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REPORT NO. 648.3/1

SUMMARY

FATIGUE STRENGTH OF WELDED JOINTS

and

REPORT

FATIGUE STRENGTH OF WELDED JOINTS

A REVIEW OF THE LITERATURE TO JULY 1, 1936

BY

W. SPRARAGEN

G. E. CLAUSSEN

COMMENTS BY

H. C. MANN & W. L. WARNER

DECEMBER, 1936

WATERTOWN ARSENAL

WATERTOWN, MASS.

F. A. Form No. 156.

Frankford Arsenal—10-28-36—1,000

S U M M A R Y

FATIGUE STRENGTH OF WELDED JOINTS

By W. Spraragen* and G. E. Claussen**

*****

This report is a contribution of the Fundamental Research Subcommittee to the work of the Engineering Foundation Welding Research Committee.

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29 West 39th Street, New York

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*Secretary, Fundamental Research Committee
**Research Assistant, Fundamental Research Committee

December 2, 1936

WLW/emg

December 20, 1936

COMMENTS ON

SUMMARY

Fatigue Strength of Welded Joints

Spraragen and Claussen

In general, the data summarized in this report are very valuable and helpful to a designer.

On page 2 reference is made to correlation between fatigue strength and other physical properties. It is believed that such correlation will be impossible until such time as a fatigue testing method is devised to make possible a correlation of some sort. Whether this may ever be done or not is doubtful.

The conclusion with regard to interrupted welds appears rather broad in that this type of weld is stated to be lower in economy from the design standpoint than the continuous weld. From the manufacturing standpoint the continuous weld is lower in economy, especially so when considering the distortion problem.

On page 3 the reference to stress annealing states that the effect 'may be expected to be small'. One wonders where the expectation comes from and why. The effect of this treatment varies with the kind of material, type of weld, and the previous history of the material. Its effect is very marked in some cases.

W. L. Warner

S U M M A R Y

FATIGUE STRENGTH OF WELDED JOINTS

*****

CAUTION

This Summary is merely a condensation of the accompanying report which reviews information now available on the subject of the fatigue strength of welded joints. It is not intended to present conclusions broader than are warranted by the sources of information cited. Further experimentation is needed to amplify, and in some cases modify, the tentative conclusions now submitted. It should be noted that some of the tests were made with out recognition of all the variables which may seriously influence results.

EXPRESSING ENDURANCE LIMIT

There are two common methods of expressing the endurance limit of welds: (1) The Wöhler method, which plots stress against the log of cycles, the endurance limit being the stress at which the curve becomes horizontal; (2) The cycles method, which defines the endurance limit as the stress that a weld can withstand for an arbitrary number of reversals.

ENDURANCE LIMITS OF WELDS AND WELDED JOINTS

<u>Butt Welds (Arc and Gas)</u>. Endurance limits in rotating bending of 16,000 psi for bare wire, and 30,000 psi for covered wire, are common values. These values represent a weld endurance ratio of .60 and .90 as compared with the endurance limit of base metal (mild steel). Gas welds generally fall between these two limits. Direct stress (tension and compression) fatigue tests give about the same values as rotating bend, but in the test results available there is apt to be less difference between the bare and covered. The endurance ratio of welds in torsion fatigue is about 25% higher than in tension or bending fatigue, but the torsion fatigue limit is somewhat lower. In reversed bending, Roš and Eichinger state that the fatigue limit of welds is 1.4 times greater than in pulsating tension.

<u>Fillet Welds (Arc and Gas)</u>. "Stress raisers" play an important rôle and sometimes completely offset any differences normally expected between the various processes, kinds of filler materials, and, in many cases, between the types of joints Some general rules for reducing their effect may be offered. Avoid all sharp changes in section, whether in shape of fillets or joints, which would tend to concentrate stresses. As a result of such sharp changes, various types of strap-joint produce very little increase in fatigue strength as compared with a simple butt joint. Transverse fillet welds with covered electrodes (mild steel) have an endurance limit of 16,000 psi as compared with 60% of this figure for bare electrodes In both cases, the endurance limit of longitudinal fillets is apt to be 15% less than that of transverse fillets. Oxy-acetylene welds generally lie between the values given for bare wire and covered electrodes, and may approach the values of either, depending upon the type of wire, technique employed, and care with which the welding is done.

Tee Joints. Taking the fatigue resistance of a solid Tee section as 100%, that of an unchamfered fillet welded Tee is 72%, and of a Tee joint with edges chamfered to facilitate welding, 84%.

Tests of All-Weld-Metal. The fatigue strength of sound weld metal (except with bare electrodes) is equivalent to rolled steel of the same composition.

Welds at Elevated Temperatures. Preliminary results indicate that welds in fatigue tests at elevated temperature differ but slightly from unwelded mild steel.

PROCESSES OTHER THAN GAS AND METALLIC ARC

The fatigue properties of atomic hydrogen welds appear to be the same as for gas and arc welds. Resistance welds, however, seem to develop remarkably high fatigue values, especially in corrosive media. The torsion fatigue limit of flash welds in mild steel equals that of base metal (about 22,800 psi). Pulsating tension fatigue strength of carbon-arc welds in mild steel varies between 14,000 and 21,400 psi, depending upon quality of workmanship. Thermit welds, as indicated by tests of welded rail joints, appear to have reliable fatigue strength equivalent to gas and arc welds.

CORRELATION OF FATIGUE WITH OTHER PHYSICAL PROPERTIES

So far, a reasonably close relationship between fatigue strength of welds and other physical properties has not been found. There are indications that good static ductility aids in obtaining good fatigue value by relieving notch effect.

INFLUENCE OF DEFECTS

Internal Defects. The adverse effect of internal defects, of which faulty penetration is a special type, is accounted for by their influence in causing local stress concentrations. Internal defects, such as pores and slag inclusions, are almost universally admitted harmful to fatigue properties of welds. Their relative importance is not as yet evaluated, although for well-prepared welds their effect is generally considered primary only when more important factors have been eliminated.

Penetration. The most important type of internal defect from the standpoint of fatigue of welds appears to be poor penetration, that is, lack of fusion along the scarves and at the root of V and double V butt welds, as well as of fillet welds.

Interrupted Seams. From the viewpoint of fatigue, interrupted seams should be avoided. If the factor 0.6 is applied to the permitted stress in plate metal at the end of a weld, and 0.85 to a continuous seam, it is usually uneconomical to use interrupted fillet welds.

MECHANICAL TREATMENT

Peening. One investigator found that unmachined all-weld-metal deposited by bare electrodes gave 18,000 psi in rotating cantilever tests. This value was raised to 20,000 psi by peening.

Hot Forging. Hot forging is beneficial, but less so at temperatures of about 1200°C on account of increase in grain size. Hot forging increases the fatigue limit of back-hand gas welds 20%; of fore-hand welds only 10%. Increases of 75 to 100% in fatigue value were found due to forging of welds made with coated and cored electrodes (0.07 C, 0.65 to 2.8 Mn) in mild and low-alloy steels.

Machining. To date fatigue tests show that for medium and high quality arc welds, butt or fillet, in structural steel, intelligent removal of undercutting and other surface notches or reinforcement by machining, raises the fatigue value about 25%. In poorer quality welds with high inclusion content, machining appears to be of no advantage. One investigator has obtained 40% better pulsating fatigue value from parallel shear fillet welds, the inner ends of which had been machined (details not given) than from unmachined.

OTHER WELDING CONDITIONS

Scarf Angle. Scarf angle is important for fatigue value only insofar as penetration is concerned, and it is recommended that scarf angle be as small as possible consistent with good penetration.

V and X. Single V welds appear to have better fatigue properties in the as-welded condition. Double V welds appear superior when stress relieved.

Current and Reverse Run. Fatigue tests on welds made with different sizes of electrodes show variations that are probably to be ascribed to variations in workmanship. A reverse run (re-welding the root) raises the direct tensile and reversed-bend fatigue strengths in mild and alloy steel by 10 to 20%. The reverse run is important because it eliminates notch-effect at the root of the V, not because it refines the grain structure.

THERMAL TREATMENT

Full Annealing. Annealing (880 to 920°C) is detrimental to welds with medium or high nitrogen content, above about 0.04% N_2, but is beneficial when the nitrogen content is below 0.04%. The difference between the as-welded and the annealed specimens was never more than 3,000 psi, however.

Stress Annealing. The effect of stress annealing may be expected to be small.

Shrinkage Stresses. In welds with high ductility and yield point, interna stresses are quickly eliminated by plastic yielding under repeated loads. In brittl welds, shrinkage stresses lower the fatigue as well as the impact value.

CARBON CONTENT

Carbon content of the plate has only slight effect on the reversed-bend endurance limit, 21,400 to 22,800 psi, but the endurance ratio: (endurance of weld)/(endurance of plate) decreases from 0.4 with 0.1% C, to 0.2 with 0.7% C.

ALLOYS

Welds in low-alloy steels have acceptable fatigue value, but they possess little advantage over mild steel in fatigue, except at high values of superimposed tension.

For best fatigue behavior, weld metal and plate should have identical elastic moduli as nearly as possible, in order to minimize shear forces and stress peaks caused by cross-sectional contraction.

Other Alloy Steels. V butt welds by the atomic hydrogen process in plate containing 0.28-0.35 C, 0.5 Mn, 1.1 Cr, 2.0 Ni, 0.25-0.40 Mo, using the cantilever machine and a rod containing 0.47 C, 1.98 Si, gave a fatigue value in the weld of 25,000-35,000 psi. Fatigue value in general is a function of the composition of filler rod. The comparative fatigue value of chromium-molybdenum electrodes was higher than chromium-nickel, or 3-1/2% Ni electrodes, in plate containing 0.32 C, 3.4 Ni, according to one investigator using the Upton-Lewis reversed-bend machine. Welds in plate containing 3-1/2%Ni withstood 20 times as many cycles at 30,000 psi as plain medium-carbon plate, a low-carbon electrode (0.13-0.18% C) being superior to chromium-vanadium (0.89 Cr, 0.15 V), or nickel-chromium (1.0 Ni, 0.5 Cr) electrodes for both plates.

Austenitic Steels. The fatigue limit of spot welded 18-8 is estimated to be 26,000 to 31,000 psi, and of an 18-8 containing 0.1 C, 1.3 Ta to be 37,000 psi.

Cast Steel and Cast Iron. The rotating-bend fatigue limit of cast steel welds (bare electrodes) is 15,800 psi. The decrease in fatigue strength by welding is proportionately less than the decrease in tensile strength fer specimesn without cast skin. Annealing is not beneficial to fatigue properties. The cantilever fatigue limit of gas welds, 45° V, in 1-inch cast iron (3.46 total C, 0.74 combined C, 1.33 S, 0.106 Si, 0.66 Mn, 0.282 P) using cast iron welding rods, was 12,000 psi; the unwelded cast iron gave 13,500.

Brazing. Reversed bend fatigue limit of a brazed joint in mild steel 3/4" x 3/8" cross-section, was found to be 20,000 psi.

Non-ferrous Metals.

Rotating-Bend Fatigue Limits of Non-Ferrous Acetylene Welds

Material	Tensile Strength (psi)		Endurance Limits (10×10^6 Cycles)		
	Unwelded	Welded	Unwelded	Welded	Welded Annealed
Copper - - - -	39,000	17,700	12,100	5,700	6,400
Aluminum - - -	17,400	13,400	8,500	8,500	-
Silumin- - - -	19,500	6,250	7,800	10,700	5,000
Copper-Silumin	16,600	10,100	9,300	11,400	-

CORROSION FATIGUE

The results show that rotating bend fatigue limit of welds in mild steel in tap water is usually higher than in air.

METHODS OF DESIGN

Methods of designing welded structures on the basis of fatigue have been discussed on a number of occasions, especially during the past few years, and have been embodied in the national standards of Germany and Austria, and in important specifications in Switzerland and the U.S.A. The Germans have specified fatigue requirements for filler metal to be used in important specifications, such as railway bridges. A machined specimen double-V butt weld in mild steel must give a pulsating tension fatigue endurance limit of 24,200 psi, and in low-alloy steel, 25,600 psi.

The American Welding Society Bridge Specification permits design stresses in properly made butt joints welded from both sides when subjected to pulsating stresses from zero to maximum of 13,500 psi. When the stresses are alternating, only 2/3 of this value is allowed. There is a 15% penalty in design value in case of single V backed-up welds. In butt welds subjected to a pulsating shear from zero to maximum, a design value of 9,000 psi is allowed, which is again reduced to 2/3 if there is a reversal of stress. The same 15% penalty applies to single V backed-up welds.

Fillet welds subjected to either tension, compression, or shear are allowed 7,200 psi when the stress varies from zero to maximum, and 2/3 of this figure when the stress is reversed. Only a good grade of heavily covered electrode is permitted.

REPEATED IMPACT

Repeated impact tests were carried out in 1928 by the British Engine, Boiler, and Electrical Insurance Company. Welds free from oxides and nitrides gave the best results. Normalizing at 910°C had little effect. The surprising fact that a cast iron weld having less than 10% the single-blow notched-bar impact value of welds in mild steel is more resistant to repeated light impact than the latter, seems to be explained only by considering damping capacity. For surfacing plate and cast steel with low-carbon steel, gas was superior to the DC arc, and it is the deposit, not the heat-affected zone, that injures the repeated impact resistance. Flame-cut surface is equivalent to a milled surface, and only about 10% inferior to a planed surface in repeated impact for four types of structural steel. If the machining grooves were at a large angle to axis of impact, or if the flame-cut surface was subsequently ground, the original flame-cut surface had superior repeated impact value. On fillet arc welds, a model of such a weld machined from a single piece of steel, and a double-riveted joint, the welded specimens were equivalent to the riveted in repeated tensile impact, and were 20% better than the machined models which had equal static tensile strength.

CREEP

Limiting Tensile Creep Stress lb/in^2

Material	300°C	400°C	500°C
Mild Steel Plate - - -	31,200	15,600	5,700
All Weld Metal, gas- -	18,500	8,500	1,400
All Weld Metal, arc- -	19,900	9,900	2,800
Welded Joint, gas- - -	25,600	12,100	5,700
Welded Joint, arc- - -	25,600	15,600	5,700

Being a composite of plate and weld metal, the welded joint displays creep properties intermediate between them. Above 400°C, the welded joint is equivalent to mild steel. Creep rates in welded steam station piping at 850°F (455°C) determined by the single-step method are tabulated as follows:

Rate of Creep at 850°F, 15,000 psi tensile	Percent Per 100,000 hrs.
Pipe material (0.33 C, 0.75 Mn, 0.04 Al (metallic)) - - -	1.1
Welded pipe-to-pipe -	1.2
Welded pipe-to-casting (0.24 C, 0.62 Mn, 0.82 Cr, 1.19 Ni, 0.40 Mo) - - - - - - - - - - - - - - -	1.5

The welds were made by the shielded arc process and were drawn at 1100°F. The test results were not so consistent for the welds as for the unwelded pipe; the duration of the tests was 500 to 600 hours. At a stress of 12,000 psi there was no appreciable creep at 850°F. Summarizing, the creep strength of welds in mild steel is probably little, if any, inferior to unwelded plate up to 500°C, although the initial creep rate may be somewhat higher. Full annealing is not beneficial.

BOILERS

Pressure vessel fatigue tests show that fatigue failure inevitably occurs in regions of stress concentrations; e.g., gage plugs, manholes, and pads, rather than in the welded seam itself. The only unsatisfactory welds in all the fatigue tests were those made with bare electrodes.

RIVETING AND WELDING

Strengthening by Welding. The effect of strengthening by welding is not so great in fatigue as in static load conditions. Welding intended to strengthen riveted joints must be designed to take the whole load in order that plastic yielding will not take place in the neighborhood of the weld and lead to fatigue failure. The fatigue strength of welded and riveted joints do not differ greatly. High quality unmachined double V butt welds have higher reversed-bend fatigue strength than riveted overlapped joints.

BRIDGES AND MACHINERY

Riveted bridges strengthened by welding are stiffened, the natural frequency being increased 3 to 7% in the loaded and unloaded states. Welding decreases the damping factor, that is, the range of frequencies at resonance, and decreases stresses and deflections due to traffic. The advantages of welding in preventing vibration in machinery are connected with the higher modulus of elasticity of welded steel as compared with cast iron. The closed section, ideal for preventing vibrations, is easy to weld but difficult to cast.

TUBES

The fatigue value of welds in aircraft structural tubing has been investigated by rotating bend tests on individual gas butt welds. Values given vary from 14,000 psi for gas welded plain carbon and Cr-Mo tubing to 28,500 for plain carbon and 30,000 for Cr-Mo, depending on welding technique and penetration. Filler rods play an important part. Flash welds in Cr-Mo tubing gave 32,000 after stress annealing, but gave low values (13,000) in plain carbon. The reversed bend method with 0.11% C tubing gave 25,000 psi, 0.32% C, 29,000, and Cr-Mo 24,000 to 31,000, depending on heat treatment. For low carbon superheater tubing, the reversed bend fatigue limit was found to be about 15,000 for gas welds, but less than 10,000 for arc welds. Using the stationary cantilever type machine, the fatigue limit was found to be 25,000 psi for as-welded Cr-Mo tubing and 35,000 for heat treated; these values are, respectively, 1/4 and 1/3 the static tensile strength of the as-welded tube. The ratio of fatigue strength welded to that unwelded is in the neighborhood of 60% for all types of tests. In general, as the carbon (0.25-0.40% C), or alloy content (Cr, Mo, or Mn), of the tube is raised, the ratio of endurance limit to static tensile strength of the weld is lowered from 50% to 20%. Lap and fish-mouth joints appear to be at least as good as butt joints, but brazed, soldered, and bell-and-socket joints are definitely inferior. Pinned and riveted joints have only 50 to 80% of the fatigue strength of welds.

HCM/emg

December 29, 1936

COMMENTS ON

REPORT

Fatigue Tests of Welded Joints

(Review of Literature to July 1, 1936)

by

Spraragen & Claussen

The report of Fatigue Tests of Welded Joints presents a most complete abstract compilation of literature on the subject. Although an enormous amount of time was necessarily involved in the preparation of this report, its value would be greatly increased if all tests on a given type of machine, together with detailed information as to material analyses, electrodes, time, etc., were tabulated in separate sections. While the endurance strength of materials has become recognized as an important physical property, it should be borne in mind that the so-called fatigue strength of metals under various combinations of stresses such as shear and bending, shear and tension or compression, and under various ranges of stress, is not the same, and, therefore, the type of the test must be given due consideration in the final analysis.

It would appear from a general consideration of the data presented that the butt weld type of joint is superior to all others under fatigue stress conditions, although it is entirely possible that such results might be due to the form of test specimen used, and with this doubt existing, no definite conclusions regarding this point should be drawn.

It has been the general experience that unless the fatigue test assimilates actual service conditions, the results are of little value in predicting subsequent service behaviour. Furthermore, the forms of specimens used are entirely different geometrically than the part in service; therefore, the results from one would not necessarily apply to the other. From the fact that fatigue test results of very carefully prepared specimens of homogeneous materials cannot be conclusively correlated with any other physical property, and that an entirely different conception of the material behaviour can be obtained from tests on different types of machines, it is not at all unexpected to note the extreme differences of opinions which are evident, particularly when the relatively low order of perfection attained in normal welding procedure is considered.

It would seem that the principal value of this report is that it offers a very forcible argument against the use of the fatigue test as a means of obtaining information of value for design purposes. This report also brings out quite clearly the need for more systematic procedure in future investigations, particularly with regard to the type of electrode, the welding process, the composition, thickness and width of plate used, etc. Without a systematic program of test, the results are merely a confused mass of data from which little information of value can be derived.

It has been brought out by Professor A. V. DeForrest, Mass. Institute of Technology, that the cause of subsequent fatigue failure is present in the material before it is ever put under stress, and that this factor can many times be revealed by the magnaflux method of examination. Since this method is not subject to the wide variations of the fatigue test, it is suggested that it offers possibilities toward obtaining the maximum efficiency in welding technique, which in the final analysis is the deciding factor.

Respectfully submitted,

H. C. Mann,
Senior Materials Engineer.

WLW/emg

November 17, 1936

COMMENTS ON

REPORT

Fatigue Tests of Welded Joints

(Review of Literature to July 1, 1936)

by

Spraragen & Claussen

This report is indicative of a tremendous amount of painstaking work on the part of the authors, for which they should be complimented. However, there are numerous statements in the report which should be explained more clearly. Comparisons of data obtained by various investigators do not mean a whole lot unless all of the welding details are given, or it is known that the welding method has been used suitably. Inferences may be made from some of the data presented which are not generally correct.

Page 5

A comparison is made between bare and covered electrodes from tests of transverse fillets and a figure for endurance limit of 16,000 psi. is given for covered electrodes. It is not explained as to what this figure refers.

Page 7

At the bottom of the page the figures given for relative fatigue resistance of various joints are:

Solid T Section	100%
Unchamfered Fillet T	72%
Chamfered Fillet T	84%

Details of the test are not given. Therefore, these figures are comparative only and show the relationship under a certain applied load. This relationship may very well change under other methods of load application.

Page 8

Reference to results by R. R. Moore in which the oxy-acetylene weld metal shows 20% higher endurance ratio than bare wire or atomic hydrogen, weld metal is meaningless because no details of filler rod or electrode are given. The inference is that bare wire weld metal and atomic hydrogen weld metal are similar in fatigue. This is not true.

Page 9

General data given without giving also details of the welding procedure used is not satisfactory. The variables entering into the various test methods used by different investigators affect the comparison of results.

What is wanted mostly is data showing the effect of details of welding procedure and variations in that procedure.

Page 11

There are so many different methods of fatigue testing used that results cannot be found in general agreement between investigators. A study of methods of fatigue testing whereby the various physical properties of materials are brought into play during the test would be desirable, if it were possible.

Page 12

It is of interest that the German specifications permit of undercut up to 5% of plate thickness. This is difficult to determine.

Page 15

Fry's conclusion that nitrogen content, oxygen content, and microstructure are more important than internal defects in the weld metal is interesting. This point is not checked by other investigators.

Poor penetration is singled out as the worst type of internal defect in its effect on fatigue strength. Poor penetration includes lack of fusion on the scarf and at the root.

Page 16

The suggestion that interrupted weld seams or intermittent welds should be avoided as low in fatigue strength is contrary to usual structural welding practice where this type of joint is used to avoid excessive distortion when the structure is being built. It would seem that the length of weld increments and spacing as well as size of fillet and method of laying in the weld would have an effect on the fatigue resistance of the structure. No data bearing on such points are given.

Page 20

The beneficial effects obtained by machining the weld surface to remove surface defects is to be expected and this is one of the biggest arguments against using a machined specimen for testing welds. In many cases machining would not be possible and it would be of interest to get data on the effect of welding up these defects so as to remove them rather than by machining them out and reducing the cross-section of the joint.

Apparently none have attempted to do this.

<u>Page 24</u>

The detrimental effect of the weld reinforcement advocated here is undoubtedly encountered in tests of straight specimens, but it is doubtful if this effect would be harmful in a pipe joint or a tank seam, for example. We have found that the reinforcement on a butt weld in the tension impact test may reduce the impact strength of the joint, even though fracture takes place through the plate.

<u>Page 25</u>

Effect of scarf angle on fatigue strength as indicated here appears to be similar to the effect found at Watertown Arsenal in tests of butt welds in tension. We have found that the narrower the groove an alloy steel plate, the higher the tensile strength, as long as good weld penetration is obtained. Hence, we use a 30° bevel single V.

Jennings' results are for machined round test bars in which much of the effect of bevel is eliminated.

<u>Page 27</u>

The reference here to "reverse run" is probably what we call "seal bead". It is to be expected that a "seal bead" would increase the fatigue resistance of a welded joint.

<u>Page 29</u>

The effects shown here of weld beads on plate surfaces on the fatigue strengths of the plate itself is an indication of the value of "heat effect" studies on structural steels. The results are very startling and should present some idea of the type of information which can be obtained by studying the effect of weld beads deposited on flat plate surfaces, even though there is no welded joint present.

Page 30

The data by Schaesterle show that low alloy steel is no better than mild steel in fatigue after welding has been performed on it. This is a startling conclusion and based on tests of "as welded" specimens. The heat effect of welding on the alloy steel plate is the logical reason for the impairment of fatigue strength.

Pages 35 & 36

It is stated that "stress annealing" does not benefit the fatigue strength of welds. In accepting a statement of this kind we are assuming that the heat treatment procedure is proper. The only references to the treatment used are contained in the last paragraph on page 35 where it is stated, "stress annealing 1/2 hour at 600°C" and "stress annealing at 500-600°C".

The first procedure is inadequate as we have found from actual shop experience and the second reference is meaningless because no time of hold is given. On page 36 a temperature of 650°C is mentioned but the time of hold is not given.

The tests were in many cases made with machined specimens which in itself would affect the stress condition. Hence, it is no wonder that there is a lack of agreement between investigators.

Page 41

On this page near the middle is to be found the following statement "Welds in steels with 0.24% C have 20 to 30% lower fatigue values in direct tension than steels with 0.16% C and the same static tensile strength procured by alloying".

The first part of this statement is simple to understand and is along the lines suggested by our studies of heat effect and tension impact properties of low alloy steels. The last part of the statement however, is not clear but is taken to mean that the 0.16% C steels will produce the same tensile properties as the 0.24% C steels by the addition of suitable alloys, and the fatigue strength will be 20 to 30% higher than the 0.24% C steels. This is further proof that weldability of alloy steels is mainly a function of carbon content.

Page 45

Orr's data in the table shows slight improvement in fatigue due to stress annealing at 600°C for 1/2 hour. This treatment is insufficient to show much effect.

Page 47

In discussing suitable alloy compositions in the last paragraph on this page molybdenum is not mentioned and neither is nickel. The carbon content is not considered.

Page 47 con't.

This question of composition presents a big problem beyond the scope of one investigation into the phenomena of fatigue. This is a problem of fundamental metallurgical research. Results from various investigators cannot agree so long as the viewpoints on methods of fatigue testing vary as widely as they do. This is a problem for Professor Sayre's committee.

