times the sum of the hot and cold S-values; since all tests were conducted at constant temperature, the latter reduces to 2.5 times the S-value at the test temperature. This comparison indicates a safety factor of the order of 2.5 in terms of stress, and of 100 in terms of life. This would appear more than ample to the writer for noncorrosive service; under active corrosion, there is of course no endurance limit. An additional consideration in setting a proper stress limit, of course, is the rupture strength. While the authors' fatigue tests were of too short duration to provide any clues with respect to this factor, it would seem improbable in the light of their rupture-strength data that failure as a result of extended service at stress should occur with the proposed limits; the stress amplitude (one half the range) is only 16,375 psi, as compared with an extrapolated rupture strength in 100,000 hr of twice that value at 1050 F.

In conclusion the writer would like to suggest the initiation of full-scale fatigue tests in which the stress conditions would be produced directly by temperature variation, as is the case in actual practice. Thanks to the authors, engineers now have available high-temperature in addition to room-temperature fatigue-test data, and while these can be combined to produce a picture of the actual performance under temperature changes, experimental proof of the validity of any reasoned theory appears necessary for certitude. The writer fully appreciates that tests of the type he proposes could be quite time-consuming, but perhaps some arrangement for alternating automatically the admission of hot steam and cold air in a test at extremely high stresses may make a project of this type feasible.

J. J. Murphy[6] and N. A. Weil.[7] This paper is a valuable contribution to the limited literature on the action of piping systems subject to fatigue at elevated temperatures, and we hope will foster further research in this field.

The thermal-shock tests represent a good duplication of conditions which might be encountered in high-temperature piping systems of steam power plants under the action of boiler feedwater carry-over. It is felt, however, that a more representative condition would have been obtained with fixed instead of free-end pipes, inducing thereby additional thermal-expansion stresses.

As carried out, the thermal-shock tests impose a nearly balanced biaxial stress system on the surfaces of the pipe, these stresses being tensile on the inner face and compressive on the outer face during the quenching cycle. Based on previous progress reports[8] and using the von Mises-Hencky theory, the stress levels given in Table 8 were reached during the water-quenching cycle.

Table 8 may aid in explaining certain phenomena observed during the tests, such as the greater distortion of the ferritic assemblies of the 80-schedule specimens, and the more intensive crack formation encountered in the 160-schedule than in the 80-schedule assemblies.

The tensile stresses at the internal surface of the pipe exceeded the compressive stresses at the external surface during the quenching cycle, this difference becoming even more prominent by the addition of the tensile stresses. In view of the larger magnitude and tensile nature of stresses, it seems plausible to expect cracking to be initiated at and propagated from the bore of the pipe. Unfortunately, the problem of crack initiation cannot be resolved with certainty, since no examination was made of the bore of the assemblies during the present tests. However, the probability of crack initiation at the inside surface is enhanced through the results of some very simple cyclic thermal-shock tests, carried out by Holmberg,[9] who produced cracks radiating from the bore of short tubular specimens, although the thermal stress was developed by suddenly heating the inside wall of the cylinder.

Conversely, the cracking actually observed in the external surfaces of the welded regions can hardly be relegated to the biaxial stresses developed during quenching, in view of the mediocre level and compressive character of these stresses. A better explanation may be offered by considering the local differential

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[7] Development Engineer, The M. W. Kellogg Company.

[8] E. E. S. Reports 4C(1)17X1603 to 4C(7)17X1603, by W. G. Schreitz, et al., September, 1949, to October, 1951.

[9] Authors' Bibliography (2).

plastic flow developing at the location of peak stresses, and at junctions of materials having different mechanical and thermal properties; this would tend to induce cracking at the less ductile zones, notably the weld deposit or heat-affected base metal, as indicated by the tests.

The anomalous behavior of the temperature changes in the austenitic assemblies during the thermal-shock tests merits attention and should encourage further research; a liquid-film effect may explain only a small part of the unorthodox behavior encountered.

The mock-up tests follow more directly the lines of elevated-temperature fatigue tests. The significant part of the stresses is the "stress range," which corresponds to the differential displacement during cycling; the maximum initial stress imposed, corresponding to the constant portion of displacements, does not merit great importance, since the resulting stresses, if excessive, are reduced by local plastic flow. It is deemed, therefore, that more significant information could have been gained by focusing attention only on the cyclic portion of stresses, by means of a simple reversed cyclic fatigue test. Fortunately, sizable reductions took place only in the "equivalent mean stress," whereas the "equivalent stress range"[10] remained practically constant throughout the tests, as shown in Table 9 of this discussion; also as would be expected, the ferritic assembly exhibited a greater tendency toward stress relaxation than did the austenitic specimen.