Page 48

Reference to work of McManus and Barnes with Upton-Lewis fatigue machine pertains to bare and wash-coated electrodes. No covered electrodes were used.

Pages 48 & 49

Reference to austenitic weld metals at bottom of page 48 and top of page 49 does not show any great improvement over other alloy electrodes.

Pages 58 and 59

References to alpha and gamma factors do not show how these factors are obtained from the diagrams in Figures #14 and #15. The references are not clear.

Note: - There is one striking point to be observed from this report. Most references to research workers on repeated impact and flexure fatigue show a predominance of German and other Europeans engaged in these studies. Perhaps the American attitude is that fatigue testing is a waste of time and money.

Page 86

In reference to fatigue strength of double-riveted joints on mild steel plate the following figures are given:-

Net cross section of plate	25,600/28,400
Gross cross section of plate	21,400/22,800

It is believed that the figure based on gross cross section should be used since that is the basis used for calculation of a butt welded joint. In calculation of weld strengths no allowances are made for the heat affected zone. Strengths are calculated based on gross cross-section of the plate joined.

Pages 87 & 88

From the discussion presented here it is evident that considerably more effort is being directed in Europe to testing full size structures and joints under accelerated service conditions than in America. This is particularly true of bridges where the European engineers have gone farther than American engineers in applying welding.

Page 90

The reference to an observation by W. L. Warner as to damping of vibrations by a poor weld is misplaced. The observation referred to is, in fact, that found in a quotation in a paper by me. The quotation was from a report of Mr. H. C. Forbes to the Welding Committee of the Emergency Fleet Corp., and in this report Mr. Forbes made the suggestion quoted. The idea was only suggested. No actual tests were made.

Page 93

In the next to the last sentence of the first paragraph the reference to carbon and alloy contents of 0.25% C and up for alloy steels being detrimental to fatigue ratios is a confirmation of our ideas regarding weldability of alloy steels.

This point is again referred to at the bottom of page 93 and top of page 94.

Page 94

Last sentence of first paragraph - "Air hardening has practically no effect on fatigue cracking in Cr-Mo welds".

This statement is very difficult to believe and needs some explanation.

Appendix B, Page 2

Last sentence of first paragraph makes reference to the fact that in pulsating fatigue fillet welds made with bare electrodes are equivalent to fillets made with covered electrodes. This is a very interesting conclusion, if true. These tests were apparently made on large size specimens in a machine of some sort. Under tension impact loading we have found the covered electrode much superior to the bare electrode.

Appendix B, Page 3

Near the bottom of the page occurs the following statement:- "The shape factor cannot be altered by using stronger steels for welding but can be favorably affected by using more ductile electrodes. Large specimens showed the same trend as the small but had lower fatigue limits".

This conclusion appears significant and possibly could apply as well to impact properties although a change of steel compositions will alter the impact strength of the welded joint.

General Note

In many of the references to researches data on details of procedure are lacking. This lack of data renders any worthwhile conclusions impossible.

Respectfully submitted,

W. L. Warner

FATIGUE TESTS OF WELDED JOINTS

A REVIEW OF THE LITERATURE TO JULY 1, 1936

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This report is a contribution to the work of

THE ENGINEERING FOUNDATION WELDING RESEARCH COMMITTEE
29 West 39th Street, New York

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INTRODUCTION

Fatigue, or repeated loading, of welded parts is quite common and sometimes dangerous. Developments in welding rods and welding design are toward increased fatigue strength rather than increased static strength. This non-partisan review is intended primarily to show progress as indicated by experimental and service studies of welds under fatigue conditions.

Welds have been tested in almost every type of fatigue machine. Most of the investigators have used the rotating-bend type with four-point (Farmer), or three-point(Foeppl), loading. Neither of these types of tests approximates fatigue conditions generally found in service. This has led many of the European investigators to use machines which apply direct tension and compression. The cycle of loading may consist, for example, of (a) alternating stress; (b) pulsating tension; (c) alternating with superimposed static tension; (d) pulsating with superimposed static tension.

These various types of tests have led investigators in Germany to coin a word which literally translated is "origin" fatigue. Actually this represents the endurance limit under stress in one direction, that is, not alternating from zero to a maximum. A brief discussion of the various methods of testing and the types of machines used for each type of test is given in Appendix A.

EXPRESSING ENDURANCE LIMIT

There are two common methods for quoting the endurance limits of welds: 1. the Wöhler method using a plot of stress versus log cycles, the endurance limit being the stress at which the curve becomes horizontal, usually about 2×10^6 cycles for homogeneous welds or 5×10^6 for defective welds in alternating tension and compression, or 10 to 20×10^6 in rotating bend for welds in steel; 2. the cycles method; that is, the fatigue limit is arbitrarily stated to be the stress that a weld withstands for, say, 10^6 cycles. The former is, of course, preferable, but the latter is often used for low-frequency tests. That weld-metal has a clearly defined endurance limit was shown by R. R. Moore(1) whose gas weld deposits withstood over 700×10^6 cycles of reversed bending at 23,000 psi without failure. Dutilleul(2) tested two rotating cantilever specimens (V welds in plate having 71,000 psi tensile strength made with an electrode giving a minimum fatigue limit of 18,500 psi) at 15,600 psi. Both withstood 500×10^6 cycles without rupture, although numerous blow holes were revealed by microscopic examination of the weld.

ENDURANCE LIMITS OF WELDS AND WELDED JOINTS

Butt Welds (Arc and Gas)

In this country most of the tests have been made on the rotating bend type of machine. Endurance limits of 16,000 psi for bare wire and 30,000 psi for covered wire are common values. These represent an endurance ratio of .60 and .90 as compared with the endurance limit of parent material (mild steel). Gas welds generally fall between these two limits. It should be noted that in Germany endurance values of 34,100 psi have been obtained for gas welds in mild steel representing an endurance ratio of .86 as compared with parent mild steel plate, and that the bare wire welds are more apt to range from 20,000 to 25,000 psi.

In some alloy steels of good weldable quality these endurance ratios are likely to remain in force but the endurance limits will naturally be higher.

In reversed bend tests (not rotating) the endurance limits and ratios compared with parent steel are about the same as in the rotating bend, providing specimens are carefully machined. Unmachined welds are likely to develop from 10 to 20% less depending on undercutting, and whether or not reinforcement is gradual. The effect of annealing, and mechanical working is discussed elsewhere.

Direct stress (tension and compression) fatigue tests give about the same values as rotating bend, but in the test results available there is likely to be less difference between the bare wire welds and the coated electrodes. This may be due to additional variables introduced by the investigators, as, for example, the matter of residual stresses, or to the care with which the welds are made. The slightest imperfections, or lack of penetration at the root of the weld, or undercutting at the surface, will cause a greater variation in the results obtained from two specimens with the same type of electrode than between different types of electrodes, or processes of welding.

Although the matter of electrodes is covered in a separate section, it should be definitely noted that not all types of bare wires or heavily coated electrodes, or even gas welding wires, give the same results. It is unfair to average good results obtained with one type of covered electrodes with the bad results of another type. The same is also true of bare wire, or gas welding.

The authors note with a great deal of interest that the average results obtained with bare wire welds in foreign countries are likely to be better than the results obtained with bare wire welds in this country insofar as endurance limit is concerned. The reverse is true for covered electrodes. In the latter case the difference may be due to differences in steels, and techniques as well as to possible superiority in the type of coverings. The difference would be a fruitful field for further research.

The endurance ratio of welds in torsion fatigue is about 25% higher than in tension or bending fatigue, but the torsion fatigue limit is somewhat lower. Tabular results are given in Appendix B. Haigh[3] and Dustin[4] believe that butt welds of ordinary good quality in mild steel plate have about one-half the fatigue strength of the plate itself. In reversed bending, Roš and Eichinger[5] state, the fatigue limit of welds is 1.4 times as great as in pulsating tension.

Fillet Welds (Arc and Gas)

The tests of fillet welds in fatigue have received considerable attention by foreign investigators as have also various types of joints employing fillet welds. It is difficult to draw general conclusions. Many variables are apparently unavoidably introduced which mask the results of the problem under consideration. "Stress raisers" play an important rôle and in many cases offset completely any differences which one may normally expect between the various processes, kinds of filler materials, and, in many cases, between the types of joints. Some general rules may be promulgated.

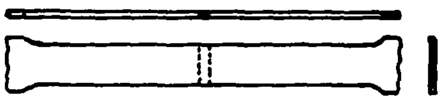
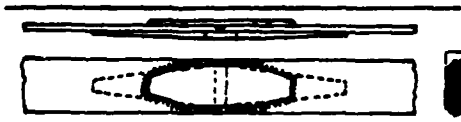
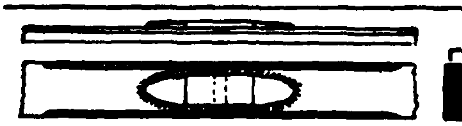
Avoid all sharp changes in sections whether in shape of fillets or joints which would tend to concentrate stresses. For this reason various types of strap joints produce very little increase in fatigue strength as compared with the simple butt joint. There is need in this country for tests of large size butt and fillet welds in fatigue. In comparing the static tensile strength of various types of welded joints, investigators agree that these results are no indication whatever of their fatigue resistance. Graf[6] shows that rough cover plates attached by fillet welds actually weaken a butt joint. In butt and fillet welds alike it is important to provide gradual transition of section, smooth surfaces, proper penetration, and sound metal free from inclusions and cavities. Transverse fillet welds with covered electrodes have an endurance limit of 16,000 psi as compared with 60% of this figure for bare electrodes. In both cases longitudinal fillets are likely to be 15% less than the transverse fillets. Oxy-acetylene welds generally lie between the values given for bare wire and covered electrodes and are apt to approach the values of either depending upon the type of wire, technique employed, and care with which the welding is done.

A great deal of caution must be observed in comparing values obtained from any specimen, large or small, in the laboratory with expected results in the field. For example, in the laboratory even with a large specimen the relative space occupied by the cover straps, or overlapping portions of lap joint is large as compared with the same joint, say, on a ship. This would tend to exaggerate the importance of some of the "stress raisers" encountered in a laboratory as compared with practice, although their importance cannot, and should not, be neglected. It must be remembered that in many cases in practical design the alternative is not a welded joint instead of a solid plate, but a welded joint as against a riveted joint.

A number of general recommendations regarding shape have been proposed. Thus, cover plates tapered to meet the plate are good but plates tapered to a point offer no advantages although the weld area is increased 26%, according to Roš and Eichinger.[7]

Pulsator tests reported by Witt[8] show that cover plates with parallel shear deposits are not nearly so harmful as plates with normal shear welds, as the following table shows. The fatigue fractures in specimens III and IV occurred at the normal shear fillet welds as shown by the wavy line in the diagrams. In Specimen II, fracture started in the fillet weld or at the inner edge of the butt weld.

Pulsator Tests on Butt Welds with Cover Plates

		Specimen	Welded	Probable pulsating tension Fatigue strength psi
	3.6×0.40	I	Coated Electrode	20,000-21,400
	3.6×0.67 2.8×0.40	II	Stabilend electrode	18,500-20,000
	2.8×0.48 3.6×0.40 2.8×0.28	III	Kjellberg OK 37	12,800
	3.6×0.67 2.8×0.40 2.2×0.28	IV	"	12,800

The relatively good fatigue qualities of butt welds with parallel-shear cover plates has been shown by Memmler, Bierett, and Gehler,[9] but has not been noted by Graf[6] nor by Schick,[10] nor in service[11]. In boilers the unequal expansion between such so-called strengthening plates and parent metal is an additional factor in hastening fatigue failure.

Bierett[12] gives the following summary for insuring good fillet welds:

1. The ratio of plate to seam cross-section should not be greater than 0.4 to 0.5.

2. For the same seam cross-section short thick seams are better than long thin seams.

3. The more nearly the plate and straps approach the square as compared to the rectangular cross-section, the better is the fatigue value.

4. Channel-iron straps permit thicker seams and hence are better in fatigue. Angle-iron straps are not recommended.

5. To make seam ends less susceptible to fatigue they should be rounded (see section on machining).

The relieving of fillet welds is not recommended by Graf[6] but Schick[10] has shown that some types of relieving contribute slightly (10%) to fatigue strength of fillet but not butt welds. Graf[13] showed that as the ratio: $\frac{\text{stress in straps}}{\text{stress in plate}}$ for parallel shear fillet welds decreased from 1.1 to 0.5 the pulsating tension fatigue limit was increased 100%. Below 0.5 there was no further improvement.

T Joints

The best way to improve the pulsating tension fatigue value of T joints is to taper the leg of the T as shown by Thum[14] and Graf[6]. Fig. 1. A well prepared T joint with tapered leg is equivalent to a butt weld in tensile fatigue and this should be borne in mind when considering the low values reported by Thum and Lipp[15] in reversed bending.

Roberts[16] gives figures for the relative fatigue resistance of a solid T section (100%) and unchamfered fillet welded T (72%), and a T joint with edges chamfered to facilitate welding (84%).

Profile or ribbed plate to avoid T joints of web to tension flange of welded beams is supplied by several German steel works (Fig. 2). Tests by Bühler and Buchholtz[17] having shown that fatigue fracture started in the weld on the upper (less highly-stressed) side of the tension flange, it was believed that simply inserting the web in a profile plate without welding would remedy the trouble. Earlier tests by Schulz and Buchholtz[18] demonstrated that beams constructed of profile plates gave 28% higher fatigue strengths and were superior in bend-fatigue to riveted beams of the same static strength. Bierett[12] showed that stiffeners in welded T beams need not and should not extend into the tension area of the beam. (Consult section on Service Tests for further information.)

Tests of All-Weld-Metal

The fatigue strength of sound weld metal is equivalent to steel of the same composition, as R. R. Moore[1] showed in 1927. In his tests all-weld-metal deposited by oxy-acetylene had about 20% higher endurance ratio than metal deposited by bare electrodes or atomic hydrogen. As stated earlier in this review, one of his all-weld-metal specimens withstood over 700 x 10^6 cycles at 23,000 psi without failure. The rotating bend fatigue value (up to 50 x 10^6 cycles) of all-weld-metal deposited by the atomic hydrogen process was reported by Weinman[19]. The highest value was obtained with a filler rod containing 0.46 C, 3.4 Ni whose fatigue limit determined on a machined specimen was between 35,000 and 40,000 psi. Unexplained wide differences between practically identical low-carbon filler rods that were obtained by him may have been caused by differences in degree of soundness. Hankins and Thorpe[20] found that the rotating bend fatigue limit (25 x 10^6 cycles) of all-weld-metal deposited by a high-grade covered electrode was 18,400 psi whereas an unwelded mild steel of the same static tensile strength (58,000 psi) attained 26,900 psi. The low value for the weld metal is attributed to blowholes and inclusions which have little effect on static strength.

Welds at Elevated Temperatures

The only fatigue tests of welds at elevated temperatures have been made by Lea and Parker[21], who tested machined welds (70°V) 0.48" deep; 0.62" wide in a reversed bend, constant bending moment machine at 1,000 cpm at 250 and 450°C, 10×10^6 cycles criterion. The welds were made with reverse run with a covered, shielded arc electrode (analysis not given) in mild steel plate (61,500 psi tensile strength). These investigators also found that understressing raised the apparent fatigue limit of welds.

Temperature ° C	Fatigue Limit psi
20	23,300
250	27,300
450	23,500

Lea[22] is also performing fatigue tests on mild steel welds under slowly repeated cycles of stress at boiler temperatures. Preliminary results indicate that welds in such tests do not differ appreciably from unwelded mild steel.

Notched-bar Fatigue Tests

The only investigator of the notched-bar fatigue strength of weld metal has been Leitner[23], who used the M.A.N. reversed bend machine, 10×10^6 cycles criterion. The specimens were 0.59" x 0.18" x 8" long and were machined from welds in mild steel (details of notch and electrodes not given.)

Electrode	$\%O_2$	$\%N_2$	Yield Point psi	Tensile Strength psi	Elong %	Red. Area %	Notch Impact Value mkg/cm^2	Fatigue Limit, psi Polished	Fatigue Limit, psi Notched	Fatigue Limit, psi Decrease
Coated	0.033	0.056	61,000	73,000	27.8	57.5	14.2	39,500	31,000	21.3%
Coated	0.052	0.067	56,000	71,000	19.9	44.9	8.2	28,400	25,800	14.0%
Cored	0.013	0.065	56,000	73,500	19.5	43.8	4.8	31,500	30,600	2.7%

PROCESSES OTHER THAN GAS AND METALLIC ARC

The published information on fatigue for the remaining welding processes is so scanty that general conclusions cannot be reached. The fatigue properties of atomic hydrogen welds appear to be the same as gas and arc welds, according to results given by Weinman[19], Thornton[24], Harvey and coworkers[25], Dorrat[26], and Becker[27]. Resistance welds, however, seem to develop remarkably high fatigue values, especially in corrosive media, as Harvey[25] has shown. Thornton[24] found 25,000 to 27,000 psi for resistance-butt and AC flash welds (rotating beam, mild steel) and Vér[28] found 32,800 to 35,600 psi in low carbon steel (0.05-0.08 C, 0.3-0.4 Mn) also in rotating bend. Rosenberg[29] quotes tests by Behrens who showed that the torsion fatigue limit of flash welds in mild steel was equal to that of parent metal (about 22,800 psi). Baumgärtel and Heinecke[30] also obtained high values of rotating-bend fatigue strength (43,000 to 67,000 psi) in highly-alloyed exhaust-valve steels flash-welded.

Pulsating tension fatigue strength of carbon-arc welds in mild steel, according to Wallmann[31], varies between 14,000 and 21,400 psi, depending on degree of workmanship. Thermit welds appear to have reliable fatigue strength equivalent to gas and arc welds, as tests of welded rail joints indicate (see section on Rails). The oldest welding process, hand-forging, has not been extensively studied in fatigue, practically the only information being given by Stanton and Pannell[32] in 1911. Their rotating-bend cantilever tests, of comparative value only, showed that hand-forged and butt resistance (Thomson process) welds were practically equivalent to mild steel and wrought iron; gas welding, at that time a new idea, was not nearly so good. Laboratory fatigue studies of water-gas welds have not yet been reported.

CORRELATION OF FATIGUE WITH OTHER PHYSICAL PROPERTIES

The presence or absence of correlation between the various physical properties of structural elements such as welds, often provides an indication of the nature of defects. Up to the present time a reasonably close relation between the fatigue strength of welds and any other physical property has not been found.

The overwhelming majority of investigators, particularly Otte[274], report no relation between the fatigue properties of welds and the usual static and impact properties such as yield and tensile strength, ductility in tensile and bend tests, and tensile and notch-impact value. The National Physical Laboratory, England, for example, state[3] in their Report for 1934 that the static tensile test is of no real value for assessing the fatigue value of welds. There are indications that good static ductility aids in obtaining good fatigue value by relieving notch effect, as Lohmann[34] points out. Graf[6] and Bierett[12] also state that welding rods having high ductility (20% elongation) and a pronounced yield point give good fatigue values especially in welds stressed along their axis, but the relation is by no means close. Wadling[35] believes that a high ratio of yield point to tensile strength is important for good fatigue properties.

Schulz and Buchholtz[18] found that the relation between pulsating tension fatigue strength and static tensile strength was roughly linear for machined welds in a number of structural steels; this, of course, was not true for unmachined welds. Hoffmann[36] stated that there was a close relation between certain static and impact properties and fatigue strength of welds, but his own results did not substantiate his conclusions. X-Ray examination, according to Wallmann[31] and Bierett[12], should not be too greatly depended upon as an indication of fatigue value. As stated in "Impact Tests of Welded Joints," there is no clearly-defined relation between repeated-impact value and other physical properties. There is no relation between fatigue strength and repeated impact value as Roš[37] and Bartels[38] showed.

In view of the failure to detect correlations, little success is to be expected from formulas by means of which fatigue strength can be computed from other physical properties (see section on specifications for purely empirical formulas). As early as 1919, Stromeyer[39] applied his general formula to Abell's results[40] on welds but the attempt was unproductive. Pester and Schulz[41] and others have shown that existing fatigue formulas are of no great value for welds. Erber[42] suggests that his formula for notch fatigue strength may be applied to welds, but has not yet so applied it. The formula indicates that the fatigue value of welds rises with ductility, and that the fatigue strength of welds is fundamentally a notch fatigue strength.

Credit for the investigation of the large effect of undercutting and other stress-concentrating effects at the junction between weld metal and plate must be given largely to German investigators. Among the first clearly to demonstrate the effect was Jünger[43] in 1930 who studied V, lap, and T welds. A complete investigation has been made by Graf[6] whose micrographs showing fatigue cracks originating from microscopic notches are very convincing. Decreases of as much as 40% in pulsating tension fatigue strength are ascribed to these notches. The removal of the notches, explains the beneficial effect of machining, but careless transverse grinding of a weld may develop, rather than remove, undercut. The German specifications permit undercut to the extent of 5% of plate thickness. Mailänder and Ruttmann[44], Shepherd and Moritz[45], and Lea[46], emphasize the general significance of the surface quality of welds on fatigue strength and Driessen[47] observes that fatigue failure of welded structures in pulsator tests invariably starts at the junction between surface of weld and plate. This is also the observation of the majority of investigators, particularly of fillet welds. Hankins[48] notes the effect, but also finds that the roots of fillet welds are sensitive to fatigue failure.

Recommendations for obtaining a gradual transition from surface to plate are given by Graf[6] and Bierett & Grüning[49]. Gas welding and coated electrodes give more gradual transitions than bare electrodes, and, in fillet welds, an angle of 30° between surface of weld deposit and plate is better than 45°. A smooth, broad, low deposit in butt welds is better than a rough, narrow, high deposit. Fig. (3) by Bierett[12] shows the types of loading in which undercut notches should be or need not be removed by machining. The bend fatigue tests of Dumas[50] at 10 to 12 cpm on V butt welds in mild steel also showed that fatigue cracks usually start at the junction between plate and weld metal.

INFLUENCE OF DEFECTS

Internal Defects

The adverse effect of internal defects, of which faulty penetration is a special type, is largely accounted for by their influence in causing local stress concentrations. Internal defects, such as pores and slag inclusions, but excluding poor penetration which is the subject of the next section, are almost universally admitted to be harmful to the fatigue properties of welds. Their relative importance is not yet evaluated, although for well-prepared welds their effect is generally considered to be primary only when other more important factors have been eliminated.

The adverse effect of porosity and inclusions in bare-electrode welds is considered by Jennings[51] on the basis of laboratory fatigue tests to be more important than design of specimen. Even with high-class covered electrodes Hankins and Thorpe[20] explain the low fatigue value of welds by the stress raising effect of inclusions and small blow holes. H. F. Moore[52] and Peterson[53] also note the decidedly disastrous effect of internal defects. The observation that slag inclusions and blow holes cause service fatigue failures in welds was made as early as 1926 by Schottky[54] and has also been made by Kautz[55] (boiler welds), Pohl and Ehrt[56] (surfacing layers), and Bauer[57] (water-gas welds) among others. Lohmann and Schulz[34] found that fracture followed blow holes in rotating- and reversed-bend fatigue tests of welds made with bare or coated electrodes, and Matting and Oldenburg[58] made the same observation in pulsating tension fatigue tests. The surface porosity of bare-electrode welds was held to account for their inferiority to welds made by cored or coated electrodes. Using rotating-beam specimens 5/8" in diameter with recesses filled with weld metal, H. T. Lewis[59] showed that if the deposit is porous the specimen has a higher fatigue limit when the recess is not filled. Better-class welding (details of welding and recess not given) raised the fatigue strength of the recessed specimen.

But Wallmann[31] could detect a difference of only 1,500 psi in pulsating tension fatigue strength between machined carbon-arc welds in mild steel (0.12 C, 0.6 Mn) with large and with small blowholes. Bierett[12], too, while admitting the importance of pore-free welds, especially for side fillet welds, believes that internal defects simply accentuate the external notch effects, and cites pulsator tests on butt-welded stiffened T beams. In two beams developing good fatigue value the X-ray revealed fine or coarse blow holes near the tension edge; a beam that gave poor results had defective penetration. Graf[6] gives more emphasis to the notch effects due to absence of reverse welding or to the junction between the surfaces of weld and plate, than to the effects of internal defects. Porosity and notch effect usually occur together. Kruger[60], as well as Mailänder and Ruttmann[44], mentions porosity as one of several effects contributing to the low fatigue value of improperly made welds. Normalized, reversed-bend specimens of V welds in mild steel plate (61,500 psi tensile strength), as tested by Lea and Parker[21], had fatigue values depending on porosity as revealed by micro-, macro-, and X-ray-examination. Specimens free from porosity (shielded arc electrode, analysis not given) had a fatigue limit of 28,000 psi, whereas porous welds (electrode analyzing 2.92 Mn, 0.15 C, 0.29 Si, 0.10 Ni) gave only 19,000 psi. Hodge[61] also states that fatigue value is largely dependent on mechanical defects. Welds free from defects as shown by the X-ray had a fatigue limit (details not given) of 30,000 psi; welds with porosity, 16,000 to 18,000 psi.

Two investigators have definitely stated that internal defects are not a factor in the fatigue of welds. Blackwood[62] noted that small gas holes and slag inclusions had little effect on the rotating-bend fatigue value of welds in mild steel made with bare or fluxed electrodes. In pulsator tests on unmachined, unannealed butt welds in 1/2 inch plate (0.1 C, 0.5 Mn) between +21,300 psi upper stress, +2,850 psi lower stress, Fry[63] obtained the results shown in the table at the top of the next page.

Electrode	Cycles to Fracture	Location of Fracture
Bare	1.2×10^6	Started at Pores in weld; spread thru plate.
Dipped	1.0 "	Principally in weld.
Heavy-Coated	1.7 "	Same as bare-electrode weld

X-Ray examination revealed more porosity in the heavy-coated than in the dipped-electrode welds; bare-electrode welds were practically free from blow holes. Fry concludes that blow holes and slag inclusions, although they should be avoided, are not important factors in the fatigue value of welds. Poor cast microstructure and high nitrogen content explain the low values of bare and dipped electrodes, Fry believes.

Penetration

The most important type of internal defect from the standpoint of fatigue of welds appears to be poor penetration, by which is meant lack of fusion along the scarves and at the root of V and double V butt welds as well as of fillet welds. Poor penetration is usually the result of poor or hasty workmanship, as Chapman(64), Sulzer(65), and Johnson(66) imply, but may also be caused by *too narrow* a weld angle as in tapered T welds (Bierett(67)), by the use of thick electrodes, and by other factors.

In Haigh's(68) opinion, poor penetration is the chief factor in lowering fatigue value. Welds with small speck-like inclusions and having an alternating direct tension-compression fatigue limit of $\pm$ 12,300 to 13,400 psi and a pulsating tension fatigue limit of 21,300 to 22,400 psi are not further affected by the scratching and indenting expected in service. Such a butt weld with a hole drilled through the middle to represent a standard stress raiser withstood 16.8×10^6 cycles at 12,300 psi and 1.8×10^6 cycles at 17,900 psi before fracture, the cracks following slag inclusions. For joints with poor penetration however no fatigue limit can be assigned. Roš and Eichinger(7) also regard poor penetration as more important than small superficial defects such as notches and corrosion pits, which have no further effect on the fatigue limit of welds. Graf(6) found that poor penetration in V and double V welds is as important as undercutting. The rotating-beam specimens (double V welds) of Musatti and Reggiori(69), without exception, broke thru the root of the X. The magnitude of the effect of poor penetration is shown by the results of

Wallman[31] on carbon-arc welds with shielding gas (referred to in preceding section). Specimens with large blow holes had a pulsating tension fatigue limit of 19,900 psi; specimens with poor penetration only 14,200 psi.

Poor penetration is also an explanation for many service fatigue failures, as Pfleiderer[70] showed for welded superheater tubes. The relative importance of poor penetration depends on the type of joint and stress, according to Bierett[12], Fig (4). In the lower set of drawings, as in beams, other influences are so much more powerful that the penetration problem is secondary. Covered electrodes aid in obtaining good penetration and consequently good fatigue value, and in keeping slag out of the weld.

It may be concluded that, as Orr's results[71] suggest, poor penetration is not an inherent defect in welds-good workmanship, materials, and design may always eliminate this defect - but that, when present, it may decrease the fatigue value up to 50% and more.

Interrupted Seams

The poor fatigue characteristics of interrupted seams were shown by Hochheim[72] in pulsator tests of welded beams. A welded I beam with continuous welds withstood 2 x 10^6 cycles of bending between +22,200 psi upper stress and +7,400 psi lower stress without fracture. A beam of identical construction but with interrupted seams fractured after 60,000 cycles in the same range of stress. The beams were made of low-alloy structural steel (74,000 psi static tensile strength) with special electrodes (type not stated). Bierett[67] states that interrupted seams should be avoided, and Roš and Eichinger[7] show by an example that, the factor 0.6 being applied to the permitted stress in plate metal at the end of a weld bead and 0.85 applying to the continuous seam, it is not generally economical to use interrupted fillet welds. The adverse effect of interrupted seams on fatigue value appears to be explained by the stress concentrations known to exist at the end of a bead of deposited weld metal.

MECHANICAL TREATMENT

Peening

The effect of peening on fatigue value has been studied by Peterson and Jennings[73], and Lohmann[34]. The first-named found that unmachined all-weld-metal deposited by bare electrodes gave 18,000 psi in rotating cantilever tests and that this was raised to 20,000 psi by peening. Peening the outer layer was as effective as peening each layer successively, which is in agreement with Bierett[12, 67], and with Strelow's statement[74] that the coarser the grain at the surface of the weld the lower the fatigue strength in reversed bending. About the same increase in reversed-bend fatigue strength as observed by Peterson and Jennings was found by Lohmann in low-nitrogen double V welds. Gerritsen and Schoenmaker[75] attribute the increase to the closing of pores under the hammer. Peening did not appear to be beneficial in Wilson's fatigue[76] tests of welded girder-to-column connections.

Hot Forging

The effect of hot forging has been closely studied by Becker[27], Pester and Schulz[41], and Hoffmann[77]. The Lehr short-cycle method was used by Becker to evaluate the rotating bend endurance limits of his specimens; this method has been shown by Bartels[38] to give slightly higher values for welds than the 10 x 10^6 cycle method. The specimens were oil-cooled during test to maintain their temperature at 20°C. The specimens were machined from 60° double V welds in 3/4" plate (0.1 C, 0.4 Mn, 0.15 Cu) using DC arc (bare electrode, 0.08 C, 0.45 Mn, 0.0 Si), gas, and atomic hydrogen (filler rod in both cases: 0.10 C, 0.4 Mn, 0.12 Si). The specimens were heated in a gas furnace to three forging temperatures:

800	1050	1200	°C Furnace Temperature.
700	950	1050	°C Final Temperature.

Reductions of 20 and 40% were made using the same number and weight of blows in each case. The results shown in Fig 5 (the fatigue limit of the unwelded plate was 36,200 psi, given as 100% in the graph) indicate that forging is beneficial but is less so at temperatures of about 1200°C on account of increase in grain size. Becker explains the effect of forging as an equilization of internal stress and a homogenization of the structure of the weld. The effect of forging appeared to be independent of carbon and manganese content of the filler rods up to 0.32 and 3.15%, respectively.

The effect of hammering double V gas welds in 3/4" mild steel plate (percentage of reduction in forging not stated) has been studied by Pester and Schulz using the rotating-beam machine (10×10^6 cycles). The results are shown in the following table.

Effect of Hot Forging on Gas Welds. Pester and Schulz[41] (1932)

Material	Type of Welding	Endurance Limit psi	Percentage Decrease
Unwelded	-	24,600	0.0
Unforged	Fore-hand	18,800	23.7
Forged	"	21,400	13.3
Unforged	Back-hand	18,500	24.9
Forged	"	23,600	5.4

Hot forging increased the fatigue limit of back-hand welds 20%; of fore-hand welds only 10%. The superior fatigue qualities of back-hand welding in mild steel has also been shown by Kleiner and Bossert[78]. Hoffmann[77] found increases of 75 to 100% in fatigue value due to forging (details not given) of welds made with coated and cored electrodes (0.07 C, 0.65 to 2.8 Mn) in mild and low-alloy steels. Friedmann[79] showed that hammering was beneficial to the fatigue value of welds in Aldrey wire (see section on Alloys, Non-Ferrous). Bierett[12] states, however, that forging in general has little effect on the fatigue strength of welds (no details given). Johnson[66] and Reiter[80] believe that the good fatigue strength of flash welds is accounted for by the hot work involved in the process. Nevertheless, Etter[81] advises that to avoid cracks, especially in copper but also in mild steel, welding should not be done where shaking of the work is involved, as by riveting at the same time.

Cold Work

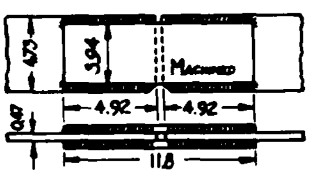
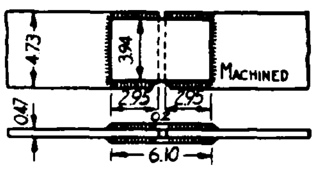
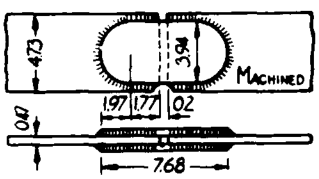
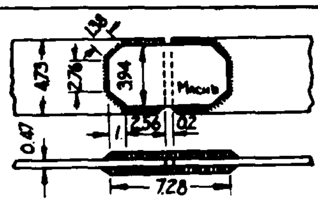
The advantage derived by cold working welds has been mentioned on several occasions, for example by Miller[82], Hoffmann[83], and Hobrock[84], but this apparently simple method of improvement seems to have received little attention in industry. Hoffmann observed an increase of about 10% in the fatigue strength of shot welds by cold working and Hobrock believes that it is quite possible that spot welds in Duralumin, cold worked or pre-stressed below the natural elastic limit, may have improved fatigue characteristics. The local application of repeated small loads might not only relieve stress concentrations but might raise the endurance limit of the annealed region adjacent to welds. The former effect has been amply demonstrated by Ludwik, and more recently by Thum and co-workers, and others on a variety of machine elements, such as screw threads, and filleted and drilled shafts.

Machining

To date, fatigue tests show that for medium- and high-quality arc welds, butt or fillet, in structural steel, intelligent removal of undercutting and other surface notches, or reinforcement by machining raises the fatigue value about 25%. In poor quality welds with high inclusion content, machining appears to be of no advantage.

The effect of machining on fatigue value has been investigated mainly in direct-stress and reversed-bend machines. Results with the former type of machine have been reported by Graf[6], Schick[10], Haigh[68], Hankins and Thorpe[20], and Memmler, Bierett, and Gehler[9]. Graf showed that well-made butt arc welds have a pulsating tension fatigue strength of up to 27,000 psi (2×10^6 cycles) and that this is increased to 34,000 psi by machining the weld flush or by carefully grinding out all notches so that there is a gradual transition from plate to weld cross-section. On the other hand, Schick using the same type of machine (pulsator) as Graf found that machined bare-electrode V butt welds in mild steel had no higher fatigue range (upper stress 31,300 psi, lower stress 20,000 psi, 2×10^6 cycles) at high superimposed loads than unmachined butt welds. This is confirmed by Haigh who tested low-grade welds. Tests by Lee and Parker[21] showed that 70° V welds (slag-coated electrodes: 0.15 C, 0.10 Si, 0.57 Mn) in plate having a tensile strength of 71,000 psi had a reversed-bend fatigue limit (10×10^6 cycles) of 19,500 psi; when the weld was machined flush with the plate the value rose to 21,500 psi; and when both surfaces of the specimen were machined the value was 24,000 psi. Hankins and Thorpe using high-class covered electrodes and double V welds found that the pulsating tension fatigue limit unmachined was 17,500 psi; by machining flush the value was raised to 31,200 psi, parent metal giving 34,700 psi. (These values are given as 17,900, 31,200, and 35,800 psi, respectively, in the Report of the National Physical Laboratory for 1933.) The Goodman diagram Fig 6 for flush-machined and unmachined double V welds in two thicknesses of low-alloy, high-strength structural steel emphasizes the increased importance of machining in attaining maximum fatigue values for high-strength structural steel. The graph also shows that the effect of machining becomes negligible with high superimposed static tensile loads. Machining is also beneficial for austenitic welds, as Kautz[55] has shown (see section on Alloys).

The effect of machining is at least as beneficial for fillet as for butt welds in pulsator tests. The benefit of machining the inner ends of fillet welds is shown in the following table taken from the tests of Memmler, Bierett, and Gehler. The welds were made with bare electrodes in mild steel; the radius of the machined ends of the welds was 1 3/8 inches. The tests were performed on the pulsating bridge (see Appendix A). As shown in the small diagrams the thickness and breadth of plate metal were ½ inch and 4¾ inches respectively. Schick also obtained 40% better pulsating fatigue value from parallel shear fillet welds the ends of which had been machined (details not given) than from unmachined.

	Type of Joint	Lower Stress psi Su	Upper Stress psi So	No of Cycles to Fracture x 10^6	Fracture
	VIA	11,200	22,600	0.31	Inner end of weld, in strap
	VIB	11,400	22,800	2.10	Outer end of strap
	VIIA	11,400	22,800	0.91	Inner end of weld, in strap
	VIIB	11,100	22,500	1.63	Normal shear weld
	VIIIA	11,900	23,300	1.21	Inner end of weld, in strap
	VIIIB	12,100	23,500	2.48	"
	IXA	11,200	23,600	0.64	"
	IXB	11,200	23,600	3.17	Normal shear weld

A-Unmachined
B-Machined inner ends

An exceedingly informative study of the effect of machining on the reversed-bend fatigue strength of T welds has been made by Thum and Lipp.[15] It was shown that Tees, Fig (7), welded with bare electrodes without tapering of the shank of the T had a fatigue value (10×10^6 cycles) of 15,500 to 17,000 psi, the Wöhler curve not being horizontal. By machining the weld as shown in the diagram the Wöhler curve was horizontal at 10×10^6 at a value of 22,800 to 24,200 psi. Larger Tees (18" high, 4" wide, 3/4" thick) gave a lower increase than the smaller Tee due to removal of the notch by filing or grinding at the transition from weld to plate. The scatter decreased from 20% in the unmachined specimens to 10% in the machined specimens. It was also shown that the reversed-bend fatigue strength of polished round bars of mild steel was not affedted by welding with bare electrodes (27,000 psi).

Three other investigators, Roberts[16], Leitner[23], and Orr[71], also find that machining raises the reversed-bend fatigue strength of butt welds. Roberts found that by machining bare electrode welds in 1/2" mild steel plate to 3/8" thick the fatigue value (magnetic impulses synchronized with natural frequency) was equal to parent metal. A small improvement of 10 to 15% due to flush machining was observed in atomic hydrogen welds (Swedish iron rod) as well as in arc welds. According to Leitner, the reversed bend fatigue limit of butt welds (coated and cored electrodes) in mild steel is increased from 23,500 psi unmachined to 30,000 - 31,000 psi after removal of reinforcement. Orr found that arc welds in high-strength structural steels (compositions not definitely stated) had 65% of the fatigue value of parent metal and that this was raised to 70% by machining. Brown and Orr[85] found that welds machined flush have practically the same reversed bend fatigue limit as unmachined welds, namely, 21,500 psi. Using plate having a rotating bend fatigue limit of 23,600 psi, Townshend[86] found that flush machined X welds had a fatigue limit of 21,400 - 22,400 psi, but unmachined only 16,900 psi (no material or welding details).

Leitner[87] states that welds prepared with cored electrodes are improved in fatigue value (details not given) up to 4,000 psi by grinding off the reinforcement, and Schuster[88] advises that wherever possible the reinforcement of boiler welds should be ground off. Bierett[12] shows that by machining the tension stressed area of a butt weld connecting two mild steel T beams, the fatigue fracture develops in parent metal not near the weld. In general the weld reinforcement should not be removed, the notches and surface irregularities in the weld and its immediate vicinity being removed by grinding; planing is not usually sufficient because notches are not necessarily removed.

The rounding off of the inner ends of parallel shear fillet welds by portable millers is recommended for improvement of fatigue value. Hoffmann[36] also notes the improvement in fatigue value to be expected by machining.

That machining may be undesirable in certain types of welds is shown by Peterson and Jennings[73] who tested low-carbon all-weld-metal deposited by bare electrodes in a rotating-cantilever machine. The unmachined deposit had a fatigue limit of 12,000 to 16,000 psi; machining lowered this to 9,000 to 13,000 psi. The decrease was attributed to the exposure of internal pores by machining, surface notches being in general more detrimental than interior notches. It is also possible that blowholes on the surface may transmit deformations to adjacent material more readily than "constrained" internal discontinuities, especially in impact loading. The rotating bend fatigue value of gas, arc, and flash-welded tubes in rotating-beam fatigue was found to be practically unchanged by machining (See R R Moore and Johnson whose work is summarized in the section on <u>Tubes</u>).

The reversed-bend fatigue value of low-nitrogen double V arc welds in structural steel plate was shown by Lohmann[34] to be considerably raised by machining away the reinforcement, as the following table shows:

The Effect of Machining on the Reversed-Bend Fatigue Limits (psi) of Double V Welds. Lohmann (1933).

Pulsating Bend Fatigue Limit (0 to Max)			Reversed-Bend Fatigue Limit	
Material	Unmachined	Machined	Unmachined	Machined
0.08C, 0.5 Mn	25,600	39,800	17,100	24,200
0.18C, 0.4 Si, 1.0Mn, 0.7Cu, 0.5Cr	32,700	45,500	21,400	24,200

He conceives that it is the purely stress concentrating effect of the reinforcement that accounts for the benefits derived from machining, and gives the following values for artificial reinforcements on plates of the low-alloy steel mentioned in the above table:

Type of Unwelded Specimen	Reversed-Bend Fatigue Limit psi
Smooth surface, milling marks perpendicular to axis of stress	± 32,700
Artificial reinforcement 1 mm high	± 21,300
" " 2 " "	± 19,900
" " 3 " "	± 17,100

The figures are revealing but their interpretation must not be too rashly attempted. All results indicate however that in welds, as in other structural elements, removal or addition of metal may have quite unexpected effects on fatigue strength and that the designer and welding engineer should bear them in mind.

WELDING TECHNIQUE

Scarf Angle

Scarf angle is important for fatigue value only insofar as penetration is concerned, according to Bierett[12] who recommends that scarf angle be as small as possible consistent with good penetration. Jennings[89] found the following rotating bend fatigue limits for low-carbon, bare electrode welds in hot-rolled steel plate having an endurance limit of 27,000 psi:

Jennings' Results with Different Scarf Angles (1930)

Type and Angle	Endurance Limit psi
0°	16,000
30°V	21,000
45°V	20,100
30°X	16,200
45°X	17,800

The specimens were 0.30 inch diameter. Dörnen[90], using two types of scarfs on double V welds in mild steel plate (Kjellberg electrodes): (A) 70° with no spacing; (B) 120° with 3/64" spacing, found that the latter was superior in low frequency fatigue (8cpm). At ± 20,000 psi unmachined welds of type (A) withstood less than 16,000 cycles whereas type (B) withstood 50,000 (average of 14 specimens). The scatter was lower for type (B) (+100; -40), and only a little higher than for unwelded mild steel. An increase in root spacing as well as the use of small diameter electrodes for the starting run is also recommended by Schaechterle[91].

V and X

The relative merit of single V welds as compared to double V in fatigue is decided by Jennings' tests given above in favor of the single V. Thornton[92] found that double V welds gas and arc in mild steel gave 5,000 psi less cantilever fatigue strength than similar single V welds. Bock[93] also finds that single V welds are superior to double V but only to the extent of 1,500 psi for well-made covered electrode welds. Bierett[67], Roberts[16], and Roš and Eichinger[7], however, find that there is no difference between the two types. Roš and Eichinger qualify their statement to include only welds perpendicular to the axis of tension. If the welded seam lies in the direction of tension, differences are revealed as shown in the following table. The stress cycle was between + 1,500 psi and + 27,000 psi; plate thickness; 5/8 inch. The stress anneal was beneficial only for these welds in the direction of stress, not for welds transverse to the axis of tension, which suggests that the high longitudinal shrinkage stresses may have an effect on fatigue behavior. It may also explain the superiority of the double V joint as compared with the single V; after both had undergone a stress relieving treatment.

Pulsator Tests on Unmachined Arc Welds in Mild Steel Parallel to the Axis of Tension. (Roš and Eichinger, 1935)

Type of Weld	double V butt weld with V's offset	V welds	X welds	300 cpm.
As Welded	716,500	343,500	267,400	cycles to failure
Stress Anneal 650°C.	958,800 (Unbroken)	692,800	878,500	"

The increase in fatigue value to be obtained by using butt welds inclined to the axis of tension is particularly demonstrated by the results of Diepschlag, Matting, and Oldenburg[94], shown in Fig. 8. There is a linear increase in pulsating tension fatigue strength with increase in angle between weld and the normal to the tensile load. This is confirmed by the results of Graf[6], Figs 9 and 10, and Bierett[12] for semi-circular and other butt welds. The latter and others believe however that the inclined or curved butt weld is justified in locations where sensitivity to production defects is expected; the angle between weld and the axis of load should never be less than 45°. Furthermore, the magnitude of shrinkage stresses, especially with arc welding, is not known in these joints. Schaechterle[91] and Graf[6] show that failure in static tension in butt welds occurs in the parent metal but in fatigue the butt weld, even the inclined type, starts to fail in the weld.

Current and Reverse Run

Butt welds (30°V) in mild steel made at different amperages, from 200 to 275 amps, with 5/32" bare electrodes (semi-automatic process) had identical cantilever fatigue limits ($\pm$ 3%) in the tests reported by Jennings[95]. Fatigue tests on welds made with different sizes of electrodes showed variations that are probably to be ascribed to variations in workmanship. Leitner[87] and Vincent[96] show that a reverse run raises the direct tensile and reversed-bend fatigue strengths in mild and alloy steel by 10 to 20%. In the extensive pulsator tests performed by Graf[6] butt welds, arc or gas, that were not carefully reverse-welded had 30 to 50% lower pulsating tension fatigue strength than welds that had been carefully reverse welded. The German investigators in general recommend reverse-welding wherever possible. The reverse run is important because it eliminates notch-effect at the root of the V, not because it refines the grain structure.

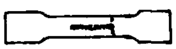
Plate Thickness

The effect of increasing plate thickness on the fatigue value of welds is adverse. Jennings[89] found that a small cantilever specimen 0.469 inch diameter gave 14,400 psi, a large cantilever specimen 2-1/4 inches diameter giving only 10,000 psi. Both specimens were 45° double V welds in mild steel using 5/32" bare low carbon steel electrodes, 150 amp. (analysis of plate and electrodes not given). He ascribes this size effect to residual stresses and is concurred therein by Roš and Eichinger[7]. These investigators observed that the pulsating tension fatigue limit of the junction zone material was: Butt welds 19,900 to 21,800 psi; T and normal- and parallel-shear joints 11,300 to 17,100 psi the lower value being obtained for samples from thick plates (about 1"), the upper applying to medium size plates (about 1/2"). Annealing raises the pulsating tension fatigue value of the junction zone somewhat (no data given). Graf[97] showed that the pulsating fatigue strength of structural steel with flame-cut surface depended on plate thickness, the plate containing a transverse hole. In 1-9/16" plate the value was 27,500 to 29,000 psi; in 3-1/8" plate, 23,200 to 25,600 psi. H. T. Lewis[59], however, found that fatigue cracks appeared to about the same extent after 227,000 cycles at 29,700 psi in all three of the equally stressed necks (diameters: 1", 0.91", and 0.79") in a composite rotating cantilever specimen of mild steel with recesses containing weld metal (no details). The rotating bend fatigue tests performed by Ehrt and Kühnelt[98] on different zones of a shaft (0.66%C) built up with different kinds of wear resisting electrode deposits confirms Lewis's conclusions.

Fatigue Tests of Weld Elements

To indicate the directions in which the search for means to improve the fatigue properties of welds should proceed, a number of investigations have been made on weld elements and models of welds. The most extensive series of tests on weld elements has been reported by Schulz and Buchholtz,[18] who performed their tests, summarized in the following table

Pulsator Tests of Weld Elements - Schulz and Buchholtz (1933)

	Element	Percentage of Pulsating Tension Fatigue Strength of Machined Flat Bar
	Plate with hole 5/8" diam.	78%
	Plate with rivet	64
	Transverse bead (arc) on one side	75
	Transverse bead " " both sides	39
	Longitudinal bead on one side	43
	" " " both sides	39
	Stud on one side	64
	Stud on both sides	56
	Rectangular strap	34
	Rhombic "	32

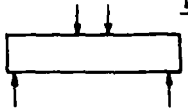
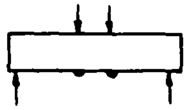
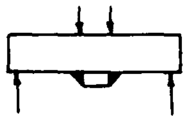
Location of fatigue fracture is indicated by wavy line.

on a high-strength, chromium-copper structural steel having a fatigue strength in pulsating tension of 39,800 psi as determined on a machined rectangular bar. According to Bierett,[67] other tests have shown that the double-sided run is no more unfavorable than the single-sided and that the transverse is as dangerous as the longitudinal bead. The more longitudinal runs are present the more dangerous they are. He also points out[97] that the loss due to a bead welded on the surface ought not to be quoted as a percentage because the loss depends on plate thickness.

The reversed bend fatigue limit of a plate tested by Roberts[16] was reduced from 29,200 psi as received to 15,700 psi after a bead of metal had been deposited across the maximum stress section. After the bead is machined flush the fatigue limit rises to 18,000-20,000 psi. Schaecterle[91] gives the following values for pulsating tension fatigue strength of mild steel and low-alloy structural steel. (Dimensions of specimen: $2\frac{3}{4}$" x 1 3/16".)

Condition	Pulsating Tension Fatigue Strength psi Mild Steel	Low-Alloy Structural Steel
With mill scale	21,300 to 34,200	34,200 to 51,000
With mill scale and central hole	25,600 to 28,500	28,500 to 31,300
With welded bead on one side	22,800	25,600
With welded bead on both sides	17,100	17,100
Double strap normal shear joint plate: ½" straps 3/8"	11,400	11,400
ditto with tapered seams and machining	17,100	17,100
Butt weld; V or double V	22,800 to 25,600	22,800 to 25,600

In pulsating bending, the reduction due to beads of weld metal is very large according to Hochheim[100] whose results are given below:

	Lower Stress	Upper Stress	Pulsations
	17,100	51,500	2×10^6 unbroken
	8,600	25,600	2×10^6 unbroken
	8,600	25,600	730,000 fracture

In specimen 2, two beads of metal were run across the bottom of the bar; its endurance limit was close to 25,600 psi upper stress, 8,600 psi lower stress.

These tests are exceedingly interesting from a practical standpoint but may be explained by stress raising due to shape, notch effect due to undercutting, shrinkage stresses (none of the specimens appears to have been annealed), or to microstructural changes depending on the inclinations of the interpreter. Less objectionable are the fatigue tests of models of butt welds in reversed bending by Lohmann (quoted above) and by Schulz and Buchholtz. These tests indicated that, for the thickness of plate used (not stated), an artificial "reinforcement" 1/12 inch high machined in unwelded plate in medium-carbon or low-alloy structural steel reduced the fatigue value by 30%. A perfect weld therefore will probably give a reversed bend fatigue of much lower value than the parent metal. Baud[101] states that the points of maximum stress concentration indicated photoelastically in models of welds are confirmed by fatigue tests, but the degree of magnitude of concentration is much less in the fatigue test than in the photoelastic specimen.