TABLE 9 RELAXATION OF MOCK-UP ASSEMBLIES DURING FIRST RUN

		80-Schedule, 6-in. pipes		160-Schedule, 6-in. pipes	
		Ferritic	Austenitic	Ferritic	Austenitic
Equivalent mean stress	Initial	12600	17000	12900	16550
	Final	9300	12200	8000	13000
Equivalent stress range	Initial	6300	9500	6200	10700
	Final	5000	8700	5100	10000
Hours at stress and temperature..........		64	92	116	135

For piping systems operating at high temperatures, the ASA Code for Pressure Piping prescribes an allowable stress which is to be compared with the equivalent hot and cold stresses developed under thermal expansion. The code allowable stress is in reality an "allowable stress range," as is brought out more clearly in the proposed revision of the code now under consideration. To apply this allowable stress range as the maximum stress during the elevated-temperature test, while using the allowable "hot stress" for the equivalent stress range of the fatigue cycle, is certainly in excess of the demands imposed on a piping system due to heating and cooling as reflected by the code, especially when these "allowable stresses" were increased by the ratios of 1.25 and 1.67 during the first and second runs, respectively. It is, therefore, very reassuring to know that the piping assemblies performed as well as they did, both types of materials withstanding 4000 cycles each at 1.25 and 1.67 times the basic stress condition, and still having a considerable life left for the alternating cyclic fatigue tests.

The stress-level sequence of the mock-up tests is suggestive of a coaxing effect that may have taken place, especially in the case of the 160-schedule austenitic assemblies. Coaxing has been shown[11] to have a beneficial life-extending effect on materials subjected to fatigue conditions, if the successive stress-level increases (the coaxing) are sufficiently small. This may help explain the good performance of the 160-schedule austenitic assembly.

The advisability of selecting the stress-reference station at location *e-f* also must be questioned; it seems preferable to report stresses at the expected location of fracture. As shown in Figs. 8 and 9 of the paper, locations of maximum stresses should have been anticipated at the pipe bend joining the straight segment near the moving end of the mock-ups (location *m-n*), since the high moments developed there are aggravated by the stress concentration occurring in the bend.

These remarks are documented by Table 10 which shows that, if the stresses at location *e-f* are taken as a basis of performance, the austenitic assembly must be taken as the superior one. If, however, one draws into comparison the maximum stresses measured at location *m-n*, the austenitic assembly seems to be only slightly stronger, having withstood 3730 cycles at a 51,000 psi equivalent-stress range, as compared to the 54,800-psi range sustained by the ferritic assembly for 2160 cycles. Adding the fact that the austenitic assembly actually failed at location *m-n*, whereas the ferritic assembly failed in the cross section of gages *m-n* but at the bottom of the pipe bend (where the stresses must have been higher than at *m-n*), it becomes difficult to establish a clear-cut superiority of one piping assembly over the other.

It may be added in conclusion that the mock-up tests are not truly representative of the stress conditions existing in elevated-temperature piping systems, since with the latter the reversed stresses will occur only in the off-stream (cold) condition. The tests, however, do provide a clear picture as to what could be expected to occur if a highly stressed elevated-temperature pipe line were subjected to relatively small vibratory stresses (1st and 2nd runs), or if these vibrational stresses would become overwhelming as compared to the steady stresses (additional runs). In this aspect the tests provide a welcome addition to the knowledge in this field.

TABLE 10 COMPARISON OF STRESSES AT LOCATIONS *e-f* AND *m-n* DURING MOCK-UP TESTS

		Equivalent-stress range in psi at location *e-f*	Equivalent-stress range in psi at location *m-n*	Number of cycles
80-schedule ferritic assembly	1st run	6300	12400	4030
	2nd run	8200	16100	4270
	3rd run	28000	54800	2160
160-schedule austenitic assembly	1st run	10700	11600	4100
	2nd run	13300	14400	3970
	3rd run	30800	32850	4000
	4th run	36200	38700	880
	5th run	42000	44800	520
	6th run	47600	51000	3730

Ernest L. Robinson.[12] The writer would like to summarize in very round numbers the extent of the tests described in this report and the significance of the test results. Full-sized lengths of $2^1/_4$ per cent chromium, 1 per cent molybdenum piping, and 18-8 (Type 347) piping were tested under full steam pressure and temperature and given all the kinds of mistreatments that were anticipated as possible to occur in 20 years of naval service. (If, in what he has to say, the writer appears to criticize some of the test conditions as not having been severe enough to bring out the

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[10] The following meaning is attached to these terms:

Equivalent mean stress =

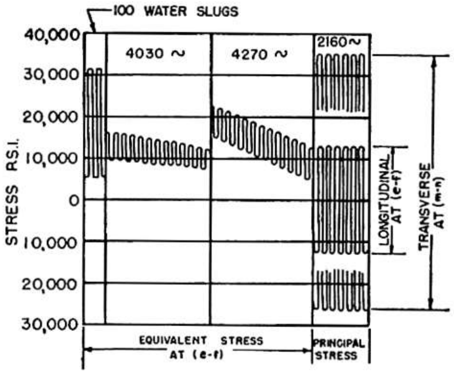
$$\frac{\text{Max equivalent stress} + \text{min equivalent stress}}{2}$$

Equivalent stress range =

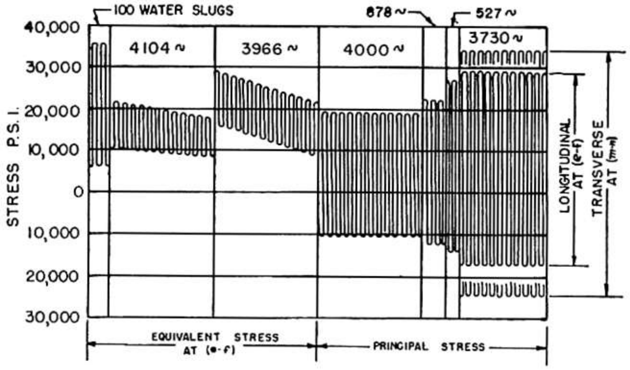
$$\text{Max equivalent stress} - \text{min equivalent stress}$$

[11] "An Investigation of the Coaxing Effect in Fatigue of Metals," by G. Sinclair, presented at the Annual Meeting of ASTM, June, 1952, New York, N. Y.

[12] Structural Engineer, Turbine Division, General Electric Company, Schenectady, N. Y. Fellow ASME.



2¼ % Cr. 1% Mo. 80 SCHEDULE AT 900 PSI, 1050°F.



18-8 (TYPE 347) 160 SCHEDULE AT 2000 PSI, 1050° F.

FIG. 31 TEST CONDITIONS OF AUTHORS' TABLES 1, 2, 3

full strength of the pipe, he has to admit having been one of the group who decided upon the test conditions.)

These pipes included welds in which the adjacent base materials were unlike, that is, the low-alloy ferritic material on one side and the high-alloy austenitic material on the other.

The pipes were given 100 slugs amounting to 8 or 10 gal of boiler water each. That was a much larger slug than was originally thought to be representative of carry-over.

Thereafter in the form of expansion loops, the pipes were wiggled back and forth through a deflection corresponding in amount to the allowable bursting stress permitted in the Boiler Code at the time the program was set up. This was done for 4000 cycles in each case, a number which was thought of as corresponding to lighting up the boiler every other day and letting it go cold every other day for 20 years.

After this test was completed, certain rearrangements were made and the amount of the wiggle was increased by a third and the pipes given an additional 4000 cycles.

Finally, in order to find out how much it would really take to break the pipe, the range of the wiggle was greatly increased until the stress range was approximately 60,000 of which 25,000 was compression and 35,000 tension. The low-alloy pipe stood 2000 such cycles in addition to the previous 8000 and the high-alloy pipe stood nearly 4000 such cycles in addition to more than 13,000 previous cycles.

This was the range of stress (whether longitudinal or transverse) in the region where the pipe finally leaked. In each case the leak occurred in a region where the moment was roughly 20 per cent greater than at the control station but roughly 20 per cent less than the maximum over at the fixed end where no leak occurred. The moment at the weld between unlike materials was a little smaller than at the control station.

The most important result of these tests is the fact that it took 10 times the 6000-psi allowable stress in the low-alloy pipe and 6 times the 10,000-psi allowable in the high-alloy pipe to cause failure and it would seem as if this ought to be very hopeful for the success of such pipe in the future.

The diagrams, Fig. 31 of this discussion, attempt to show pictorially the test conditions listed by the authors in Tables, 1, 2, and 3.

AUTHORS' CLOSURE

The authors greatly appreciate the efforts made by Mr. Markl to bring out the significant features in their paper as they relate to the design of high-temperature piping systems. It is also gratifying to learn of the correlation between his own room-temperature fatigue tests and high-temperature fatigue tests, reported on in the paper. The assumption that a certain percentage of fatigue life is used up at a given stress level may be open to question. There is evidence to indicate that materials like the austenitic steel, which is susceptible to strain hardening, are strengthened when tested in fatigue at increasing levels of stress.

The stresses at location *m-n* which were obtained by Mr. Markl from data given in the paper for location *e-f* are in good agreement with corresponding stresses computed by Mr. D. B. Rossheim from strain-gage measurements contained in the original Progress Reports which were available to him. This comparison is shown in Table 11, herewith. The stresses for location *m-n* are transverse stresses, whereas those given in the paper for location *e-f* are longitudinal.