There are three types of heat treatment applied to welds: 1. full annealing, 2. stress annealing, 3. hardening and tempering. By full annealing above Ac_3 the static strength of the weld is generally decreased, internal stresses are removed, and grain size is refined. Stress annealing has much less effect on grain size and static strength than full annealing. Quenching and tempering is generally applied only to welds in medium- or high-carbon alloy steels. The effect of the first two treatments on the fatigue strength of welds is a matter of controversy, but the beneficial effect of the third type of treatment on welds in aircraft tubing and rail steels is shown by Ward[102], Beissner[103], and Reiter[80]. Brenner[104] also states that welds in aircraft steels should be heat treated for best fatigue behavior.

Full Annealing

The effect of full annealing on the rotating- and reversed-bend strength of arc welds in mild and low-alloy steels has been studied by Lohmann[34]. His results show that annealing (880 to 920°C) is detrimental to welds with medium or high nitrogen content, above about 0.04% N_2, but is beneficial when the nitrogen content is below 0.04% The difference between the as-welded and the annealed specimens was never more than 3,000 psi, however. Lohmann and Schulz[34], and Hodge[61](the latter gives no details), however, have found that there is no connection between the nitrogen content of welds and their fatigue value. The results of French suggest that the fatigue value of age-susceptible welds may be raised by tensile over-stressing. In materials that are not susceptible to stress-aging, tensile overstressing lowers the fatigue value. Bartels[105] showed that the rotating-bend value of gas welds in mild steel and cast iron was not improved by annealing, that welds in silumin were adversely affected, and that welds in copper were slightly improved by full annealing. Brown[106] also showed that full annealing was disastrous to the reversed-bend fatigue limit of gas welds in 5/16 inch mild steel plate, the fatigue limit as-welded being $\pm$ 25,000 psi, and after annealing only $\pm$ 14,000 psi. Annealing lowered the reversed-bend fatigue limit in double V arc welds in mild steel from 19,200 to 15,500 psi, according to Lehr[107]. But Sulzer[65], using the rotating-beam machine found that arc welded mild steel was raised from 14,500 psi as-welded to 22,400 psi after full annealing. Furthermore, Burn[108], and Schulz and Buchholtz[18] recommend the full annealing of welded structures to avoid service fatigue failures.

The effect of annealing is closely related to the grain size, as Peterson and Jennings[73] have shown. By annealing bare-electrode all-weld-metal for 2 hours at 1700°F they observed a 30% decrease in cantilever-fatigue limit which they ascribed to coarse grain, although Harvey and Whitney[25] could detect no effect of grain size on the corrosion fatigue of mild steel. This effect of coarse grain on fatigue limit was developed by Thornton[24] as a theory of the fatigue failure of welds whereby the difference in grain size and hardness between weld and base metal is said to account for the low fatigue strength of welds. The grain size effect has however been shown by Lohmann and Schulz[34] and others to be secondary, for weld fatigue failure in rotating- and reversed-bend tests occurs usually through the middle of the weld; sometimes the first crack originates at a point of high stress concentration.

The tensile- and non-reversed or pulsating bend-fatigue results of Graf[97] on flame-cut surfaces in structural steel (65,000 psi static tensile strength) show that the increase in grain size due to flame cutting is not an important factor. The plates with oxy-cut surface have a fatigue value equivalent to a plate with a rivet hole, namely, 27,500 psi in pulsating tension, and 28,500 psi in pulsating bending. After heating for one hour at 885°C followed by air-cooling, the section being 1-1/2" x 1-1/8", the grain size at the surface was refined and the pulsating bending origin fatigue limit rose to 37,000 psi. However, by grinding the rough oxy-cut surface to remove surface irregularities without removing the zone of coarse-grained material, the pulsating-bend value was raised to over 55,000 psi. Graf therefore concludes that oxy-cut surface need be machined only deep enough to eliminate surface irregularities; there is no need to remove the coarse-grained zone. Similar tests[97A] on flame cut surfaces of low-alloy structural steel (static tensile strength 74,000 psi) showed a fatigue limit of 34,100 psi compared with 38,400 psi for a sawn surface. Ground or milled flame cut surfaces had intermediate values. Melhardt[109] found that oxy-cut surfaces in mild steel had slightly better reversed bend fatigue strength than planed surfaces, tested parallel or perpendicular to the direction of planing. At a reversed stress in the extreme fibers of $\pm$31,300 psi the oxy-cut surface withstood 2.25×10^6 cycles to fracture whereas the parallel and transverse planed specimens failed after 2.0×10^6 and less than 1×10^6 cycles, respectively.

The annealing effect of gas welding on the adjacent plate was found by Kleiner and Bossert(78) to reduce the rotating bend fatigue limit up to 10% in the zone heated above 400 to 500°C, which coincides approximately with the recrystallization of the mild steel plate that was used. This decrease extends over a 70% wider zone in fore-hand than in back-hand welds. These investigators believe that the coarse grain structure of gas welds prevents their attaining the same fatigue value as the base metal. Of the contrary opinion are Musatti and Reggiori(69), who supply experimental proof in the following table.

Musatti and Reggiori's Results (1934)

Specimen	Brinell Hardness	Fatigue Limit psi	Tensile Strength	Ratio= Endurance Fatigue Limit / Tensile Strength
Parent Metal	170	41,000	85,500	0.48
Double V Weld	-	27,600	81,500	0.34
All-Weld-Metal	-	29,000	83,700	0.35
Overheated Parent Metal	220	47,000	100,000	0.47

The parent metal contained 0.23 C, 0.8 Mn, 0.3 Si; the electrode containing 0.08 C, 0.4Mn, 0.01 Si and being coated with a mixture containing 12% $CaCO_3$, 19% Fe-Mn (80% Mn), 3% Pyrolusite, 37% red Hematite, 6% Fe-Si-Ti, 23% Sodium Silicate (90% accounted for). The overheated base metal had a coarse Widmannstätten structure which did not however injure fatigue strength. Other considerations point as well to the relative unimportance of the metallographic structure of welds in mild steel so far as fatigue is concerned.

Occasionally the fatigue failure of welds has been observed in the heat-affected transition zone between weld and base metal, especially in the overheated structure of tubes (Franke(110), Baumgärtel(111), Ward(102), Hoffmann(112)). The heat-affected zone, it is supposed, may act in two ways.

(1) The Widmannstätten structure in the overheated zone is undesirable in fatigue. Existing fatigue values of unannealed cast steel or overheated rolled steel show that in neither material is the fatigue limit below that expected from the static tensile strength. Indeed, Vér[28] demonstrated that slip lines, which precede fatigue cracks, form less readily in the Widmannstätten structure of flash welds in low carbon steels during rotating bend fatigue tests than in the as-rolled structure. Rosenhain[113] has suggested that the weakest zone in fatigue is that in which, due to the welding heat, cementite has just begun to spheroidize. Observations of fatigue fractures can scarcely be said to confirm this hypothesis.

(2) The difference in grain size, structure, and physical properties between base metal and weld acts in some way as a source of stress concentration which lowers the fatigue value. This hypothesis is stated most convincingly by Diepschlag, Matting, and Oldenburg[94] and mentioned also by Thornton[24], who noted that fatigue cracks never started in large Widmannstätten grain boundaries, and by Brenner[104] and Schaechterle[114].

Kleiner and Bossert[78], and Roš and Eichinger[7] showed that the heat-affected zone had a fatigue value intermediate between weld metal and plate, the former showing that in gas welds in mild steel the rotating-bend fatigue value of the weld was 30 to 50% below plate metal, the transition zone only 10% below plate metal. Bierett and Grüning[49] also point out that if the heat-affected zone were the cause of fatigue failure, the procedure of depositing concave weld beads with smooth gradual surface transitions which produces a larger heat-affected zone should be expected to lower the fatigue value. Actually the fatigue value is considerably increased.

The two types of explanations may perhaps suffice for the very small proportion of fatigue failures that demonstrably occur in the heat-affected zone of welds. It cannot be said that the presence of this zone is more than a minor factor in the fatigue of welds. In this connection, Vér[28] found that in flash welds in medium-carbon steels, fatigue cracks had a strong tendency to develop and spread in previously-formed slip lines in the network of ferrite. The slip lines tended to occur in the direction of maximum shear stress. The wider the ferrite network, the lower the fatigue strength because slip lines form more readily. Jasper[187] also found that the rotating bend fatigue limit of specimens cut from transition zone and weld metal in arc welds was slightly higher than that of base metal.

Stress Annealing

Full annealing having only a slight beneficial effect on fatigue strength in low-carbon steels, and then only in low-nitrogen welds, the effect of stress annealing may be expected to be small. This is confirmed by all who have studied the problem.Peterson and Jennings[73] found that the cantilever fatigue limit of unmachined bare electrode weld metal was scarcely affected by two hours at 1000°F. Thornton[92], Roš and Eichinger[5], Orr[71], and Ayslinger[115] found that stress annealing was beneficial. The first-named found that a covered electrode low-carbon V weld which had a cantilever fatigue limit of 27,000 psi was raised to 28,500 psi by stress annealing. The results obtained by Orr (tabulated in the section on low-alloy steels) indicate an increase of up to 15% due to stress annealing 1/2 hour at 600°C in reversed bend fatigue limit in low-alloy welds. The pulsating tension fatigue limits of gas and arc welds, V or U (reverse welded) or X joints in mild steel, according to Roš and Eichinger, are:

Direction of Stress	Unannealed	Stress Annealed 650°C
Butt weld perpendicular to axis of tension	18,500 psi	21,400 psi
" " parallel to axis of tension	22,800	25,700

Coated electrode butt welds (10 x 70 mm cross-section in structural steel plate having a pulsating tension fatigue strength (2 x 10^6 cycles) of 29,900 psi) were raised from 21,400 psi as welded to 22,800 psi after stress annealing at 500-600°C, according to Ayslinger.

Barnes[96] and Lohmann and Schulz[34], on the other hand, found that stress annealing is definitely harmful. Using 1/4" 3-1/2% Nickel and medium-carbon steels, Barnes found that stress annealing at 600°C lowered the number of cycles withstood by dipped electrode welds on the Upton-Lewis machine at 30,000 psi by as much as 100% in several cases; bare electrode (1.0 Ni, 0.5 Cr) welds suffered least by stress annealing. The reversed- and pulsating-bend fatigue strengths of machined low-nitrogen arc welds in mild steel plate (0.08 C, 0.5 Mn) were lowered from 24,200 and 39,900, respectively, as welded to 21,400 and 38,400 psi after stress annealing at 650°C, according to Lohmann and Schulz.

The results show that the genuine beneficial effect of stress annealing on impact behavior and bend values does not extend to fatigue characteristics. Opinions based on experience and theoretical considerations are more numerous than test results and are summarized in the next section on Shrinkage Stresses. Whether and to what extent the origin fatigue strength of the weld can be raised by annealing depends on process and materials, according to Graf[6].

Shrinkage Stresses

Shrinkage or residual stresses are actual stresses, usually local in character, existing in plate and weld metal during or after welding. In the welding of unrestrained parts, the stresses are confined to the vicinity of the welded seam itself; the shrinkage stresses due to closing welds in a rigid structure may be distributed throughout the structure. Shrinkage stresses are created by local heating into the "plastic" state followed by cooling, and must be clearly distinguished from shrinkage cracks and from plastic permanent deformation due to shrinkage at elevated temperatures. At the present time the extent and even the character of the effect of shrinkage stresses on fatigue limit appear to be matters of debate. Some of the available information is summarized in other sections (see sections on stress annealing and on tubes).

Of those who have discussed the subject, the majority believe that shrinkage stresses have a significant effect on the fatigue value of welds. However, Johnson[66] and Roš and Eichinger[5,7] adduce almost the only experimental evidence. Johnson found that the rotating bend fatigue limit of flash-welded Cr-Mo aircraft tubing having an as-welded value of 24,000 psi was raised to 32,000 psi by stress annealing at 950°F. Roš and Eichinger's results have been discussed in the section on V and X welds. The vibration tests of Messrs. Accles and Pollock, Birmingham, England, as described by Roosenschoon[116], showed that the high shrinkage stresses in welded structures of chromium-molybdenum aircraft tubing were responsible for poor vibration resistance. Boetcher[117], Kinkead[118], Jacobus[119], and Stone and Ritter[120] believe, on the basis of service results (details not given), and Becker[27] and Schaechterle[121], on other grounds, that shrinkage stresses detract from fatigue value.

Those of opposite opinion have presented more evidence, but the problem is by no means solved. Peterson and Jennings[73] proved that shrinkage stresses were not the cause of the low rotating-bend fatigue value of unmachined all-weld-metal deposited by low-carbon, bare electrodes. Lohmann and Schulz[34] also regard shrinkage stresses as of no importance in fatigue because all fatigue failures in rotating- and reversed-bend followed blow holes. Stress annealing had a negligible effect on fatigue value. On the basis of pulsator tests of welded, stiffened I beams, which were superior in fatigue value to riveted beams, Schulz and Buchholtz[18] concluded that shrinkage stresses cannot be very important. This may be partly explained by the results of Dörnen[122] that the shrinkage stress in welded beams may be less than in rolled sections of the same dimensions.

Schulz and Buchholtz have also found that internal stresses in unwelded, quenched bars are somewhat equalized during fatigue testing. Buchholtz[123] as well as Graf[6] states that in welds with high ductility and yield point, internal stresses are quickly eliminated by plastic yielding under repeated loads. In brittle welds shrinkage stresses lower the fatigue as well as the impact value (no details given). Buchholz states that stress concentrations due to notches, unlike shrinkage stresses, are not removed by cyclic loading. Orr[71] and Boulton[124], however, are of the opinion that stress concentration factors obtained by photoelastic and similar studies do not apply to actual welds under conditions of fatigue because as soon as plastic yielding occurs the unfavorable stress distribution is relieved. Possibly this is connected with the effect of understressing in raising the apparent fatigue limit, noted by Vér[28] and Bartels[38] for welds. Bernhard[125], in his portable pulsator tests of riveted bridges strengthened by welding, observed no effects that he could attribute to shrinkage stresses.

A good demonstration of the release of stresses during fatigue tests was given by Siebel and Pfender[126], who measured the permanent bulging outwards of a gas-welded patch on a boiler drum during a pulsating pressure test. The patch caused a considerable bulge inwards of the shell and the shrinkage stresses were estimated (not measured) to be beyond the yield point. Measurements of the bulge were made at various times during the pulsating-pressure test, the stress pulsating between 2,140 and 19,900 psi. The first cycle of stress caused a recovery of nearly 1% although the stress was well below the yield point of the boiler plate (35,600-37,000 psi). Recovery during additional cycles was comparatively small and after 10,000 cycles the patch still bulged in.

Siebel and Pfender also developed a known internal stress in a flat bar (8" wide, 9/16" thick) by heating both sides with welding torches. After loading statically to 28,400 psi the internal stress in the direction of breadth was decreased from 35,600 psi maximum to 22,700 psi maximum. After 10,000 cycles at a lower stress of 5,700, upper stress of 28,400 psi, further release of internal stress occurred, particularly in the axis of tension.

Bierett[12,67] deduced from the fact that, for the same steel, (S_f - Su) in Goodman diagrams (see section on machining) decreased more rapidly for welds lying in the axis of tension than for welds transverse thereto, that the shrinkage stresses, which are close to the yield point, parallel to the seam have more effect on fatigue than shrinkage stresses perpendicular to the seam. Bierett concludes however that if shrinkage stresses are not too high they have little effect on fatigue. He also shows that the shrinkage stresses in plates free to move during welding, Fig. 11, put the edges in compression. This is a protection to the edge of the seam where fatigue failure usually starts. The usual laboratory specimen cut from a large plate does not have this protection.

Kautz[55] determined that stress annealing, which is intended to raise toughness and impact value, is not required for austenitic welds in non-aging boiler plate; shrinkage stresses have no effect on fatigue because they are largely eliminated by the first loading and besides are partly relieved at ordinary operating temperatures. Kautz bent an unwelded mild steel bar in an arc, annealed the bar, and then bent it straight again. The internal stress distribution thus created resembled welding stresses. This stressed bar had the same fatigue limit in pulsating tension as a normalized, unstressed bar. This was in agreement with pulsating pressure tests of as-welded boilers in which the fatigue limit was close to that obtained on pulsator specimens. Kommerell[143] came to similar conclusions on the basis of similar tests.

Although the experimental evidence appears to support the view that shrinkage stresses play a minor part, if any, in the fatigue characteristics of welds, the problem is by no means solved. Service experience of welded structures under conditions of nearly pure fatigue loading has shown the necessity for stress annealing in certain cases. When fatigue test results on a wider variety of welds and welded structures under different ranges of stress with respect to yield strength become available, it will be possible to reach more definite conclusions than at present.

CARBON CONTENT

The effect of carbon content on the fatigue value of welds in steel has not yet been systematically studied. Zeyen[127] tested unmachined welds in plate containing 0.1 to 0.7% C in reversed bending; he used the same heavy-coated electrodes (0.1C, 0.4-0.6% Mn) for all steels. His results indicated that the carbon content of the plate had no effect on the endurance limit, 21,400 to 22,800 psi, but that the endurance ratio: S_{fw}/S_{tunw} decreased from 0.4 with 0.1 C to 0.2 with 0.7 C. His results are given in the following table.

Reversed Bend Fatigue Limit (10 x 10^6 cycles) of Butt Welded Carbon Steel Plate, 0.24" thick. All specimens with mill scale and unmachined.

	UNWELDED		ARC WELDED				GAS WELDED
Carbon Content of Plate %	Tensile Strength psi	Reversed Bend Fatigue Limit psi	Heavy Covered Electrode (1)	Lightly Covered Alloy Electrode (1.5%Mn) (2)	Cored Electrode Alloyed (2)	Austenitic Electrode Nicrotherm (3)	Alloyed Filler Rod (4)
0.11	57,000	22,800	22,800	22,800	19,900	18,500	22,800
0.30	81,000	28,500	21,300	not determined	19,900	not determined	28,500
0.38	92,500	29,900	21,300	21,300	21,300	19,900	25,700
0.56	98,500	28,500	22,800	not determined	19,900	not determined	24,200
0.68	121,000	31,400	22,800	21,300	19,900	19,900	24,200

(1) low-alloy structural steel, minimum tensile strength 73,000 psi.
(2) analysis not given
(3) 0.1C, 0.8 Si, 1.3 Mn, 20 Ni, 25 Cr, 0.019 N_2, 0.040 O_2
(4) about 1.Cr, 0.2Mo.

Vér[28] also found that the fatigue limits of polished flash welds in rotating bending (Retjö-Csonka oil-pressure machine) was 28,400 to 33,400 psi in the range 0.05 to 0.64%C, the 0.64% C specimens giving 29,900 psi. The ratio of fatigue to tensile strength of the welds decreased from 0.51 (0.08%C) to 0.29 (0.64%C). Specimens containing 0.86%C had a higher fatigue limit, 39,000 psi, but a lower ratio, 0.28.

A steel containing 0.08%C (tensile strength 58,000 psi) had a slightly higher rotating beam fatigue limit with cored or bare electrodes, according to Lohmann and Schulz(34), than a plain-carbon steel containing 0.27% C (tensile strength, unwelded, 78,000 psi), but the reverse was true for covered electrodes.

Carbon Content	Rotating-Beam Fatigue Limit psi	
	Bare or Cored	Covered Electrodes
0.08%	17,000-20,000	20,000-21,000
0.27%	14,000-17,000	23,000-25,500

Bend fatigue tests on gas and arc welded tubes by Müller(128), Hoffmann(112), and Wegelius(129) also demonstrated that there was scarcely any advantage in raising the carbon content, particularly beyond 0.35%C in plain-carbon and alloy tubes, and that the scatter in fatigue results increased with carbon content. Schulz and Buchholtz(18) state that the direct-tension fatigue value is generally lower in steels with 0.25%C than in steels with 0.10-0.15%C. Welds in steels with 0.24%C have 20 to 30% lower fatigue values in direct tension than steels with 0.16%C and the same static tensile strength procured by alloying. The effect of the carbon content of the filler rod on the rotating bend value of machined 60° double V welds in plate containing 0.1%C was studied by Becker(27) using the Lehr short-cycle method. Although the filler rods contained 0.1 to 0.32%C all welds had about the same carbon content (0.04-0.06). For gas welds both as-welded and after forging (40% reduction at 1050-950°C) there was an increase of 20% in fatigue value as the carbon content of the filler rod was raised to 0.32%, but in DC arc and atomic hydrogen welds there was no clearly defined effect. Becker surmised, without analytical data, that the beneficial effect of increased carbon content in the electrode was associated with a corresponding decrease in oxygen content in the weld.

There is obviously a need for a comprehensive fatigue study of welds with various known carbon contents both as-welded and after mechanical and thermal treatment. At present the inability of carbon to raise the fatigue value in welds appears to be attributed to the greater air-hardening capacity of the higher carbon steels with consequent development of micro-cracks, and to the greater sensitivity of the higher-carbon steels to stress-concentrations occasioned by their generally lower weldability and greater porosity.

ALLOYS

Low-Alloy, High-Strength Structural Steels

The fatigue value of welds in low-alloy, high-strength structural steels of the types containing Mn, Si, Cu, or Cr has been investigated mainly in direct stress fatigue using pulsators. These results are the basis of the German Solid-Girder Railway Bridge Specifications which are dealt with later. Graf[6] found pulsating tension fatigue strengths from 21,400 to 31,300 psi in arc-welded, unmachined butt welds in low-alloy steel (composition not stated; maximum tensile strength 74,000 psi, known as St 52, and generally containing up to 1.5 Mn, 0.2 Mo, 0.7 Cr, 0.7 Cu, or 0.5 Si). Kommerell[130] gives 12,800 psi to 25,600 as the pulsating tension fatigue values of mild steel butt welds and 21,400 to 25,600 for St 52. In neither case was process (gas or arc) or type of filler rod of any importance, although there was less scatter in the results with gas welds. The maximum values found in any specimen were 27,000 psi for mild steel and 31,300 psi in St 52. The latter value is obtained in mild steel butt welds if the weld is inclined 45° to the axis of loading, and is exceeded by machined mild steel butt welds (34,100 psi).

These tests, which were carried out with 1/4 inch plate, showed that welds in the low alloy steels had acceptable fatigue value but that they possessed little advantage over mild steel in fatigue except at high values of superimposed tension, as Schaechterle's diagram shows, Fig. 12. Graf[6] demonstrated that the same was true for fillet welds, and other investigators are of the same opinion. The better performance of low-alloy structural steel in the highly prestressed condition is due to its high yield point, but, as Bierett[12] found, butt welds in St 52 parallel to the axis of tension have practically the same pulsating tensile fatigue value as in mild steel (cored or coated electrode) except above about 50,000 psi superimposed tension where St 52 is superior.

In rotating-bend fatigue, Lohmann[34] found that butt welds in mild steel and St 52 (0.18 C, 0.7 Cr, 0.4 Cu, 1.0 Mn) with bare and cored electrodes had the same fatigue limit, but with covered electrodes St 52 gave 25,000 psi as compared with 20,000 for mild steel (0.08%C, 0.5Mn). In alternating and pulsating bend, annealed butt welds in mild steel and St 52 had about the same fatigue limit: ± 28,500 psi (alternating bend value of machined specimens). Gerritsen and Schoenmaker[75] also found no difference in fatigue limit in rotating bending between mild steel and St 52 as the following table shows. The electrode used for St 52 contained 0.35 Mn, 0.50 Cr, 0.30 Cu. An all-weld-metal specimen therefrom had a fatigue limit of 30,600 psi. The composition of the electrode for mild steel was not stated. The specimens were turned from V butt welds in 1" plate.

Gerritsen and Schoenmaker's Results

Steel	Endurance Limit psi
1.0-1.3 Mn, 0.15-0.25 Mo, 0.35 Cu	30,600
0.7-1.0 Mn, 0.4 -0.6 Cr, 0.6-1.0 Cu	30,200
1.2-1.6 Mn, 0.3-0.6 Cu	27,200
Mild Steel	30,200

Thierens[131], and Schoenmaker[132], however, found that welded St 52 had considerably higher rotating- and reversed-bend and torsion fatigue limits than mild steel but that the fatigue values for St 52 were lowered 5 to 30% by welding, but in mild steel welding did not lower the fatigue limits (see Appendix B, Tables 2 and 4). Kater[133] also found that welded St 52, in the form of unstiffened I beams, gave a higher fatigue value 37,000 (27,000) than mild steel 31,000 (23,500); these values represent the maximum and mean stresses in the extreme fibers, not in the welds.

The reversed-bend fatigue values of several high-strength structural steels have been determined by

Reversed-Bend Value of Unmachined Arc Welds. Orr

Material	Composition	Endurance Limit psi Plate	Unmachined	(Stanton and Pannell Method) Machined Flush
Mild Steel	-	A 26,900	21,300	21,300
Alloy Steel	0.25 C, 1.5 Mn - - - - - - - - -	A 35,600 B 35,400	21,700 22,000	24,900 -
" "	0.28 C, 1.0 Mn, 0.5 Cu - - - -	A 37,600 B 36,800	22,200 26,000	- -
" "	0.28 C, 0.8 Mn, 0.5 Cu, 0.6 Cr	A 38,600 B 36,500	23,700 25,300	- -
" "	0.35 C, 1.1 Mn, 0.6 Cr - - - -	A 38,600	25,600	29,800

A - as welded
B - stress annealed 600°C, 1/2 hour, furnace cool

Orr[71], on double V welds (electrode not stated). His results are shown in the above table. The static tensile strength of unmachined welds in medium-carbon, alloy plates was up to 50% higher than that of mild steel, but the maximum improvement in fatigue limit was only 22%.

It is shown in a later section that the relatively low fatigue values of low alloy structural steels as compared with mild steel is due to the relatively greater effect of notches and stress raisers in the higher strength plates. Schulz and Buchholtz[18] found that models of welds with 2 mm high reinforcement machined from solid mild steel or low-alloy structural steel gave 21,400 and 24,200 psi respectively in alternating direct tension-compression and that both steels gave 40,000 psi in pulsating tension. These values are about 30% lower than the fatigue limits for flat plate and represent the maximum fatigue values that can be obtained in perfect, unmachined welds. The average fatigue value of low-alloy structural steel containing a hole in pulsating tension is 28,500, for mild steel 25,600 psi, according to Graf.

Recently, Diepschlag, Matting, and Oldenburg[94] have offered a general theory of the fatigue strength of welds in low-alloy steels based on the difference in modulus of elasticity between weld metal and plate, Fig. 13. The figure is based on V, X, and double T welds in two high strength plates (0.13 C, 0.61 Si, 0.95 Mn, 0.46 Cu, 0.17 Mo; and 0.18 C, 0.38 Si, 0.72 Mn, 0.26 Cr, 0.51 Cu, 0.07 Mo) using four coated electrodes with different contents of Mn, Cu, and Cr, and one gas rod containing 3.3% Ni. The pulsating fatigue strength thus appears to be closely related to differences in modulus of elasticity but is not at all connected with the notch impact value of the weld or the static strength of filler rod. It was also shown by pulsating tension fatigue tests on flash welds between various steels that the fatigue value of a flash weld between two different steels is less than the fatigue value of the weaker partner. The larger the difference in modulus of elasticity between the pair of steels, the greater is the percentage decrease in fatigue value of the weld below that of the weaker steel. It is concluded that, for best fatigue behavior, weld metal and plate should have as nearly as possible identical elastic moduli in order to minimize shear forces and stress peaks caused by cross-sectional contraction.

Although it may be found that this theory has only a limited application, the opinion has often been expressed that optimum fatigue behavior is obtained by a weld metal similar to the plate. Thus Orr[71] explains his own results and reconciles them with Brown[106], who found that the alternating direct stress fatigue value of a series of welds in mild steel with different electrodes was in inverse proportion to the static strength of all-weld-metal deposited by the electrodes. Jennings[134] also found that the rotating bend fatigue limit of a hot rolled steel to cast steel weld (bare or coated electrode) was lower than either the welded cast steel or welded hot-rolled steel. Sulzer[65], on the other hand, showed that a T formed by welding a steelcasting to boiler plate had about 70% higher reversed-bend fatigue value that a boiler plate-to-boiler plate weld. The high fatigue value of austenitic welds, determined by Schick[10] and Kautz[55] also appears difficult to reconcile with the elastic-modulus theory.

Manganese as an alloying element in filler rod (up to 3.15% Mn) and weld (up to 2.4% Mn) with the content of other elements held constant was shown by Becker[27] to have very little effect on rotating-bend fatigue value as determined by the Lehr short-cycle method. Becker used the DC arc with bare electrodes, gas, and atomic hydrogen processes.

The question of suitable alloy combinations in structural steel from the standpoint of the fatigue value of welds has been raised by surprisingly few investigators. Manganese, state Schulz and Buchholtz[18], should be below 1.2% in high-strength structural steels, but silicon and copper are not unfavorable to fatigue strength. This is in agreement with the belief that high-strength structural steels sometimes tend to air harden after welding, particularly in light sections and after arc welding; silicon or copper on this account, would seem to be better additions than chromium or manganese.

Other Alloy Steels

The fatigue value of V butt welds by the atomic hydrogen process in plate containing 0.28/0.35C, 0.5 Mn, 1.1 Cr, 2.0 Ni, 0.25/0.40 Mo, has been determined by Weinman[19] using the cantilever machine and three alloy filler rods. A rod containing 0.47C, 1.98 Si, gave the highest fatigue value in the weld (25,000/35,000 psi) but in the form of all-weld-metal this rod was inferior to a rod containing 0.46C and 3.4 Ni (35,000/40,000 psi). However, Weinman concludes that fatigue value in general is a function of the composition of filler rod not plate metal. Thornton[92], also using the cantilever machine, showed that chromium-vanadium and carbon-vanadium welding rods gave higher fatigue values in gas welded boiler plate than low-carbon rods.

The comparative fatigue value of chromium-molybdenum electrodes was higher than chromium-nickel or 3-1/2% Ni electrodes in plate containing 0.32C, 3.4 Ni, according to McManus[135] using the Upton-Lewis reversed-bend machine. Barnes[96] using this type of machine, showed that, as a rule, welds in plate containing 3-1/2% Ni withstood 20 times as many cycles at 30,000 psi as plain medium-carbon plate, a low-carbon electrode (0.13/0.18%C) being superior to chromium-vanadium (0.89 Cr, 0.15 V) or nickel-chromium (1.0 Ni, 0.5 Cr) electrodes for both plates.

Austenitic Steels

The rotating-bend corrosion fatigue limit of welds in 18-8 Rezistal KA2 plate and rod (0.07%C) is reported by Harvey and co-workers (see section on Corrosion Fatigue). The fatigue strength of spot welded V2A (18-8), thin sheet, is 11,400 psi, according to unsigned German results. The same strength was found in gas welded and arc welded plates 0.2" thick. The tensile strength of the welded sheet was 128,000 psi; of the unwelded sheet 213,000 psi. The fatigue limit of spot welded 18-8 is estimated by Hoffmann[83] to be 26,000 to 31,000 psi, and of an 18-8 containing 0.1C, 1.3 Ta to be 37,000 psi.

The fatigue value of austenitic welds in mild steel plate has been determined by Kautz[55], Schick[10], and Schönrock[136]. Krupp's Nicrotherm patented austenitic electrode containing about 0.1C, 0.8Si, 1.3Mn, 20.Ni, 25Cr, 0.019 N2, and 0.04 O_2 was used. Kautz gives 0.2 C, 18.4 Ni, 22.3 Cr, 0.044 N_2 and 0.05 O_2 as a typical weld analysis in Izett plate and found that the pulsating tension fatigue strength of such unmachined welds was 24,200 psi, of the machined welds, 28,800 psi. The pulsating tension fatigue strength of Nicrotherm welds in low-alloy structural steel (composition and type of specimen not stated) is given by Schönrock as 25,600 to 27,000 psi as compared with 22,800 to 24,200 psi for the same plate welded with a light-coated low-alloy electrode. Machined V welds in mild steel using austenitic electrodes have a pulsating direct-tension fatigue limit, according to Schick, of 34,200 (25,600) when the breadth of the specimen is 3-1/2 inches, but only 27,000 (16,100) when the specimen is 1-1/2 inches wide; plate thickness in both is about 5/8 inch. No explanation was offered. About the same fatigue strength is developed in austenitic welds in Izett plate.

Cast Steel and Cast Iron

The rotating-bend fatigue limit of cast steel welds is 15,800 psi (bare electrode, cast kerf, 0.28 C, 0.86 Mn, 0.47 Si) according to Jennings[134], and about 10,500 psi (0.24 C, 0.6 Mn, 0.8 Cr, 1.2 Ni, 0.4 Mo; shielded arc), according to White and co-workers[137]. Both investigators and Sulzer[65] (T joints) give fatigue values for steel casting-to rolled steel welds.

The rotating-bend fatigue limit of cast iron (3.12% total C, 2.34% graphite, 2.65 Si, 1.05 Mn, 0.37 P, 0.027 S, 0.06 Cu) with and without the cast skin, which had a very fine graphite eutectic structure, has been investigated by Bartels[38] using short-cycle as well as the usual Wöhler methods. The ends of 0.79 inch bars were turned to 45° cones and cold welded by gas with a cast iron rod (3.4 C, 3.15 Si, 0.9 Mn, 0.7 P, trace S) using a flux. The specimens with skin were vertically cast and were welded in a jig to hold the bars concentric. The results are given in the table at the top of the next page. The annealed specimens were brought to a yellow heat with two torches, held several minutes, and cooled in sand, or held 3/4 hour at 950-1000°C in an electric furnace. Bartels concludes that the decrease in fatigue strength by welding is proportionately less than the decrease in tensile strength, for specimens without skin, and that annealing is not beneficial to fatigue properties. A few tests on specimens containing two welds in the stressed length gave higher fatigue strengths than single welds.

Rorating-Bend Fatigue Limits of Welded Cast Iron. Bartels, 1930

Cast Iron	Tensile Strength		Endurance Limit (10×10^6 Cycles) psi		
	Unwelded	Welded	Unwelded	Welded	Welded Annealed
without skin	27,000	10,000	10,700	8,500	8,500
with skin	38,400	35,200	28,500	25,600	22,800

The cantilever fatigue limit of gas welds, 45° V, in 1-inch cast iron (3.46 total C, 0.74 combined C, 1.33 S, 0.106 Si, 0.66 Mn, 0.282 P) using cast iron welding rods was 12,000 psi; the unwelded cast iron gave 13,500, according to Mochel[138].

Brazing

Pulsator tests on two "Sildo" brazed specimens (composition not stated) of mild steel, 1-1/2" x 1/4" cross-section, 10^6 cycles, gave 28,500 psi and 32,200 psi as the pulsating tension fatigue strength by the step-up method, as Keel[139] has shown. He also found that the reversed bend fatigue limit of the brazed joint, 3/4" x 3/8" cross-section was 20,000 psi. McManus[135] states that a brazed joint (aluminum bronze) in steel containing 0.32 C, 3.4 Ni, is inferior in reversed bending, Upton-Lewis machine, than welded joints using alloy steel electrodes.

Non-ferrous Metals

Only one investigator, Bartels[38], has made an extensive study of the fatigue properties of welds in non-ferrous metals and alloys. Bartels determined the rotating-beam fatigue limit of copper (99.62% Cu using copper welding wire and a flux), aluminum (weld analysis: 98.6 Al, 0.7 Cu, 0.2 Fe), silumin (weld analysis: 10.14 Si, 0.46 Cu, 0.70 Fe), and copper-silumin (weld analysis: 9.80 Si, 1.03 Cu, 0.77 Fe; pure silumin rod). All specimens were cold welded with acetylene. The results are summarized in the following table.

Rotating-Bend Fatigue Limits of Non-ferrous Welds. Bartels (1930)

Material	Tensile Strength (psi) Unwelded	Tensile Strength (psi) Welded	Endurance Limits (10×10^6 Cycles) Unwelded	Endurance Limits Welded	Endurance Limits Welded Annealed
Copper	39,000	17,700	12,100	5,700	6,400
Aluminum	17,400	13,400	~8,500	~8,500	-
Silumin	19,500	6,250	7,800	10,700	5,000
Copper-Silumin	16,600	10,100	9,300	11,400	-

The copper welds were peened at a red heat; the annealed specimens were heated to a bright red for ten minutes and cooled in sand. The aluminum welds were also peened, but the cast silumin welds were not; the annealed specimens were torch heated for about ten minutes and cooled in sand. The torch-annealed copper-silumin welds were so brittle that they broke during machining. Understressing raised the apparent fatigue limit in both the aluminum and the silumin welds, and a specimen of silumin containing two welds in its stressed length had a fatigue limit of 12,100 psi.

The reversed-bend fatigue limit of welded copper, aluminum and Aldrey wires (about 0.1 inch diam.) has been determined by Friedmann[79] whose results are given in the table at the top of the next page. The problem of preventing fracture in the grips of the Föppl-Heydekampf machine, which applies a uniform bending moment over the entire specimen was solved by using cardboard packing and cold-rolling the ends of the wires in and near the grips. The heat of welding softened the hard-drawn aluminum wire and heat treated Aldrey so that fracture occurred in the weld even after filing. After being hammered, the Aldrey specimens usually broke in the base metal.

Reversed-Bend Fatigue Limits of Welded Non-Ferrous Wire. Friedmann (1935)

Material	Fatigue Limit S_f(2 x 10^6 cycles)	S_f(Welded) / S_f(Unwelded)
Soft Copper	9,500 - 10,200	0.50 - 0.57
Aluminum (99.5% Al) Filed	< 5,500 - 6,600	0.50 - 0.59
Aldrey 0.4-0.7 Si 0.3-0.5 Mg "	8,100 - 12,200	0.51 - 0.77
" welded, filed, hammered	10,700 - 12,400	0.67 - 0.78

Aside from the fatigue value of gas welded copper given by Laute (see the following section on Corrosion-Fatigue) and a note by Horn(140) on the fatigue value of cupro-nickel, no other information on the fatigue strength of welded non-ferrous alloys appears to be available. Horn found that the reversed bend fatigue limit of cupro-nickel (20-30% Ni) welded with a weak flame was 16,500 psi but was only 13,000 psi when welded with the type of flame usual for steel; the filler rod had the same composition as the plate, which had a fatigue limit of 23,400 psi.

CORROSION FATIGUE

The rotating bend fatigue strength of machined welds in tap water at room temperature has been stuided by Harvey and coworkers[25], whose results are summarized in the table below.

Material	Welding Process	Rotating bend fatigue limit psi(70×10^6 reversals) In Air (G E Thornton)	In Tap Water
Firebox steel about 0.20 C	unwelded	32,500	14,000(McAdam)
"	Flash	25,000	24,000
"	Oxy-acetylene	12,500 (low-carbon rod)	19,000
"	Atomic hydrogen	12,500	17,000-18,000
"	Covered electrode	-	14,000
"	Bare electrode	7,500 (X weld)	9,000
18-8 (0.07C)	Unwelded	28,000-29,000	34,000
"	Flash	-	40,000
"	Oxy-Acetylene	-	10,000-12,000
"	Atomic-hydrogen	-	29,000

The results show that the rotating bend fatigue limit of welds in mild steel in tap water is generally higher than in air. The heat treatment of the welded 18-8 specimens was not the same for the different welding processes. Harvey concluded that when the welded joint has a higher corrosion fatigue limit than **base** metal the weld is cathodic and is not attacked by the corrosive agent. Grain size did not appear to be a factor in corrosion fatigue.

Using a high-frequency direct tension-compression machine (30,000 cycles per minute), Laute[141] obtained the following values (see next page) for welded and soldered mild steel and electrolytic copper. The criterion of fatigue limit was 100×10^6 cycles; at this stage all the Wöhler curves had become horizontal except the curve for unwelded mild steel in tap water. The results with mild steel agree with Harvey's in that welding actually improves the corrosion fatigue resistance. It is doubtful therefore whether the explanation offered by Roš and Eichinger[7] is correct, namely that poor penetration and other defects in welds, as in cast iron, are far more important than small superficial defects, such as corrosion pits. The effect of corrosion on the fatigue value of welded copper is negligible, the low values being due to coarse grain structure.

Corrosion Fatigue Results (Laute)

Material	Condition	Fatigue Limit psi	
		Air	Water
Mild Steel	Hot Rolled	24,100	9,200
" "	Arc Welded	14,500	9,500
" "	Soldered	14,200	9,900
Copper	Cold Drawn	15,600	15,600
"	Annealed, Coarse Grain	9,400	9,400
"	Gas welded	3,000	2,400
"	Soldered	5,300	5,800

The reversed-bend fatigue limit of a brazed joint in mild steel was found by Keel(139) to be unaffected by tap water. The fatigue limit was close to 20,000 psi both in air and tap water, but the results of only one specimen are reported and the step-up method of loading was adopted. A reversed-bend fatigue machine for testing large specimens of riveted and welded boiler plate in hot and cold corrosive agents has been described by Gough and Clenshaw(142) but their tests are still in progress.

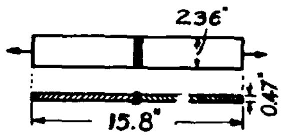
METHODS OF DESIGN

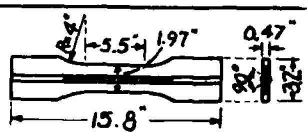
Methods of designing welded structures on the basis of fatigue have been discussed on a number of occasions, especially during the past few years, and have been embodied in the national standards of Germany and Austria and in important specifications in Switzerland and the U. S. A. These methods of design are complicated and will be only briefly summarized in the following paragraphs.

Germany

Although, strictly speaking, the national standards of Germany (e.g. D I N 4100: Specifications for Welded Steel Structures, 1934) give no design information, the new Specifications for Welded Plate - Girder Railway Bridges[143] adopted in August 1935 by the German State Railways, fully outline the methods of design, and, in addition, require that electrodes pass fatigue tests. The specifications apply not only to plate-girder railway bridges (not highway bridges but to traveling platforms (not traveling cranes) and to turntables. Both mild steel (52,500 psi minimum tensile strength, St 37) and low-alloy high-strength structural steel (74,000 psi min. tensile strength, St 52) are covered by the specification.

In addition to the usual static, bend, and notch-impact tests, filler rods for railroad bridges must attain the following strengths.





Type of joint	Pulsating Tension Fatigue Strength psi Mild Steel (Min. tensile strength 53000 psi)	Low-Alloy Structural Steel (Min. tensile strength 74,000)
Double V transverse, unmachined	19,900	21,300
" transverse, machined*	24,200	25,600
" longitudinal, unmachined	24,200	25,600

*machining marks lie in the axis of loading

These minimum fatigue strengths must be developed at 2×10^6 cycles (lower stress = 1500 psi) in a pulsator test, as determined from a plot of log cycles vs stress. The scarf angle of the transverse weld is that dictated by the type of structure involved. The angle is 90° for the longitudinal specimen and the ratio of weld to total cross-section is 0.12. The longitudinal specimen is new and no definite tensile elongation is prescribed; however, the usual bend test must be passed. The specimens are made as good as possible. No other country includes fatigue tests in welding rod specifications. At present it is considered that the machined transverse specification is the easiest to fulfill using cored or coated electrodes and probably also bare. The requirements of the unmachined specimen will force progress in developing electrodes that will yield notch-free welds. The longitudinal specimen depends principally on the weld itself and is the most stringent test. It is not considered a disadvantage if the electrode passes in only one of the two specimens, that is, if two different electrodes must be used on the job.

Fundamentally the same system of computing permissible working stresses is used as in riveted construction. The diagrams showing permissible stresses of welds in all types of fatigue and service, Figs 14 and 15, are based on the Kuratorium tests(6) discussed in the section on Results of Tests and on the fact that joint quality is variable. Line I (a), (b) refers to the base metal in the welded bridge and is used in calculating cross-section, weight, and economy of welded construction. For St 37 this line is identical for welded and riveted construction; for St 52 the riveted has higher permissible stresses. All Line II butt welds must be X-rayed. Reverse welding must be done unless it is structurally impossible and butt welds to joint plates are always machined. Notches must be machined from butt welds to web plates if the range of pulsating stress is greater than 15,900 psi.

The stresses for welds which are not definitely included in Figs 14 and 15 are calculated by the so-called "Gamma" method,

$$S = (\text{Gamma})\frac{Mc}{I}$$

where M = maximum bending moment (algebraic)

$\frac{I}{c}$ = section modulus

S = stress in weld designed for fatigue, and

Gamma = the fatigue factor.

The algebraic maximum bending moment is that calculated with the numerically largest static plus traffic load including the impact factor but not including fatigue. The values of the Gamma factor are given for all values of min. M/max. M from -1 to +1; for example for St 37 in pulsating tension Gamma = 1.0, but for St 52 in heavy traffic Gamma = 1.944 when min. M = - max M.

In order to take account of the fact that the fatigue strength of a weld depends on the type and form of joint and deposit, the stress determined by application of the Gamma factor is further reduced by a shape factor, "Alpha".

$S = \frac{(\text{Gamma})\,Mc}{(\text{Alpha})\ I} \leqq$ permissible stress (19,900 psi for St 37; 29,900 for St 52). A list of 20 values of the alpha factor is given in the specifications for both steels. The fundamental values of alpha for tension (or compression) and shear are 1.0 and 0.8 respectively for unwelded base metal in the form of beams, etc. and cover plates in both steels. The values of both alpha and gamma factor are derived from Figs 14 and 15. According to Klöppel(144), truss bridges are not yet welded on the German Railways on account of the low fatigue strength of fillet welds. Adrian(145) states that certain specifications for Stationary Boilers require welding rods to pass an alternating tension fatigue test as well as notch-impact and age-notch- impact tests.

Switzerland

The enactment(146) of the Swiss federal authorities and the Swiss Association of Engineers and Architects concerning the design, construction, and maintenance of structures in steel and reinforced concrete contains design methods for welds based, like the German methods, on pulsator tests performed in the Swiss government materials testing laboratories. These tests showed that butt welds in mild steel to which the specifications apply had an average origin fatigue strength (1×10^6 cycles) of 19,900 to 22,800 psi whereas different types of fillet welds gave only 10,000 to 11,400 psi. The diagrams are plotted to the equation

$$S_f = S_s(1 + 0.4\,\frac{A}{B})\ ,$$

where S_f = stress in weld designed for fatigue,

S_s = " " " " " static load,

and A and B are the minimum and maximum values respectively of the forces, moments, or stresses with their algebraic sign. Butt welds in compression are assigned the same stresses as unwelded mild steel. This is somewhat in agreement with the German diagrams which show higher origin fatigue strengths in compression than in tension though the respective yield strengths are the same.

Butt welds in tension are given only 1/1.4 the stresses allowed in unwelded mild steel. Fillet (normal- or parallel-shear) and butt fillet (double T) welds have only 0.5 the stresses allowed in butt welds stressed in tension. The factor 0.5 compares with the alpha factor 0.65 of the German tables for fillet welds.

Austria

The Austrian specifications for Welded Steel Construction(147) (Önorm B 2332, 1934) apply only to mild steel St 37 and to welding rods containing not more than 0.30 C, 0.025 P, 0.035 S, and not less than 0.40 Mn. Design stresses for welds under fatigue conditions (bending, tension, or compression) are calculated by the formula:

$$S_f = 12(1/1 + m\left[1 - \frac{B}{A}\right]) \text{ KG/MM}^2 ,$$

where m is the fatigue factor, equal, for example, to 0.4 for traveling cranes and 0.0 for tanks.

U. S. A.

Design stresses for welded highway and railway bridges under fatigue conditions are calculated, according to the specifications of the American Welding Society (1936), according to diagrams similar in conception to the German and Swiss. The specifications refer to the use of covered electrodes only. The method and examples of its application are completely described and illustrated by numerical examples in Appendix A, pages 38, to 44, of the Specifications.

The Swedish ship inspection service, according to Ringdahl(148), requires fatigue tests on 12 specimens, 0.59 inch diameter, cut from a test weld, of which 8 must withstand 5×10^6 cycles at 14,200 psi without fracture.

Dutilleul introduced a rotating bend fatigue test of V welds in the welding specifications of the French Marine Nationale, Port de Brest, 1933.

Other Methods

Although fatigue investigators have often warned that their results were not to be used directly for design (Lohmann[34], Dorey[149], Beckmann[150]), there have been a number of efforts besides those listed in the preceding section to embody fatigue strengths in design calculations. Early attempts made by Stone and Ritter[120], who applied Soderberg's principle to welds, Sandelowsky[151], Fish[152], and others, and recent suggestions by Hovey[153], and Roš and Eichinger[5] were not basically different from the latest methods. All methods apply factors to an assumed or actual fatigue strength, as described in the AWS Specifications. In welded marine construction, according to Brown[154], design stresses are computed with a factor of safety of 3 on the endurance limit of the electrode. Hobrock[84] states that in aircraft structures not more than 80% of the endurance limit may be used in dynamic loads, but he notes that the use of a factor applied to static strength is the accepted method at present. Patton and Gorbunow[155] show that section economy can be achieved by basing the section modulus on plastic rather than elastic deformation. They retain the yield strength and customary design factors in their method and suggest that stresses due to the local heat of welding may be neglected, as Kommerell's tests[143] showed.

A well-developed method of welding design has recently been explained by Bobek[156], who gives an illustrative example of a welded rotor. He points out that in rotor sections, the welds undergo the same fatigue cycles as the shaft due to its rotating bending. To the stresses computed according to standard German practice, Bobek adds 20% to allow for shrinkage stresses and stress concentrations caused by shape, thus obtaining the average stress S_m.

$$S_D = S_m \pm S_A$$

where S_A is the alternating stress. S_D is then multiplied by three factors to obtain the design stress:

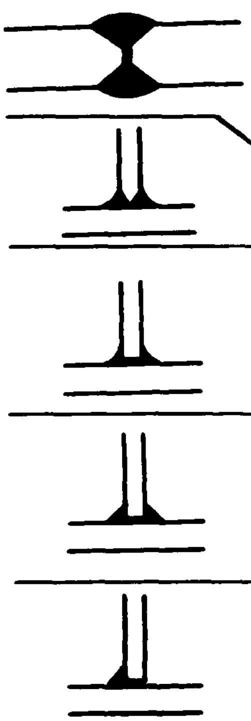
b_1, due to shape of seam

b_2, due to quality of weld, and

b_3, due to the notch effect associated with the type of joint.

The factor b_2 is 1.0 for dense, pore-free welds, and 0.5 for usual structural welds. The factor b_3 is given by the VDI fatigue diagrams for the different grades of steel. Bobek[156] presents the following tentative values for factor b_1.

Fatigue Factors for the Design of Welded Machinery. (Bobek,[156] 1936)



Type of Joint	Kind of Load	b_1
Double V	all	1
Single V	all	1
Double V with gap	A	0.6
Double V with gap	B	0.8
Double V with gap	C	0.6
Symmetrical tapered butt fillet	all	0.9
Symmetrical fillet without taper (concave)	A	0.7
Symmetrical fillet without taper (concave)	B	0.9
Symmetrical fillet without taper (concave)	C	0.7
Symmetrical fillet without taper (flush)	A	0.6
Symmetrical fillet without taper (flush)	B	0.8
Symmetrical fillet without taper (flush)	C	0.6
One-sided fillet without taper (flush)	A	0.4
One-sided fillet without taper (flush)	B	0.2
One-sided fillet without taper (flush)	C	0.4

A= tension B= compression
C= shear, parallel and perpendicular to seam

All welds are assumed root welded, and with combined loads the less favorable value of b_1 is chosen.

REPEATED IMPACT

Although the repeated impact test has been rarely used in this country and was discarded many years ago in England, it is finding increasing favor in Germany(157). A test is known as a repeated impact test whose cycle of load consists of a relatively long rest period and a short load period, the load usually being a falling weight and the maximum stress induced in the specimen being in the neighborhood of the yield point. Such a load cycle is often encountered in machine parts and bolted rail joints, but seldom in structural members. Repeated impact tests on welds are used with three intentions:

1. of measuring a material constant;
2. of determing the general endurance properties of a weld quickly: a quasi fatigue test;
3. of reproducing service conditions.