TABLE 11 COMPARISON OF STRESSES

	As computed by D. B. Rossheim from strain-gage measurements		As given in discussion of A. R. C. Markl	
Test run	Max. stress, psi	Min. stress, psi	Max. stress, psi	Min. stress, psi
1	25800	14700	31650	17300
2	39100	22400	38900	20450
3	23400	—14500	25600	—14300
4	27000	—17600	29600	—17300
5	32400	—19200	35500	—18900
6	34900	—23900	38100	—23600

The mechanical method of loading was employed inasmuch as the original purpose of the test was to compare full-scale assemblies in austenitic and ferritic steels at temperature and pressure, and besides, the necessary equipment was available. However, the suggestion to initiate full-scale fatigue tests in which the stress conditions would be produced by temperature variation has merit. The temperature-cycle method would be better adapted for allowing sufficient time for significant thermal creep to occur. The ductility of certain ferritic steels, as, for example, the chromium-molybdenum-vanadium steel, is reduced when stressed and subjected to prolonged heating at temperatures in the vicinity of 1000 F. The temperature-cycle method also should prove useful for determining the effect of reduced ductility on the performance of full-scale assemblies in such materials.

The authors wish to thank Messrs. Murphy and Weil for their carefully prepared discussion of the paper. The comments are a real contribution, particularly in view of the fact that the paper was presented as a summary report.

In planning the test it was recognized that higher stresses would be induced by fixing the ends of the thermal-shock specimens but it was not considered that this procedure would be more representative of conditions in service. Therefore it was decided to isolate the effect of temperature shock by having the ends unrestrained. The equivalent stresses for conditions reached in the water-quenching cycle were not included in the paper, and as pointed out by Murphy and Weil, may aid in explaining the greater distortion of the 80-schedule ferritic specimen, and the more intensive crack formation encountered in the thicker 160-schedule specimens.

No inspection was made of the bores of the thermal-shock specimens as these provided the horizontal member of the mock-ups. Although the probability of crack initiation in the bores as a result of the shock treatment should not be minimized, it is believed that sudden heating from the inside, as carried out by Holmberg, would result in more drastic action, and produce cracks within fewer cycles. This would seem to be confirmed by the fact that internal surface cracks developed in pipes subjected to the periodic flow of hot oil after comparatively few heating and cooling cycles.

It was not the intention to imply that cracking in the external surfaces of the welded regions resulted from biaxial stresses during quenching but only that the oil-powder method of inspection revealed cracks which were not apparent prior to the shock treatment. The fact that external cracks were not revealed in the welds by the oil-powder method until after the thermal-shock treatment might indicate that the defects were potentially present as planes of weakness from the beginning, and that the stress conditions imposed by the test caused development into fine cracks. The suggestion that local plastic flow with peak stresses at junctions of dissimilar metals would tend to induce cracking in the less ductile zones is tenable. It was pointed out in the paper that welds joining austenitic to ferritic piping, 25–20 chromium-nickel in V-type joints and 19–9 Cb in transition-type joints, showed the greatest tendency to fissuring.

The authors agree that the anomalous behavior of the temperature changes in the austenitic assemblies during the thermal-shock tests merits further investigation. The liquid-film effect was suggested as a possible explanation.

The mock-ups were not subjected to a simple reversed cyclic fatigue test from the start because it was desired to have results which could be interpreted in terms of Code practice. The assemblies were finally tested to failure under completely reversed cycles of stress as this method gave greater assurance that the elastic range of the materials would not be exceeded.

Testing the austenitic mock-up at several levels of fatigue stress may have had a strengthening effect. However, tests of rotating cantilever fatigue specimens taken from the pipes gave substantially higher values for the austenitic steel. The endurance limit for the latter at 1100 F was 35,000 psi as compared with a corresponding value of 18,000 psi for the ferritic steel. The plan of test called for subjecting the mock-up to a level of reversed stress calculated to produce failure within 4000 cycles, starting with the 80-schedule ferritic mock-up. Actually, failure occurred after 2160 cycles. As a result, a somewhat lower stress level was selected for testing the austenitic mock-up than would have been done otherwise. Increasing the stress level in three stages may have contributed to the good performance of the austenitic assembly. The other two mock-ups are still available. Should these be tested at a later date, the experience thus gained will serve as a guide in selecting the fatigue stresses.

It was considered advisable to locate the control strain gages *e-f* in the straight section of horizontal piping where there were no stress concentrations. This would seem permissible considering that strain gages also were placed in the region of maximum stress, namely, location *m-n*. The tests, in addition to providing information concerning probable behavior under vibrating stresses, also give assurance of high-temperature piping systems under conditions that are in excess of demands imposed by the Code.

Mr. Robinson's running account of the test together with the excellent diagram illustrating the stress history of the specimens should inspire confidence as regards the performance of such pipe.