The machine for tests of the first type is the Stanton, or a bend-impact machine designed on its principles, such as the Eden-Foster or the Krupp. The specimen for the latter machine is cylindrical and is notched in the center of its span. The impact is applied as a four-point bending load at equal distances from the notch, which usually has a generous radius, about 0.3" radius, 0.04" deep. The impact load is a weight actuated by an immediate-release cam and the specimen is usually rotated 180° between blows. By not allowing the hammer to strike the critically loaded section, complications due to work hardening are avoided. With heavy impacts the results correlate with the single-blow impact test, but with relatively light impacts requiring thousands of blows to fracture, the results may or may not correlate with the fatigue test depending upon conditions and the material. The number of blows and angle of rotation are stated as the results of the test.

The machine used for reversed bend impact of T welds (Stromberger machine) is cam-actuated but the impact loads are compressed-air pistons giving 200 cycles per minute. In this, as in the other machines, the stresses due to impact are not known and comparative results are obtainable only on specimens with identical dimensions, especially of the notch. In general, repeated impact tests are more severe than smooth-cycle fatigue tests.

The first, yet most comprehensive, study of the resistance of welds in mild steel to repeated impact was carried out in 1928 by the British Engine, Boiler, and Electrical Insurance Company[158]. Flush machined specimens were tested in repeated reversed-bend impact (Arnold machine). The ratings of the different welding processes were: coated electrode, 100%, gas (Swedish iron rod) 40%, carbon arc and bare wire, 10% of the number of blows withstood by the unwelded plate. Welds free from oxides and nitrides gave the best results. Normalizing at 910°C had little effect.

The harmful effect of nitrides and annealing on double V arc welds in four grades of structural steel was also observed by Lohmann and Schulz[34] whose results show that for the repeated bend impact test (Krupp machine, round notched specimen), electrode composition and the static tensile strength of the plate are the important factors. Capacity for deformation and fatigue value are not revealed nor is there any relation with the notched or unnotched single-impact test. Best results in plain carbon steels (0.1 or 0.25% C) were obtained with a bare alloyed (0.4 Cr, 0.7 Cu) electrode. Covered electrodes were better for the low-alloy structural steels. In all cases annealing lowered the number of blows (Krupp machine) by 10 to 70%, the reduction being greater the higher the nitrogen content. The best value for any weld was only about 70% of the unwelded plate. However, higher values for arc welds (coated electrodes) than for the unwelded mild steel plate are reported by

Passau[159] and by Joellenbeck and Massmann[160], and Schoenmaker[161] found that welds made with high-quality coated electrodes withstood 85% as many blows as unwelded plate.

The above results on machined specimens leaving the repeated impact question to a large extent open, Thum and Lipp[15], using a Stromberger machine, investigated the comparative repeated impact value of 14-inch, unmachined T joints made of cast iron or cast steel, or welded (bare electrode) in mild steel. Wöhler curves to 10^6 cycles plotted on the basis of kgcm per blow became horizontal for the cast steel at 5.4 in. lb., for cast iron at 3.5 in lb., but the curve for the welded T was not yet flat at 3.5 in. lb. The shape of the weld is of vital importance as shown by Fig. 16. Although in specimen 3 about half the weld deposit was removed by filing, the Wöhler curve most nearly approached that of the unwelded specimen. Machined specimens also had smoother fractures, an indication of better dynamic behavior, and gave much less scatter. Concave welds deposited by coated electrodes, specimen 4, showed little ultimate superiority over bare-wire welds.

The repeated impact value of gas welds in mild steel may be lower than in cast iron, as Bartels[38] has shown in the table on the next page. These results are for light impacts (4.4 lb. load, 0.39 inch drop). For heavy impacts (8.8 lb., 0.39 inch) the mild steel welds are superior, however. The surprising fact that a cast iron weld having less than 10% the single-blow notched-bar impact value of welds in mild steel is more resistant to repeated light impact than the latter seems to be explained only by considering damping capacity. As von Heydekampf has shown, cast iron has a high damping capacity and is consequently dynamically ductile and insensitive to destructive notch and resonance effects. The results suggest that the welding of mild steel with cast iron might involve improved repeated-impact resistance. The exceptionally high values for the welded cast iron may be related to some extent to its fine fracture; the unwelded cast iron had a coarse fracture. Bartels' results on non-ferrous welds break new ground, indeed. An unnotched cast Silumin specimen containing two welds withstood 137,000 light blows (4.4 lbs.). Bartels also states that unmachined welds were generally more resistant to repeated impact than unwelded specimens in ferrous materials.

Bartels' Results (Krupp Machine), Machined Specimens

Repeated Impact Value, Number of Blows in Thousands			
Material	Unwelded	Gas Welded	Welded, Annealed
Cast iron without skin	54	2,305	219
Cast iron with skin	92	3,000 *	73
Mild Steel	1,300 *	1,748	854
Copper	165	16	18
Aluminum	14	1	0.6
Silumin	8	11	10

* unbroken

Rosenthal(162) has also thrown light on the obscure dynamic characteristics of welds. The repeated impact value of a ductile double T arc weld, he found, was only one-half that of the unwelded mild steel. By cold hammering the junction between plate and reinforcement, he was able to make the weld withstand as many blows as the original plate. It appears therefore that Thum's rule, according to which light impacts within the elastic limit raise the fatigue value of the struck part, is valid also for welds under repeated impact. It remains to be seen whether reduction of static ductility and simultaneous increase in dynamic ductility or strength in the cold-worked weld is connected with damping capacity, as in cast iron, or simply with imposed compressive stress. The damping capacity of welds, particularly porous welds, does not seem to have been investigated. The statement by Prox(163) that inclusions and blowholes have little effect on the repeated impact value of welds may be significant in this connection.

The repeated bend impact test has been applied by Schmit(164) to surfaces built up by welding and by von Roessler(165) to flame-cut surfaces. Schmit found that, for surfacing plate and cast steel with low carbon steel, gas was superior to the DC arc, and that it is the deposit, not the heat-affected zone, that injures the repeated impact resistance. The flame-cut surface, as Roessler shows, is equivalent to a milled, and only about 10% inferior to a planed surface in repeated impact for four types of structural steel. If the machining grooves were at a large angle to axis of impact, or if the flame-cut surface was subsequently ground, the original flame-cut surface had superior repeated impact value. Repeated bend impact tests made under the auspices of the British Acetylene Welding and Consulting Bureau(166) on mild steel specimens 2-1/2" wide, 3/8" thick, showed that welds made in flame-cut scarfs were superior to those deposited in machine-cut scarfs.

Unwelded Plate - - - - - - - - -	7,759	blows
Arc Welded Machine Cut Bevel -	8,483	" (average of 5 specimens)
" " Flame " " -	9,092	" (average of 5 specimens)

The weld just protruded from the clamp and the blows were struck 5" from the weld at the rate of 600 per minute.

As a laboratory test - for the repeated impact testing machines have an astonishing number of variables, relatively few of which have been investigated - the repeated impact test has undoubtedly yielded important information on properties not prominent in static or smooth-cycle fatigue tests. It must be emphasized that the test will remain empirical in nature until reliable, simple methods for computing impact stresses in the vicinity of notches have been developed. Daeves(157) and Schoenmaker(161) recommend the test whereas Rolfe(167) and Schuster(168) regard it as unimportant.

The repeated impact test has occasionally been used as a quick substitute for the long-time fatigue test. In view of the fundamental difference between the cycles of loading in the two tests, it is not surprising that such quasi-fatigue tests have been misleading.

The third use to which the repeated impact test has been put is the most frequent, namely: to reproduce service conditions. Whereas in the first type of tests the number of blows usually exceeds several hundreds, the service repeated impact test may use only five or ten blows or may exceed 10^6 depending upon the object of the test. Santilman(169), for example, reports a six-drop test on a partly-riveted, partly-welded bridge member, Verzillo and Pizzuto(170) describe a shop repeated impact machine for rotating crank axles, and the Rail Joint Committee subjected some joints to over 3×10^6 cycles. Taylor and Jones(171) tested 7 inch welded I beams (intermittent shielded arc welds) made of silicon steel (0.27 C, 0.95 Mn, 0.26 Si) and plain-carbon structural steel in repeated bend impact (38 cpm) under a punch press. With the welds stressed to 79% of the static tensile strength, the silicon steel beam failed earlier than the plain carbon. In a supplementary test a silicon steel beam withstood 210,000 cycles at 40% of the static tensile strength without failure. Aside from the tests on rail joints perhaps the most informative repeated impact test was that made by Jurczyk(172) on fillet arc welds, a model of such a weld machined from a single piece of steel, and a double-riveted joint. The welded specimens were equivalent to the riveted in repeated tensile impact and were 20% better than the machined models which had equal static tensile strength.

Tests of the endurance of rail joints are of three types: repeated impact, pulsating stress (smooth load cycle), and service. The most extensive tests of the first type were carried out by the Rail Joint Committee[173] who devised a special machine with an eight-ton anvil for the purpose. The 400 lb. hammer struck the head of the joint centrally; the span was 22", height of fall 6", and rate 50 to 75 blows per minute. Certain types of seam welded joints gave the best results in this test, but they were not consistently good; butt, thermit, and cast-iron joints followed in order. Using a similar trip-hammer machine (525 lb hammer, 40" between supports, 80 blows per minute, 5" drop), Jurczyk[174] found that seam-welded joints made by a patented arc welding process were 10% better than thermit joints, and six times better than the bolted joint. The Krupp machine, on the other hand, gives first choice to flash welded rail steels, thermit joints having only one-third as much repeated impact value, as Reiter[80] showed.

The second and less severe type of test; pulsating stress, has largely displaced repeated impact because pulsator results are in terms of a fatigue limit and are not merely comparative. Besides, the smooth load cycle of the pulsator more nearly duplicates service conditions in welded joints, which eliminate hammer and anvil effect.

Stressing joints in reversed bending, Roš and Eichinger[37] determined in 1932 that the thermit joint with a fatigue limit of over $\pm$ 16,000 psi was superior to their best arc welded joints which failed below this value. Repeated impact tests, however, rated the joints in the reverse order, which was not unexpected because the first blow in a drop test causes plastic flow and equalizes shrinkage stresses and notch effects.

The majority of fatigue tests on rail joints have been carried out with pulsating tensile stresses in the foot of the rail. Melhardt[175] gives 19,200 psi as the pulsating tension fatigue limit for seam-welded joints containing 0.1% C and high nickel and manganese. This is somewhat higher than the value 17,000 psi given by Gysen[175] for Thermit joints but lower than that given by Keel[176] for arc welded joints (27,000 psi). In the last instance the unwelded rail had a much lower fatigue value than the joint. Melhardt observed that fracture tended to start at the ends of weld beads on the outer edges of the rail foot. Golling and Tulacz[177] report 27,000 psi as the pulsating bending fatigue limit of oxy-acetylene butt welded rail joints reinforced with welded flange straps (filler rod not mentioned). The unwelded rail had a fatigue limit of 43,000 psi. The transition zone is the cause of the low fatigue value of the welded joints, it is said.

The average joint, according to Keel, withstands 10^6 cycles in 2 to 3 years service, and Csilléry[178] states that, in heavy traffic sections, 3.5×10^6 axles pass over a joint annually. Very few laboratory tests of joints have extended beyond 2×10^6 cycles. Rail joint testing has not yet included the vibration factor which adds only about 10% the maximum pulsating stress but operates at relatively high frequencies, as Adler[179] has shown.

CREEP

The amount of quantitative data on the creep properties of welds is surprisingly small. The limiting creep stresses determined by Stäger and Zschokke(180) are given below (no details):

Limiting Tensile Creep Stress lb/in^2

Material	300°C	400°C	500°C	
Mild Steel Plate	31,200	15,600	5,700	
All Weld Metal, gas	18,500	8,500	1,400	
All Weld Metal, arc	19,900	9,900	2,800	
Welded Joint, gas	25,600	12,100	5,700	
Welded Joint, arc	25,600	15,600	5,700	
Welded Joint, arc	-	14,200-15,700	-	(Appaly)

Being a composite of plate and weld metal, the welded joint displays creep properties intermediate between them. Above 400°C the welded joint is equivalent to mild steel. Keel(181) used the Amsler and the Brown-Boveri creep machines for creep tests on oxy-acetylene butt welds in 0.39" boiler plate (tensile strength 60,000 psi; weld metal analyzed 0.19 C, 0.80 Mn, 0.30 Si). Plotting stress vs creep velocity and adopting 0.0024% strain per day as criterion, the creep limits at 400 and 500°C are

up to 400°C - 15,400 psi
" " 500°C - 4,600 psi

Keel concluded that oxy-acetylene butt welds have between 70% and 75% of the creep strength of cast steel of similar analysis.

An intensive study of creep of welds in boiler plate (0.09C, 0.5 Mn, heavy coated electrodes) at 400°C by Appaly(182) showed that the creep velocity of a welded joint consisting of six double V welds in a gage length of 5-1/2 inches is much lower than would be expected by averaging the creep rates for unwelded plate and all-weld-metal. The welded joint has its own specific creep properties probably derived from a combination of microstructure and stress distribution. The creep stress determined by the long-time method (time of test was up to 800 hours, and velocity = 10^{-5}% per hour) was 12,800 psi for plate and 14,200 to 15,700 psi for the weld specimen. After annealing (1/2 hr., 930°C) the creep stress of the weld specimen was reduced to less than 12,800 psi but the plate did not appear to suffer correspondingly. Stress annealing (1/2 hr., 650°C) did not lower the creep stress of the weld so greatly as full annealing. The ill effects of annealing are tentatively said to be due to coarse grain structure. Appaly found that Sauerwald and Juretzek's short-time method (creep stress is defined as elbow in plot of log elongation velocity at the end of one hour vs stress) is quite accurate for welds and is superior to Pomp and Ender's method (creep rate between fifth and tenth hour less than 0.003% per hour.

Lea and Parker[21] determined the creep stress giving a creep rate of 1×10^{-5} and 1×10^{-6} inch/inch/hour at the twentieth day at temperatures between 325 and 575°C using machined all-weld-metal specimens deposited by an electrode analyzing 0.15C, 0.10 Si, 0.57 Mn and having an iron silicate coating containing manganese. At the critical temperature of water and steam, 374°C, the creep rate (20 days) was less than 1×10^{-6} in/in/hour at 22,400 psi. At 490°C the corresponding stress was less than 9,000 psi. The creep characteristics of weld metal are not so good as the steel used in superheater tubes (analysis not given) but are excellent except under conditions of superheat. A covered electrode, shielded arc (analysis not given) was also tested but was inferior to the slag-coated.

Creep rates in welded steam station piping at 850°F (455°C) determined by the single-step method are reported by White, Corey, and Clark[137].

Rate of Creep at 850°F., 15,000 psi tensile. % per 100,000 hrs.

Pipe material (0.33 C, 0.75 Mn, 0.04 Al (metallic)	1.1
Welded pipe-to-pipe	1.2
Welded pipe-to-casting (0.24C, 0.62Mn, 0.82Cr., 1.19Ni, 0.40Mo)	1.5 *

*reported as 1.3 in Publication B5, Prime Movers Committee, Edison Electric Institute, 1934.

The welds were made by the shielded arc process and were drawn at 1100°F. The test results were not so consistent for the welds as for the unwelded pipe; the duration of the tests was 500 to 600 hours. At a stress of 12,000 psi there was no appreciable creep at 850°F.

The creep characteristics of gas welds in cold-rolled steel at 360, 550, and 1000°F (180, 290, and 540°C) have been studied by Ward[183] who concluded that the welds have excellent creep resistance up to 400°F. In the early stages of creep, welds are not so good as unwelded steel on account of their coarse grain structure. Annealing at 1650°F is detrimental to the creep properties of welded and unwelded cold-rolled steel. According to Roš and Eichinger[7] the creep limit of plate metal, especially under compressive stresses, may be very materially decreased in the vicinity of welds.

In view of the probable dependence of creep behavior on distribution of stress and shape of specimen, Bugden[184] determined creep data for the design of welded steam piping from actual Dawson joints. The weld (covered electrode) of such a joint in mild steel pipe (0.05%C) withstood a stress due to internal pressure of 87% of the proportional limit for 1460 hours and in addition, a bending moment of 12 ft. tons, which increased the combined stress in the weld to 95.6% of the proportional limit for 1220 hours at 805°F (430°C) without objectionable creep. The average diametral creep rate of the weld was about 5 times that of the unwelded pipe, however.

It is F E Smith's[185] experience that tubes of hydrogen-resistant steel cannot be joined by any ordinary process of arc welding without seriously affecting creep strength. The resistance butt welding process is therefore used.

Short-time high temperature tensile tests of welds have been reported from a number of sources. In general the welds are not inferior to unwelded material although Ward's results indicated that gas welds in the temperature range 75 to 1450°F had only about 70% of the tensile strength of unwelded annealed plate. In the design of boilers Schuster[88] recommends that above 250°C the rated stress in the weld should be reduced below unwelded plate. There is a serious lack of numerical information on the thermal coefficient of expansion of weld metal, a factor which is especially important for austenitic welds in non-aging steel, as Hessler and Kautz[186] have shown.

Summarizing, the creep strength of welds in mild steel is probably little if any inferior to unwelded plate up to 500°C although the initial creep rate may be somewhat higher; full annealing is not beneficial. That there will shortly be more information on the high temperature properties of welds is indicated by the fact that the research programs of several technical bodies at the present time include investigations on creep. Since, at the temperature of stress annealing for mild steel, creep is able to eliminate shrinkage stresses it has been suggested that creep is probably a factor in welds of some non-ferrous alloys even at room temperature.

BOILERS

A summary of fatigue tests on welded boilers and drums is given in the table on next page. The cylinder tested by the A O Smith Corporation[187] (Joys and Jasper) was a mild steel penstock having four longitudinal welds. The ends of the penstock were closed by 4" thick hemispherical shells. The pressure during each cycle was built up slowly and released suddenly, 15 shocks per minute. The fracture of the fatigued drum revealed a very coarse grain structure.

The John Wood Mfg. Company[188] tested four tanks with longitudinal seam welded by electric resistance butt compression method. A quick acting valve applied 150 to 160 psi water pressure 13 times per minute. Two of the tanks withstood over 300,000 cycles; the remaining two withstood 210,000 and 90,000 cycles. None of the tanks was ruptured by the tests (fiber stress and type of material not stated).

The tests reported by H F Moore[52] and Hodge[189] embodied a smooth sine-wave cycle of load from zero to maximum, 13 cycles per minute. Over 25 shells were tested of which only a few are listed in the table. The plate for the arc welded shells was ASME code plate (55,000 psi tensile). Details of the Babcock and Wilcox processed electrode are not given. Slag was visible in the fracture of some of the welds. All drums were stress annealed at 650°C, and the

Summary of Fatigue Tests on Welded Boilers

Reported by	Dimensions, etc.	Fiber Stresses in weld, psi lower	upper	No. of Cycles	Location of Fracture
Joys & Jasper, 1925	45" i.d., 8ft long, 2" wall shock cycles, bare electrode	mainly 11,250	mainly 22,500	7,000	Burst at static stress of 32,600psi in plate metal
H F Moore, 1929-31	42" i.d. 2" wall smooth cycles, bare electrode	0	16,500	5,500	Junction zone of longitudinal weld
"	Special electrode	0	16,500	270,000	Slag in weld
"	B & W Processed Electrode	0	"	1,000,000	No failure
"	" " "	0	"	2,000,000	" "
"	Same drum as preceding	0	22,000	50,000	" "
"	Hammer welded	0	16,500	900,000	" "
"	A.S.M.E. St'd Riveted	0	16,500	1,000,000	" "
John Wood Mfg Co., 1931	Electric Resistance Compression Butt welds. 12" i.d. 48" long, shock cycles 0.101" wall	-	-	over 300,000	No failure
H W Hawkins, 1932	36"i.d, 10ft long, 3/4"wall B.&W. Electrode Shock cycles	0	20,325	320,000	Short, fine crack in Longitudinal Weld
"	Riveted, 1-1/16" wall	0	"	"	No failure
M Ulrich, 1933	26-1/2" i.d.9/16" wall Pintsch black electrode Smooth cycles	3,840	23,800	156,750	Junction of surfaces of weld and plate in long.weld
"	" "	"	"	98,700	Longitudinal weld
"	" "	"	"	10,000	" "
"	As above but with cover plates welded on after normalizing	"	"	203,000	Undercut of normal-shear weld of cover plate
K Kautz, 1935	29" i.d. 26" long, 1.1"wall Austenitic electrode, shock cycles.	1,400-2,850	19,900-23,000	520,000	No failure
"	Same Drum as Preceding	1,400-2,500	25,600-28,800	107,000	" "
"	" " " "	2,450-3,800	29,200 32,300	36,000	Short crack (1 mm wide) in longitudinal weld
"	26-1/2"i.d. 55" long, 9/16" wall, Austenitic electrode	3,980	23,900	400,000	No failure.
	Same drum as preceding	3,980	26,800	55,000	Crack in longitudinal weld, ground out and re-welded
	" " " "	"	"	85,000	Crack in longitudinal weld.

reinforcement was ground off.

The mild steel drums tested by Hawkins[190] were subjected to stress cycles of a shock order, 6 cycles per minute, at 20,325 psi in the longitudinal weld; the test pressure was 750 psi. The short fine crack mentioned in the column headed Location of Fracture developed in the longitudinal weld at a distance from the circumferential weld. In the subsequent static test the riveted drum started to leak at 27,000 psi fiber stress and the leak developed until pressure could not be maintained at 43,400 psi fiber stress. The welded drum failed at 48,000 psi, fracture starting in the longitudinal weld but terminating in plate metal. Not counting straps and rivets, the welded drum was 42% lighter than the riveted drum of identical capacity.

The mild steel drums tested by Ulrich[191] had two hemispherical ends welded on and were normalized. The cover plates were partly of the Mefi type, partly Höhn type and were welded on after the drum had been normalized. The stress cycle was smooth (sine-wave type), 15 cycles per minute.

The testing arrangements and results of Kautz[55] are described by him in admirable detail. The test drum was made of Izett non-aging steel (0.25C, 0.55 Mn, 0.05 Si, 0.01P, 0.016 S; yield point, 43,500 psi, tensile strength, 71,000 psi). The austenitic electrode (20 Ni, 25 Cr, 0.1 C, 1.3 Mn, 0.8 Si) was deposited in five layers in J-shaped scarfs without backing ring. The stress cycle consisted of a relatively long period at maximum pressure and a short period at minimum, release and admission being sudden, 28 cycles per minute. The dimensions of the drum were autographically recorded throughout the test. The drum was not normalized or stress annealed. Fracture started gradually on the inside of the drum at the junction of the surfaces of plate and bead, and spread outwards until it reached the surface. The second of the drums discussed by Kautz had thicker ends (1-9/16" thick), no man hole, and was made of lower strength steel (Izett I). The stress cycle was of the smooth cycle type, 12 cycles per minute; the drum was not heat treated after welding. The reinforcement was not ground off either drum. The second drum was tested under static pressure at 29,900 psi before the fatigue test. The tests were not carried out at elevated temperatures (e.g. 300°C) in order to avoid any release of shrinkage stresses. Varriot[275] describes apparatus for testing welded pressure vessels under pulsating pressure but gives no results.

The boiler fatigue tests, as Dorey[192] points out, show that fatigue failure inevitably occurs in regions of stress concentrations; e.g., gage plugs, manholes, and pads (this was true in the tests of Moore and Kautz), rather than in the welded seam itself. According to Schuster[88], the number of fluctuations of stresses that take place in service, which is greater than that due simply to starting up and shutting down the boiler, is small compared to the large number in fatigue tests. Nevertheless, he and several others at the Welding Symposium of the Iron and Steel Institute suggested that pressure vessels ought to be designed on the basis of fluctuating loads. Kautz emphasizes, however, that the stresses indicated in fatigue tests should not be used for design purposes. The only unsatisfactory welds in all the fatigue tests were those made with bare electrodes

In 1930 the fatigue value of welded pressure vessels was considered so problematical that the Boiler Code Committee was on the point of inserting a fatigue test in their Code. The test consisted of 10,000 cycles of internal pressure from zero to 1-1/2 times working pressure. A number of fatigue tests were carried out by leading pressure vessel manufacturers, such as the Hedges-Walsh-Weidner Company[276] and the Westinghouse Air Brake Company[277].

TESTS OF WELDED STRUCTURES

Laboratory and Workshop Tests

Laboratory fatigue tests on relatively simple welded elements, such as fillet welds and T joints (Thum and Lipp, and others) have been dealt with in the section on Tabulated Results. Fatigue tests of large-size welded structrual parts such as I-beams were reported by Bissell(205) and Spindler(206). Both investigators tested riveted and welded connections between I- or channel sections, and both found that the welded were entirely satisfactory. Bissell subjected 6" I-beams welded (butt, bare electrode) or riveted to the web or flanges of a 10" I-beam to vibrations (1760 cpm) set up by a square cam. After four hours all riveted connections had failed; the welds were in excellent shape after 18 hours. Additional loads had to be applied to cause the welds to fail. Patton and Gorbunow(155) tested simple and three-support unsymmetrical welded beams of mild steel by an unusual method. The beam was subjected to pulsating loads (lever mechanism, time for one cycle not given) in increasing steps until the beam continued to deform after a large number of pulsations (about 1,500). The stress corresponding to this load was always in excellent agreement with the stress computed using a section modulus based on plastic rather than elastic deformations.

Hochheim(72,100), and Bühler and Buchholtz(17) used the pulsator to test welded I-beams with welded stiffeners above supports and under the applied loads. A plate I-beam with ribbed flanges, and with stiffeners welded to web and both flanges, had an origin fatigue limit (4-point loading) of 25,600 psi (low-alloy structural steel, minimum tensile strength 74,000 psi). A similar beam with stiffeners welded only to web and compression flange had a pulsating tension fatigue limit of 32,700 psi. Hochheim found that beams with stiffeners welded to web and both flanges, and having a drill hole through the tension flange, withstood 250,000 cycles at 41,200 psi. Graf(207) tested a 10 inch mild steel I-beam butt welded (coated electrodes) at the center to provide a length between supports of 10 feet in four-point pulsating bending. The tension flange of the beam was strengthened by a welded-on plate. With stresses in the tension side of the flange varying from 850 to 22,000 psi, in the compression side from 1400 to 37,000 psi, one beam failed in the tension flange after 1.6 x 10^6 pulsations, another beam did not fail after 2.1 x 10^6 pulsations. The test shows that butt welds have considerably higher fatigue value in compression than in tension.

A welded plate girder bridge 30 ft span, 3,700 lbs. of low-alloy structural steel (static tensile strength 74,000 psi) using bare electrodes was tested by Bohny(208) by means of a rotating eccentric pulsator (about 540 cpm). The results are shown in the following table. Fracture occurred almost simultaneously at two places, the main crack occurring at the end of a fillet welded stiffening plate, some distance from the middle of the bridge.

Load Step	Amplitude-inch	Range of pulsating stress in tension fiber, psi: Middle of beam, Lower	Middle of beam, Upper	At point of fracture, Lower	At point of fracture, Upper	No. of Cycles
1	± 0.49	4,300	17,200	3,400	13,700	60,210
2	± 0.79	"	25,000	"	19,800	69,890
3	± 0.98	"	30,700	"	22,500	2,250 (fracture)

Since the failed fillet weld had a pulsating fatigue limit of less than 20,000 psi, Bohny concludes that welded stiffening plates should be terminated as near the bearings as possible. Bierett(209,12) reported pulsator tests on butt welded T beams (4" flange, 11" web) which withstood 2 x 10^6 cycles at 28,400 psi.

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The welded I-beams tested by Kater[133] withstood 3×10^6 cycles at a lower stress of 23,400 psi, upper stress 35,600 psi (low-alloy structural steel, 74,000 psi tensile) and 10.45×10^6 cycles at a lower stress of 13,800 psi, upper stress 25,200 psi (mild steel 52,500 psi tensile), before fracture (dimensions not given). The two steels gave the following results in the complex all-welded S-shaped structure shown in Fig. 17.

Steel	Lower Stress	Upper Stress (2×10^6 cycles)
Mild steel	15,600 psi	31,200 psi
Low-alloy steel	17,100	37,000

The maximum stresses are given in the above table; the welds were not machined. These values are in good agreement with Graf's results for butt welds in the same types of steels. Wilson[76] has reported short quasi-fatigue tests on twelve types of welded girder-to-column connections (coated electrodes). The stress cycle was 25,000 psi tension to 12,000 psi compression approximately at the most highly stressed section. Cracks occurred usually at the weld most highly stressed in tension, but they sometimes originated in the less highly stressed shear fillets of the seat angles. Most of the specimens failed before 20,000 cycles had been reached.

Hochheim[100] also tested a complex all-welded C-shaped structure in the pulsator but gave no results. The correct design for a bent plate with welded or riveted hinges was arrived at by Driessen[47] from pulsator tests. The best design is that in which fatigue cracks just fail to start at the weld-plate surface junction. Schaechterle's observation[91] that service cracks in highly-stressed welded joints in railway bridges occur at the same points as in pulsator tests seems to justify Driessen's procedure of designing complex joints on the basis of experimental fatigue tests. The design of the first welded railway sleeper joints of the German railways was verified[210] by fatigue tests using compressed air hammers, impact pendulums, and alternating-bend devices.

Dustin[211] showed the value of pulsating tension tests on models of Vierendeel bridge members (scale 2.5 to 1.) in designing for maximum strength and economy. The pulsator tests showed that the maximum stresses did not occur where they were expected. Santilman[169] mentions tests of bridges by rolling stock and of bridge members by repeated impact. Details of a repeated oil pressure test of arc-welded 8" expansion joints are given in the Lincoln Prize Papers for 1929. No failure, permanent set, or leaks appeared during the 23-1/4 hour test. Alternating stress tests of arc welded elbows under internal pressure, made by the Taylor Forge and Pipe Works[272] showed the vast superiority of welded over screwed pipe joints.

Full-size fatigue tests of welded engine bed-plates by Sulzer's works are described by Pemberton[212]. A 10" diameter bar supported in the two main bearings of the bed plate was loaded at the midpoint by a hydraulic ram subjected to pressures alternating between zero and 5,000 psi (500 cpm), corresponding to an alternating load of 156,800 lb., the design load being 100,000 lbs. The bed plate was sound and free from defects after 37.5×10^6 cycles. Tests on bed plates of different design led to fatigue cracks after a comparatively short time.

Service Tests

It is beyond the scope of this review to give a complete account of service results of welds under fatigue conditions, either in the form of welded structures or repairs. Dorey(193), Burn(108), and Pohl and Ehrt(56) among others show that welding, unless unintelligently performed, is perfectly reliable. Burn states that the stress-raising and crack-producing capacity of welding is especially exerted in large forgings. According to Salmon and Bernhard(194), secondary bending moments in open-web girders are considerably increased by welding, and the stiffness of weldod frames is unfavorable under fatigue conditions. On the other hand, Bonami and Goelzer(195) state that the rigidity of a welded traveling crane eliminated vibrations and was beneficial to fatigue value(see section on Bridges). The incorrect welding of superheater tubes of different thicknesses is given by Pfleiderer(70) as a cause of fatigue failure. Long operation and repeated cleaning so weakened the forge-welded tubes in water-tube boilers that forge welds, usually low-grade, have been forbidden in these boilers in Germany since 1926, according to Krüger(60).

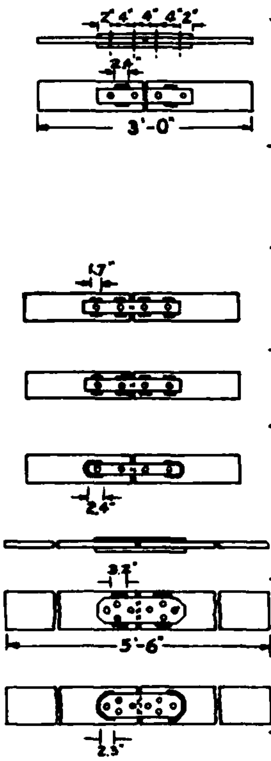
Service results in marine applications, Pierce(196) stated in 1931, have shown that covered electrode welds are equivalent in fatigue value to rolled plate. After 3-1/2 years of service a welded pressure vessel which had been tested at a relatively high stress before being placed in service, was tested to destruction in 1932 by Jasper(197); the vessel developed full strength, failure occurring in plate metal. Dorey(193) has observed that service fatigue failures of welded water-tube boilers occur at points of stress concentration rather than in the welded seams. Stieler(198) described the perfect record of bare-electrode welded locomotive gangways during over 100,000 miles of service. In 1930, Woofter(199) reported that welded truck rims had given no trouble during 12 to 14 years of service, and Cooke(200) in 1911 stated that oxy-acetylene welded goods-wagon frames stood up well in service. Boden[201] and Schinke[202] report excellent succes with welded cars on the German Railways and give methods for avoiding "corner" welds, which are weak in fatigue.

Although the above review has shown that welding, even bare-electrode welding, is reliable under fatigue conditions, it is usually difficult accurately to evaluate the number and magnitude of the cycles that a welded part is expected to withstand. As stated in the section on rails, rail joints in average sections undergo 10^6 reversal a year. In welded railway bridges, each train (locomotive) is considered to contribute only one cycle; a simple calculation then gives the number of cycles undergone during the anticipated life of the bridge. This was the basis of the choice of 2×10^6 cycles as the criterion of fatigue value in the pulsator tests of the German investigators. It is the general opinion that boilers never undergo more than 10^6 cycles of stress during their expected life. Fatigue failures are seldom encountered in aircraft, according to Wagner[203], and this is repeated by Templin and Tuckerman[204], who states that except in wires there are very few fatigue failures in the structural parts of aircraft. Burges[204] thought it hardly possible that there should have been 10^6 reversals in the girders of the Macon. But, as Hobrock[84] states, "there is no certainty as to the number of stress reversals encountered by structural parts during their life in airplanes and airships". This appears to be true to a great extent in dynamic structural design. Gough[142] recently declared that the most common type of service failure in his experience was that of fatigue.

Strengthening by Welding

Practically the only experimental fatigue study of riveted joints strengthened by welding is that of Kommerell and Bierett.[213] Their results are summarized in the following table.

Results of Pulsator Fatigue Tests in Tension on Combined Joints (Kommerell & Bierett, 1934)



Specimen Series	Area of Weld-in	Static Tensile Strength psi	Tensile Fatigue Strength 2×10^6 cycles; psi S_o	$S_o - S_u$	Fatigue Fracture
1	Only riveted	34,400	23,200	16,200	In rivets
2	Only welded but with rivet holes	56,600	24,200	17,200	Started at inner end of welded seam
3a	2.46	61,000	30,600	23,600	Started at inner end of seam in cover plates, then spread to rivet holes
3b	2.46	not tested	30,200	23,200	Same as 3(a)
3c	2.47	66,300	26,600	19,600	Started at inner end of seam in cover plates, then spread thru plate
3d	3.47	70,500	26,400	19,400	Same as 3c
3e	2.47	not tested	32,200	25,200	In plate at junction of plate with normal shear weld
4	Only riveted	38,400	21,000	15,000	In rivets
6	3.92	38,100	22,500	16,400	Same as 3(a)
7	3.92	56,500	22,500	16,800	In cover plates thru innermost rivet hole

S_o = upper stress in pulsating tension fatigue
S_u = lower stress in pulsating tension fatigue

All combined joints were riveted in the usual way. The joints of series 1 and 3 were loaded about ten times between 7,000 psi and 14,400 psi tension (all stresses are related to the weakest cross-section through rivet holes). The joints of series 3(b) were given 1×10^6 cycles between these limits. The large joints were loaded between about 6,000 and 15,000 psi about 10 times; only purely elastic deformations were found after these pre-loadings which were designed to simulate service loads on a bridge. The specimens, which were of mild steel, were then welded under the lower pre-load with bare electrodes, 170 amps DC. The temperature of the rivets did not rise above 200°C. The Losenhausen pulsator (500 cpm) was used for the small specimens; the pulsating bridge (210 to 230 cpm) for the large specimens. The fatigue limit is defined, as usual, as the stress indicated by the Wöhler curve at 2×10^6 cycles, and is quoted on the reduced cross-section, although some specimens broke in the original cross-section. The Wöhler plots had by no means flattened out at 2×10^6 cycles.

The results show that series 3(c) and 3(d) are not good in fatigue, the fatigue stress being only 19,000 psi based on the actual area of fracture. Series 3(e) is better than 3(a) because the fatigue value of the cover plates is fully developed as in series 7. The low fatigue value of 7 as compared with 3(e) is explained probably by stress distribution and larger dimensions. The dynamic pre-loading of series 3(b) did not noticeably affect the fatigue strength. The larger seam cross-section of 3(d) as compared with 3(c) is not effective in fatigue. The welding in combined joints should be so located that the beginning of the seam lies in the cross-sections in which a greater proportion of the load is already transferred through the rivets to the plates. In the larger types of riveted joints it is not sufficient to take into account only the innermost series of rivets. In this way the notch effect due to the end of a seam is transferred to a less highly-stressed location, and the innermost rivets are relieved because the bellying compressive force on these rivets due to contraction of the rivet holes is decreased.

Kommerell and Bierett conclude, in their later report, that the effect of strengthening by welding is not so great in fatigue as in static load conditions. Series 3(c) and (d) are better in static load than series 3(a) and (e); in fatigue the position is reversed. The clamping force of a riveted joint is actually raised by the increased loads placed on the joint after strengthening by welding. Donkin(214) states that welding intended to strengthen riveted joints must be designed to take the whole load in order that plastic yielding will not take place in the neighborhood of the weld and lead to fatigue failure. A similar recommendation is made in the Bridge Specifications of the A. W. S. (See Bernhard's eccentric-pulsator tests on strengthened bridges; section on Bridges)

Comparison of Welding with Riveting

The fatigue strength of riveted joints has been investigated principally by Schaechterle(215) and Graf(216); a few results on riveted joints have been included in the preceding section.

The fatigue strength of welds has unquestionably been more arduously investigated than the fatigue of rivets but it is the general concensus of opinion and results that the stress distribution in riveted joints is no less complex than in welds and that there is no great difference in fatigue value between the two methods of joining. The comparison between welded and riveted connections is also made in the section on service results (Bissell, Spindler, Bernhard, and others). Graf[216], Ayslinger[115], and others have shown that the decrease in origin fatigue limit of mild steel due to a central, transverse hole is about 20%.

The results of Graf[216] and Memmler, Bierett, and Gehler[9] place the pulsating tension fatigue strength of double-riveted joints in mild steel at 25,600 to 28,400 psi calculated on the weakened cross-section, or 21,400 to 22,800 psi on the original unpunched plate, assuming a hole cross-section of 20%. In 1919, Abell[40] found that in reversed bending as a simple beam, butt welds (60°V, flux-coated electrodes, no reverse run) had 70% of the fatigue value of unwelded; lap welds and riveted joints had only 60%. Lea[46] states that riveted and welded joints are equivalent in fatigue (no details given) but Roš[216] found gas welds superior in pulsating tension to riveted joints of about the same static strength. In Hertel's fatigue tests of spars (980 cpm) fabricated from corrugated Cr-Ni sheet (178,000 psi tensile strength) the spot welded spar had an origin fatigue strength of 27,000 psi, the riveted 21,900 psi. The ratio of origin fatigue strength to static tensile strength was:

Type of Spar	Ratio: $\frac{\text{pulsating tension fatigue strength}}{\text{static tensile "}}$
box spar; spot welded Cr-Ni	0.15
box spar; riveted Cr-Ni	0.125
steel framework spar, riveted	0.032-0.061
Dural framework spar, riveted	0.08
Spruce spar; glued joints	0.45 (based on compressive strength)

Fatigue failure of the riveted spars started at the rivet holes. The high ratio for glued spruce spars shows that gluing creates much less stress concentration than welding or riveting. Schlyter[218] found that the rotating bend fatigue strength of cold-glued Sitka spruce with the joint at right angles to the plane of deformation and at an inclination of 1:12 to the grain was the same as solid, unglued specimens. Aysslinger[115] states that the fatigue efficiency of double-riveted joints is 0.57 (static efficiency 0.70).

According to Graf[219], the fatigue strengths of welded and riveted joints do not differ greatly; the slightly higher permissible stresses for riveted construction in high strength steel is a result of the higher standard of workmanship that can be expected in riveted joints. Schuster[88] believes that perfectly sound weld metal in boiler welds would give better service when subjected to fluctuating internal pressure than the solid plate of a riveted boiler with thicker plates. Herold[220] considers that riveted joints have the advantage that they can "breathe", but, as Tracy[221] points out, this is not always an advantage. Hankins and Thorpe[20] state that there are indications that fatigue may be more important in welded than in riveted structures. Townshend[222] states that unmachined double V butt welds made with a high quality electrode have higher reversed-bend fatigue strength than riveted overlapped joints. The comparative fatigue tests on welded and riveted joints carried out by Lloyds in 1918, according to Thomson[223], showed the superiority of welding. On the basis of pulsator tests on welded beams and riveted beams, Witt[224] concludes that the welded beam is 50% superior in fatigue to riveted beams of the same static resisting moment.

BRIDGES

Experimental fatigue tests of welded bridges (as distinguished from traffic tests which have been satisfactory, or from fatigue tests of individual members) have been made by the German Railways (Bernhard[225] and Schaper[226]) and by Patton and co-workers[227]. These tests are fundamentally different from strain and vibration measurements provided by the Telemeter, Stereo-Comparator, and other devices (see Kulka[228], Report of Bridge Stress Committee, London, 1928, and others). A pulsator consisting of two rotating eccentrics (see the section on results of tests, where Gehler's system of fatigue testing is described, and the monograph of Späth[229]) puts the bridge in forced vibration. The maximum load developed in the bridge members is calculated from measurements of deflection. The eccentric pulsator may thus be used to apply cyclic stress to an entire bridge, as in the customary fatigue test. In addition, when the pulsating load acts with a frequency equal to one of the natural frequencies of the bridge, there is resonance and the power consumption of the pulsator motor is maximum. A decrease in the natural frequency during a fatigue test is an indication of loss of rigidity under load.

The first welded bridge to be tested by Bernhard in 1929 (welding details not given) had a span of 30 feet and failed after only 22,400 cycles (40 minutes) at max. tensile stress 22,700 psi, max. compressive stress -18,800 psi. The stresses were only +8,850, -4,950 psi, just before rupture. The natural frequency rose slightly in the early stages of the test indicating that the bridge actually became stiffer after repeated loading. Bernhard considered the results as unsatisfactory, but later tests on welded experimental bridges tested by eccentric pulsators at over one-half the maximum design stress were entirely satisfactory. Bernhard also showed that riveted bridges strengthened by welding were stiffened, the natural frequency being increased 3 to 7% in the loaded and unloaded states. Welding decreased the damping factor, that is, the range of frequencies at resonance, and decreased stresses and deflections due to traffic. Sahling[264] found that in a wrought iron bridge whose bending deformations had been decreased 6% by strengthening with mild steel welding, the lateral vibration of the superstructure was decreased 45%. Bohny[208] also observed definite stiffening during pulsator tests of a welded bridge.

A comparison of the fatigue behavior of a welded and a riveted bridge of the same dimensions (40 ft. span) was made by Patton, Bouchtedt, and Tchnoudnowsky[227], using an eccentric pulsator. The welded bridge failed after 215,000 cycles at a maximum load of 25 tons; the riveted failed after 250,000 cycles at the same load. The natural frequency of the welded bridge remained constant throughout the test except just before failure whereas the frequency of the riveted bridge, which was lower than that of the welded bridge at the start of the test, appeared to vary 10%. These variations were related to the warming of the riveted joints which occurred after only 30,000 cycles. The welded joints remained cool until just before failure, which occurred in the heat affected zone. Patton considered that the tests were not unfavorable to the welded bridge.

The successful traffic test of the first experimental all-welded Swiss railway bridge is described by Beguin[230] and others. The test bridge consisted of two panels of stringers, a joint of a truss, and a cross girder. The stringers were butt welded; the girders were butt-fillet welded together with flat webs and flanges. The bridge was placed in track with a specially wide rail joint over it, and withstood 1.5×10^6 cycles of load in five years, the welds showing no signs of failure.

VIBRATION

Although a few aspects of the damping and vibration characteristics of welds and welded structures have been discussed in the sections on Bridges and Repeated Impact, several investigators have studied vibrations in welds apart from the fatigue aspect. As early as 1925 W L Warner(265) made the keen observation that a vibration will be damped by a poor weld, which was later confirmed by damping tests of good welds made by Hallström(252). In these tests of cantilever bars (0.31" x 0.79") of mild steel with a vibrating length and amplitude of 38" and 1.18", respectively, the unwelded bar came to rest after 120 sec., the arc welded bar after 45 sec.

The advantages of welding in preventing vibration in machinery, as pointed out by Chapman(266), are connected with the higher modulus of elasticity of welded steel as compared with cast iron. The closed section, ideal for preventing vibrations, is easy to weld but difficult to cast. Krug(267) and Tweetside(268) also found that welded construction for machine tools fulfills the two requirements of lightness and rigidity essential for combating vibrations better than cast iron construction. Föppl(273), however, believes that welds do not develop any more damping than the base material and that, when riveting is replaced by welding, the welded steel should have high damping capacity.

The vibration experiments on welded lathe beds made by the Technical College, Berlin-Charlottenburg, jointly with the Siemens-Schuckert concern(269) confirm the experience of Chapman and others. These laboratories also made vibration tests on solid cast iron and steel I-beams as well as on welded I-beams. The results are shown in the table at the top of the next page. The welded beam(c) was made of flat plates; the welded beam (d) was made of ribbed flanges having a "nose" profile (welding details not given). The beams were placed on knife

I-Beams

	Unwelded		Welded	
	Cast Iron	Rolled Steel	(c)	(d)
Time to come to rest, free vibrations, sec.	0.50	0.36	0.32	0.22
Resonance amplitude, forced vibrations -	0.039mm	0.0265mm	0.026mm	0.0188mm
Natural frequency, free vibrations - - -	76 cycles/sec.	88.5	89.3	83.5
" " , forced vibrations - -	77.8 "	86	89.5	84
Disturbed length, % - - - - - - - - - - - -	31	22	29	21

edges 10 ft. apart and set to vibrate by means of a solenoid, 30 to 500 cycles/sec. The tests show that the welded beam (d) is most suitable, the cast iron beam least suitable from the vibration standpoint.

D M Warner(236) has described a machine to test the relative vibration fatigue characteristics of seam welded joints, spot welded baffles, and other products. No results are reported but it is stated that the test reveals grain growth in the junction zone of carelessly made gas welds. Helsby, Hamann, and Samuely (270) state that the inequality of stress in a weld in longitudinal shear is a great advantage in damping vibration.

The specification of the Department of Commerce in 1931 that spot welded ribs of airplane wings pass a 10 hour vibration test was successfully met by the manufacturers concerned.

TUBES

The fatigue value of welds in aircraft structural tubing has been investigated mainly by rotating bend tests on individual gas butt welds. There are lar e differences in fatigue strengths reported by the investigators for apparently similar material. For example, Miss Doussin[231], Hoffmann[77,112], Johnson[66], and Beissner[103] agree that gas welded plain carbon and Cr-Mo tubing has a fatigue value of 14,000 to 16,000 psi. Baumgärtel[111], however, found about 20,000 psi for Cr-Mo tubes using low-carbon or Cr-Mo fillers, Matthaes[232] 2[illegible],000 to 25,000 psi for plain carbon and 28,000 for Cr-Mo (no details), Sutton[233], 2[illegible],200 for heat treated Cr-Mo (iron or Cr-Mo filler), and Wegelius[129] 25,000-28,500 for plain carbon and 30,800 for Cr-Mo (filler not mentioned), all using unmachined specimens. The lowest of these values is higher than any given by R. R. Moore[60] in 1927. The effect of differences in welding technique, such as heat effect, grain size,and penetration, must therefore be considerable; for differences of any magnitude have not been found for variations in chemical compostion, machining, or internal stresses. Flash welds in Cr-Mo tubing gave 32,000 after stress annealing, according to Johnson[66], but gave low values (13,000) in plain carbon.

Reversed bend tests represent a less severe test than rotating bend. Müller[128] found, using the reversed bend method, that 0.11% C tubing gave 25,000 psi, 0.32% C, 29,000, and Cr-Mo 24,000 to 31,000 depending on heat treatment. For low carbon superheater tubing Ulrich[234] found about 15,000 for gas welds but less than 10,000 for arc welds. Internal water pressure (500 psi) with accompanying corrosive effect applied to the tubes during test had no appreciable effect on fatigue value. In pulsating tension Ulrich found 13,000 for gas welds in superheater tubing, and Matthaes[232] gives 40,000 for Cr-Mo tubing.

Recently, fatigue tests have been made in which service conditions are more nearly duplicated than in the rotating bend test. Ward[102], using the stationary cantilever type machine, found 25,000 psi for as-welded Cr-Mo tubing and 35,000 for heat treated; these values are, respectively, 1/4 and 1/3 the static tensile strength of the as-welded tube. Similar ratios for the rotating bend test vary from 20 to 50%. The ratio of fatigue strength welded to that unwelded is in the neighborhood of 60% for all types of tests. In alloy tubing (Cr-Mo and Cr-V), Füchsel[235] states that the ratio of welded to unwelded torsion fatigue strength is 65%. In general, as the carbon (0.25-0.40%C) or alloy content (Cr, Mo, or Mn) of the tube is raised the ratio of endurance limit to static tensile strength of the weld is lowered from 50% to 20%. This is shown by almost all investigators.

The other type of fatigue test for welded tubing that simulates service conditions is the vibration test. This test as described by Warner[236] is only comparative. The welded tube or structure is submitted to simple translational vibrations of known amplitude. A vibration test of welded tubes carried out by an English tube works and quoted by Roosenschoon[116] showed that such variables as structural welding stresses, as distinct from local shrinkage stresses, have a considerable effect on fatigue resistance, especially with Cr-Mo tubing.

The effect of chemical composition, whether of tubing or filler rod is not great. The difference in fatigue value of gas welds in plain carbon tubes of 0.1% and 0.3% C is not over 15%. Nor is the difference between plain-carbon and Cr-Mo tubing of similar carbon content over 10 to 15% and the scatter seems to be the same for both. The important consideration is evidently that the tubing be as insensitive as possible to the welding heat and this seems to be attained by using a low (0.25 - 0.30%C) rather than a high carbon content (0.30 - 0.35%C) in the Cr-Mo steel. Apart from differences in weldability, the composition of the filler rod was not important in these tests. Beissner(103) found that an alloy (Cr-Mo) filler rod was better for thick tubes (0.12") but that plain carbon was superior for thin tubes (0.06"). He also found that there is an optimum breadth of reinforcement for each size of tube. The medium-manganese (0.3C, 1.5Mn) tube has been found inferior in fatigue. Air hardening has practically no effect on fatigue cracking in Cr-Mo welds.

Lap and fish-mouth joints appear to be at least as good as butt joints, according to Miss Doussin and Johnson, but brazed, soldered, and bell-and-socket joints are definitely inferior, as Miss Doussin and Hoffmann have shown. Pinned and riveted joints have only 50 to 80% of the strength of welds. If the factor of ease of welding is not considered there is no size effect, at least up to tubes 3" in diameter.

The three most important causes for fatigue failure in welded tubes are:

(1) Poor penetration and inclusions; a service fatigue failure in superheater tubing owing to poor penetration has been described by Pfleiderer(70);

(2) Heat effect; if poor penetration has been overcome, the micro-structural changes due to torch heat, which extends for several inches on either side of the weld in tubing, is of prime importance, as Ward's and Beissner's micrographs show. A coarse Widmannstätten structure is stated to be particularly dangerous.

(3) Shrinkage stresses; although especially important in completed structures, Johnson showed that a stress anneal at 950°F increased the endurance limit of flash welded Cr-Mo joints by 30%.

It is surprising that the removal of reinforcement has not had beneficial effects on the fatigue resistance of tube welds. Moore found no difference between machined and unmachined welds, and Johnson found a slight effect (5% increase in fatigue value) only in tubes below 3/4" diameter. However, Hoffmann and Ward both recommend that reinforcement be low, and the welds concave and smooth in important joints. If distortion and decarburization can be prevented, heat treatment improves the fatigue strength of welds in Cr-Mo tubing up to 40%.

APPENDIX A

METHODS OF TESTING WELDS IN FATIGUE

Most of the investigators have used the rotating-bend type with two-point (Farmer type) or one-point (Föppl) loading. The design of welded specimen for this type of machine has been discussed by Thornton[92] and Zimmerman,[237] and Weinman[19] showed that the deflection of a welded specimen in a Farmer machine remains constant throughout the duration of the test until very shortly before fracture, if the stress exceeds the endurance limit.

The direct stress type has also been popular. Machines of this type apply direct tension and compression; the cycle of loading may consist, for example, of: (Fig 18) (a) alternating stress, $\pm S_{max}$; (b) pulsating tension, $+S_{max}/0$ (c) alternating with superimposed static tension $+S_{max}/-S_{min}$ (d) pulsating with superimposed static tension $+S_{max}/+S_{min}$. Endurance limits are quoted as $S_{o(S_m)}$; $S_m = \frac{1}{2} S_o$ represents the pulsating tension fatigue strength; $S_m = 0$ represents the customary alternating stress endurance limit. Such load cycles are provided by machines of the magnetic (Haigh) and hydraulic types (Amsler Pulsators up to 200 tons capacity). Objection to the former type has been made by Orr[238], who points out that eccentricity of loading is possible with welds. The accuracy of the Amsler pulsator has been investigated by Schick[10] who found 12 to 28% errors at 120 to 460 cpm (cycles per minute). Graf[239] also found considerable differences with riveted joints between 1, 10 and 350 cpm, the result at 10 cpm being on the safe side. Using the Schenck high-frequency tension-compression machine Laute[141] found no appreciable reduction in fatigue value of welds at 30,000 cpm. The Wazau pulsator[94] provides a stress cycle with short rest periods at the peaks in order to eliminate the inertia offects said to exist in the usual oil-pressure pulsators. Another type of pulsating stress machine particularly adapted to the testing of large welded specimens is the vibrating bridge[9]. The specimen acts as a tension member in a framework truss of 50 ft span loaded to any desired extent by dead weight or by smooth cycle pulsating load. The pulsating load is provided by two rotating eccentrics.

Three other types of fatigue tests that have been used for welds are reversed (back-and-forth) bend, rotating-spring cantilever, and reversed torsion. Of the numerous reversed-bend machines the following may be mentioned: the Schenck machine that can be adapted to 90° and T welds, the Föppl-Heydekampf machine modified by Friedmann[79] to test welded wire, the admirably simple machine devised by Orr[71], and Dörnen's low-frequency tester[90]. An excellent description of the calibration of a reversed-bend machine for welds is given by Thum and Lipp[15]. The design of specimens for the cantilever machine is discussed by Jennings[39], who, among others[240], clarifies the question of small versus large specimens. Fatigue specimens smaller than about 1/4" diameter should not generally be used for welds, and, as pointed out in the "Design" section, fatigue limits from specimens of any size should never be used directly as design stresses. Peterson[241] found that the same endurance limit was obtained with welds 0.3" diameter as 1" diameter. Lea and Parker[21] found that fatigue limits on machined specimens of 70°V welds or all-weld-metal deposited by coated electrodes were practically identical on reversed-bend and rotating-bend machines. Results from the Haigh direct stress machine were 30 to 50% lower, however, the explanation appearing to be that the Haigh machine develops maximum stress concentration around all flaws in the cross-section of the specimen whereas only the surface flaws are subjected to maximum stress in the reversed- or rotating-bend tests.

Special fatigue machines for welds or welded structures have been devised by Colinet[242] (a 4-ton repeated bend machine at 15 cpm, span 16 feet, with or without shock), Warner[236] (vibration tester for welded tanks), Wilson[76] (motor-driven eccentric for column connections) and others. A reversed-bend fatigue test is a routine test which all welders on the North-Western Railway, India[243], must pass every month. Henderson[271] states that the welders assigned to welding railway freight and passenger cars on a British railway must prepare specimens once a month to pass an alternating stress test (no details). Bright[244], Thom[245], and Green[246] describe shop fatigue tests for welds.

Short-cycle (short-time) methods for determining the fatigue limit of welds are generally unconvincing. The method of Stanton and Pannell[32], whose comparison-curve method was the first to be applied to welds (1911) has recently been adopted again[71]. Bartels[38] has shown that the Lehr power-output method, and the deflection and rise-in-temperature methods, to a limited extent, give rather close approximations of the endurance limit of welds.

A method of presenting fatigue results on welds has recently been proposed by Dutilleul[2], who observed in rotating cantilever tests that the different degrees of porosity in different specimens cut from the same welded joint gave rise to different fatigue values. Thus of three specimens cut from the same weld and tested at 21,000 psi, two fractured before 10×10^6 cycles. Nevertheless, three other specimens from the same weld and tested at 24,200 psi withstood 10×10^6 cycles without fracture. Dutilleul therefore defines an endurance coefficient C showing the scatter in results.

$$C = \frac{n_1F_1 + n_2F_2 + \text{------}}{N_1F_1 + N_2F_2 + \text{------}}$$

where F_1F_2 - - - are the fatigue stresses, n_1 is the number of specimens not broken at F_1, etc., and N_1 is the total number tested at F_1, etc. He states that the coefficient is important only when F_1N_1, F_2N_2 etc. are close together, and shows that the coefficient is reasonably reproducible for a given electrode.

APPENDIX B

TABLES OF RESULTS OF FATIGUE TESTS

Note: The details of materials and testing contained in the tables are as complete as possible. Where type of electrode, weld, or welding process is not given, it indicates that the original article does not contain the information.

RESULTS OF TESTS

Butt Welds

The numerical results of fatigue tests on butt welds in mild steel are collected in Tables 1 to 4. These are self-explanatory, available details of test conditions being given as concisely as possible. It would be presumptuous to compute an average value of endurance ratio or fatigue strength from these tables (see section on ethods of / Design). Yet it appears that there is remarkably little difference in the ranges of fatigue strength reported for direct-stress, and reversed-, and rotating-bending. There have been numerous estimates of the necessary cycles criterion for welds in fatigue, Haigh(68) giving 2×10^6 cycles for dense welds and 5×10^6 cycles for defective welds in direct stress. It seems quite certain the Wöhler curves for welds in steel do not generally become horizontal in any type of stress at 2×10^6 cycles. Thum and Lipp(15) have found that 100×10^6 cycles is an absolute criterion for welds in reversed bending. The numerous factors that affect fatigue value, noted in the tables, are discussed in detail in other sections of the review. It is unfortunate that standard welding rods are not available so that the results of these elaborate fatigue investigations will not become obsolete with the wires themselves.

Fillet Welds

Selected results of fatigue tests on fillet welds are given in Table 5. All investigators used mild steel; none of the specimens was machined. The tests of Memmler, Bierett, and Gehler[9] (since endurance limits were not determined, these tests are not reproduced) were performed on two pulsating bridges (240 cpm), and a Losenhausen pulsator (360 to 660 cpm). The arc welds were made with bare electrodes. Throat dimensions were: Series II and IV = 0.280"; series V and XIII = 0.224". Stresses are computed on the plate cross-section. Graf[6] used Amsler and Losenhausen pulsators. He quotes the origin fatigue strength as the stress at which the Wöhler curve intersects the 2×10^6 abscissa. His tests were generally made with bare electrodes, which he found equivalent in fatigue value to coated electrodes, in agreement with Schick, Bierett, and Roš and Eichinger.

Roš and Eichinger[5,7] used a 50-ton Amsler pulsator at 300 cpm. Their criterion is 10^6 cycles. Their fatigue stresses are computed in the following way:

Normal-shear fillet weld = Load/2b h

Parallel " " " = Load/2b s

where b = breadth of cover plate (not including weld);

s and h = height of deposit with respect to cover plate.

Bare and coated electrodes were used.

Hankins and Thorpe[20] tested their specimens in a 6-ton electromagnetic (Haigh) machine at 2300 cpm. Class A structural steel and high-class covered electrodes were used. The fatigue stress is computed on the throat area. The criterion of fatigue limit was 25×10^6 cycles. The fatigue strengths were only about 1/4 the static strength of the specimens, which is in general agreement with Graf. Schick[10] used bare electrodes (0.08C, 0.5 Mn, 0.01 Si; 3/16" diam), and tested his specimens in a specially-calibrated Amsler pulsator.

According to the latest German results (Kommerell[143]), fillet welds are equivalent in fatigue strength to butt welds (no details are given). It should be noted that Schick's specimen 26 was equivalent in fatigue value to butt welds at 2 x 10^6 cycles.

Schulz and Buchholtz[18], and Fry[63] also report a few results of pulsator tests with fillet welds. Butt fillet welds were tested by Jennings[89] in the cantilever machine. Nikolaev[261] tested butt and fillet welds in mild steel in alternating tension and compression, but his tests extended only to about 100,000 cycles. The specimens acted as members in a 30-ft. bridge span equipped with an eccentric oscillator (20 cpm).

T Joints

Selected results of fatigue tests on fillet welds in mild steel are given in Table 6. All results, unless otherwise noted, are for unmachined mild steel. The welded specimens tested by Thum and Lipp[15] were prepared by a commercial firm using mild steel (0.1C, 0.4Mn) and bare electrodes. The cast steel contained 0.12C, 0.31 Si, 0.88 Mn, 0.066 P, 0.041 S; the gray cast iron analyzed 3.05 C, 2.24 Si, 0.84 Mn, 0.13 P, 0.12 S; both were horizontally cast. Specially-calibrated reversed-bend machines were used (1,000 cpm for small specimens; 2,000-3,500 cpm for large). The leg of the welded T was not tapered. As shown in Fig 19 the Wöhler curve of the welded T was not horizontal at 100 x 10^6 cycles. Fig 20 is a partial Goodman diagram of some of the results. The factor B_k given in the table is the notch-sensitivity fatigue factor, which includes all fatigue-lowering factors (notches, surface irregularities, and shape-factor). Of all the materials tested the weld had the largest notch-sensitivity factor. The shape factor cannot be altered by using stronger steels for welding but can be favorably affected by using more ductile electrodes. The large specimens showed the same trend as the small but had lower fatigue limits. Machining the junction between surface of weld and surface of leg raised the fatigue value by 15%.

Sulzer[65] tested his specimens in a special reversed-bend machine fitted with a flywheel to promote smooth operation similar to Müller's device[262]. The specimens were arc welded (details not given), and the criterion of fatigue limit was 10 x 10^6 cycles. The M.A.N. magnetic reversed-bend fatigue machine was used by Jünger[43] for his arc-welded specimens (details not given). The stress was computed on the cross-section of the leg. The specimens of Roš and Eichinger[5,7], and Memmler, Bierett, and Gehler[9] were tested in pulsating direct stress (see preceding section). Gerritsen also used a pulsator, according to Schoenmaker[132] and Thierens[131], who report the results, the criterion being 2 x 10^6 cycles (leg and base of T were 3/8" and 1" thick, respectively; kind of steel not stated). Roberts[16] used a cantilever reversed-bend machine utilizing magnetic impulses synchronized with the natural frequency of the T. His specimens were welded with a bare electrode.

It is difficult to arrive at a basic value for the fatigue limit of fillet welds or T Joints, especially in view of the large effects of machining, shape, and workmanship. At present, the published results indicate that both types of welds are inferior to butt welds.

TABLES OF RESULTS OF FATIGUE TESTS

Note: The details of materials and testing contained in the tables are as complete as possible. Where type of electrode, weld or welding process is not given, it indicates that the original article does not contain the information.

Footnotes

St.w = tensile strength of weld
St.unw. = tensile strength of plate

1. Ratio: Sf. w/ St. unw. = 0.29
2. " " " " " = 0.50
3. " Sf.w/ St. w. = 0.34
4. Cantilever machine
5. Ratio: Sf.w./St.w. = 0.34; Sf.w./St. unw. = 0.32
6. " " = 0.35; " " = 0.34
7. " " = 0.33
8. " " = 0.40
9. " " = 0.66; Sf.w./St.unw. = 0.52
10. Cantilever machine
11. " " Sf.w./St.w. = 0.4
12. " "
13. Ratio: Sf.w./St.unw. = 0.28
14. " " = 0.27
15. " " = 0.13 to 0.41

Table 1.

Direct Stress Fatigue Results on Unmachined Specimens (Unless otherwise noted)

Reference and Date	Plate Material	Type of Weld	Fatigue Limit psi Plate Sf. unw.	Fatigue Limit psi Weld Sf. w	Endurance Ratio Sf.w/Sf.unw.
Scott(247)-1934	mild steel-bare	60°X	± 34,700	± 17,700	0.51
"	" " high class covered	"	"	20,200	0.58
Küchler(248)-1934	ship steel-bare	V		15,600	
"	" " heavy coat	V		19,600	
"	" " high quality coated	V		25,600-29,800	
	low-alloy				
"	structural steel heavy coat	V		19,500	
	" high quality cored	V		27,700	
Aysslinger(115)-1932	coated	V	29,900	24,200	0.81
Hankins & Thorpe(20)-1934	Structural steel coated	X unmachined	34,700	17,500	0.51(1)
"	" " "	X machined	34,700	30,200	0.87(2)
Wallmann(31)-1934	mild steel carbon arc shielded	small blow holes	35,500	21,400	0.60
"	" " " "	large " "	35,500	19,900	0.56
"	" " " "	poor penetration	"	14,200	0.40
Fry(63)-1934	mild steel 0.1 C bare	V	25,600 ± 11,400	25,600 ± 6,400	0.56
Roš & Eichinger(7)-1935	mild steel gas or arc	V or X	17,100 to 19,900		
Schulz and Buchholtz(18)-1932	mild steel arc bare or coated	X	27,000	17,100	0.63
Haigh(68)1935	mild steel arc (many cavities)	V		21,300	
Hankins(48)-1935	structural steel bare	X unmachined		<13,400	
"	" " "	V "		11,200	
"	" " "	X machined		10,100	
"	" " "	V "		9,000	
Graf(6)-1931-1935	mild steel bare & coated arc, gas	root welded V 60° and 70°	39,800	25,600 unmach. 34,100 mach.	0.64 0.86
Thum & Schick(249)-1933	mild Steel bare	V and X		25,600±5,700X 25,600±6,400V	

Table 2 Reversed Bend Fatigue Results

On Machined Specimens (unless otherwise noted)

Reference and Date	Plate Material	Welding Process or Rod	Type of Weld	Fatigue Limit psi: Plate Sf. unw.	Fatigue Limit psi: Weld Sf. w	Endurance Ratio Sf.w/Sf.unw
Joellenbeck and Massmann(160) 1931	Boiler plate about 0.05C	arc	-	28,500	20,600-22,800	0.7-0.8
Jünger(43) - 1931	Boiler plate		V	21,300 mill scale	19,900	0.94
"	" "		V	26,700 Mill scale	24,900	0.93
Lohmann and Schulz(34)-1934	Mild Steel	bare	X unmach.	32,700	18,500	0.57
" "	Mild Steel	coated	X unmach.	32,700	17,000 unannealed 27,000 annealed	0.52 0.83
" "	Low-alloy structural st.	bare	X unmach.	39,800	22,300	0.57
" "	Low-alloy structural st.	coated	X unmach.	39,800	21,400 unannealed	0.54
Orr(71) - 1935	Mild steel	arc	U	26,900	21,300	0.79(3)
Thierens(131) - 1935	Mild steel	arc	V	25,300	25,400	1.0
"	Low-alloy steel	arc	V	38,600	27,400	0.71
Roš & Eichinger(7) - 1935	Mild steel	gas or arc	V		28,500 0 to max ± 19,900	
Lea and Parker(21)-1936	Mild steel	coated	V unmach.		± 19,700	
" "	Mild steel	coated	V mach.		± 24,000	
Kirkcaldy(250)-1929	Mild steel		mach.		19,500	

Table 3 Rotating Bend Fatigue Results

On Machined Specimens (unless otherwise noted)

Reference and Date	Plate Material	Welding Process or Rod	Type of Weld	Fatigue Limit psi Plate Sf. unw.	Fatigue Limit psi Weld Sf. w	Endurance Ratio Sf.w/ Sf.unw.
Pester & Schulz(41)-1932	Mild Steel	gas	X	24,600	18,500	0.75
" "	" "	gas peened	X	24,600	23,600	0.96
Greger(251)-1933	Mild Steel	low C gas	V	33,400	26,300	0.79(4)
Musatti & Reggiori(69) 1934	Mild Steel 0.2C	Coated	X	41,000	27,600	0.67(5)
" "	Mild Steel	coated low C	all-weld-metal	41,000	29,000	0.71(6)
Hankins & Thorpe(20) 1934	Str. steel	coated	all-weld-metal	± 26,800	± 18,400	0.69
Abell(40)-1919	Mild steel	fluxed	X	± 23,500	14,600	0.62
Blackwood(62)-1933	Mild steel	coated	90°V		19,000	(7)
"	" "	bare	"		20,000	(8)
Bartels(38)-1930	" "	0.04 C gas	V	39,800	34,100	0.86(9)
Thierens(131)-1935	" "	arc	V	28,600	28,600	1.0
"	Low-alloy str.st.	arc	V	46,000	38,000	0.83
Gerritsen(75)-1933	Mild and low-alloy st.	"	V		27,200-30,600	
Roš & Eichinger(7)-1935	Mild steel	gas or arc	V		18,500-19,900	
Hallström(252)-1933	Mild steel 0.20C	arc	X	31,300	25,600	0.82
"	" " "	gas	X	31,300	24,200	0.77
Brown(154)-1935					18,000	(10)
Hodge(61)-1935					16,000-18,000 porous 30,000 sound	
Lincoln(253)-1935					10,000-12,000 bare 30,000 shielded	
Roš & Eichinger(254) 1935	Basic Bessemer	arc			16,500-18,400	
" "	Open Hearth	"			18,000-21,600	
Sulzer(65)-1933	Boiler Plate	"	all-weld-metal		22,400 annealed 14,600 unannealed	
Dutilleul(2)-1936	no details	arc	V 7 passes 3/16" electrodes	31,300-33,400	Abt 18,500	(11)
Kjellberg(255)	Mild steel 11/16" plate	arc	coated electrodes		25,600	
Lincoln Elec.Co(256) 1935	Mild steel	bare or washed electrodes	all-weld-metal	28,000	12,000-15,000	0.50
" "	" "	shielded arc	all-weld-metal	28,000	28,000-30,000	1.0
Yarko & Abramova(257) 1935	Mild steel 0.21C	chalk coated	V	-	20,000	
" "	" "	other	V	-	12,000-22,800	0.28-0.50
" "	" "	bare coated	V	-	18,500	
" "	" "	gas	V	-	18,500	

Table 3 (Continued)

ROTATING BEND FATIGUE RESULTS

On Machined Specimens (unless otherwise noted)

Reference and Date	Plate Material	Welding Process or Rod	Type of Weld	Fatigue Limit psi Plate Sf. unw.	Fatigue Limit psi Weld Sf. w.	Endurance Ratio Sf.w/ Sf.unw.
Jennings(134)-1933	Hot-rolled st.	bare	V	27,000	16,000	0.59
"	"	fluxed	V	27,000	25,000	0.93
Peterson & Jennings(73)-1931	Mild steel	bare	Mach. all-weld metal		9-13,000	(12)
" "	Mild steel	bare	Unmach. all-weld "		12-16,000	(12)
" "	" "	fluxed	" " " "		31,000	(12)
Jennings(95)-1934	" "	bare	30° V		19,180	
Zimmerman(237)-1934	" "	heavy coated	V	34,000	20,600	0.61(13)
"	" "	gas	V	"	19,700	0.58(14)
Thornton(92)-1934	15-20 boiler plate	gas	V low C rod	32,500	14,900	0.46
"	" " " "	"	X low C rod	32,500	10,000	0.31
"	" " " "	low carbon bare	V	32,500	20,000	0.62
"	" " " "	" " "	X	32,500	13,000	0.40
"	" " " "	covered	V	32,500	27,000	0.83
Jennings(89)-1930 (also Lobo - 1929)	H R S	bare low C	0°	27,000	16,000	0.59
"	"	" " "	30°V	27,000	21,000	0.78
"	"	" " "	45°V	27,000	20,100	0.74
"	"	" " "	30°X	27,000	16,200	0.60
"	"	" " "	45°X	27,000	17,800	0.66
Moore & Kommers(253)1927						(15)
Muller(259)-1936		bare or dipped		16,000-18,000		
"		heavy coated		26,000-32,000		
Lohmann & Schulz(34)1934	Mild and low-alloy str.st.	bare	X	17,000-18,500		
" "	" " " " "	covered	X	25,600-27,000		

Table 4 Torsion Fatigue Results on Machined Specimens

Reference and Date	Plate Material	Welding Process or Rod	Type of Weld	Fatigue Limit psi Plate Sf. unw	Fatigue Limit psi Weld Sf. w.	Endurance Ratio Sf.w/Sf.unw
Garre & Gerbes(260)-1931	Mild steel	Gas .06C	60°X	22,400	20,400	0.91
" "	" "	At. H_2 low C	"	"	19,200	0.86
" "	" "	Flash	Annealed 15 min.at 800°C	"	19,500	0.87
" "	0.35-0.40%C	Gas	60°X	37,700	19,200	0.51
" "	" "	At H_2 low C	"	"	29,400	0.78
" "	" "	Flash	Annealed 15 min.at 800°C	"	26,600	0.71
Rosenberg(29)-1934	Mild steel	Flash			~ 22,800	1.0
Thierens(131)-1935	" "	Arc	V	16,400	16,400	1.0
"	Low-alloy structural st.	Arc	V	23,800	22,600	0.95
Roš & Eichinger(5,7)-1935	Mild steel	Gas, arc	V		25,600 0 to max ± 15,700	

Table 5. Results of Fatigue Test on Fillet Welds

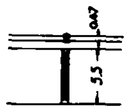
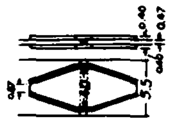
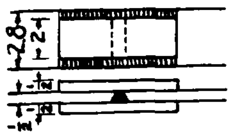
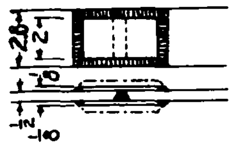
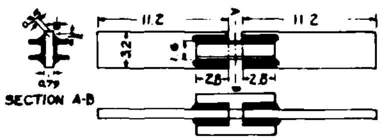
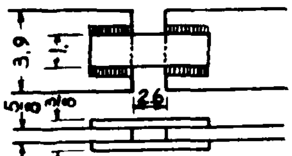
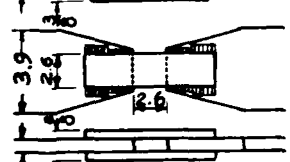
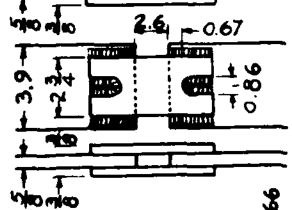
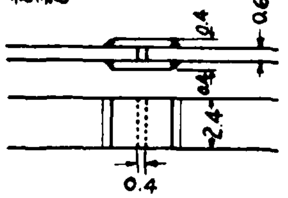
	Type of Joint	Lower Stress psi S_u	Upper Stress psi S_o	Cycles to Fracture $\times 10^6$	Fatigue Strength S_f
	O Graf[6] 1931-1935				
	Arc 27				18,500
	Arc 28				14,200
	Gas 31				18,500
	Gas 32				24,200
	Gas 33				25,600
	Arc 44 (L=1 3/16")				12,800
	Arc 44 (L=8")				15,700
	Arc 40				14,200
	Arc 41				12,800
	Arc 42				10,000
	Arc 46				10,000
	Gas 46				15,700

Table 5 (continued) Results of Fatigue Tests on Fillet Welds

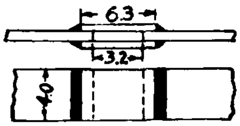
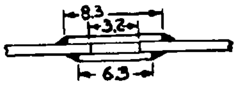
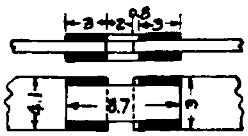
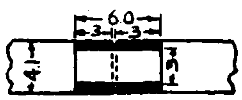
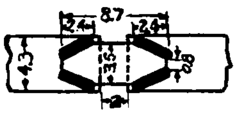
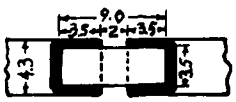
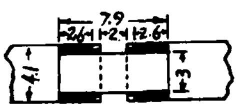
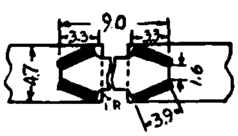
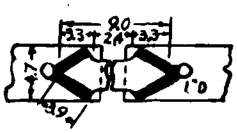
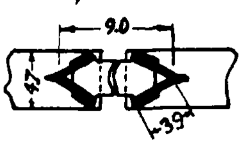
	Type of Joint	Lower Stress psi Su	Upper Stress psi So	Cycles to Fracture x 10^6
		W. Schick,[8] 1934		
	13	15,200	22,800	1.0
	14	15,200	22,800	0.7
	16	17,100	25,600	1.0
	18	17,100	25,600	0.3
	20	17,100	25,600	2.0
	21	20,400	30,600	0.5
	23	20,400	30,600	0.6
	26	21,800	32,700	2.0
	27 I	17,800	33,400	2.0
	27 IV	17,800	32,300	2.0

Table 5. (continued) Results of Fatigue Tests on Fillet Welds

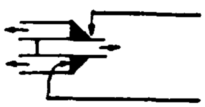
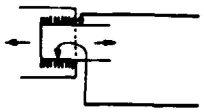
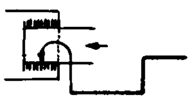
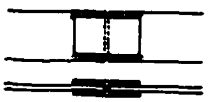
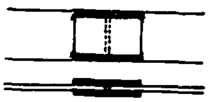
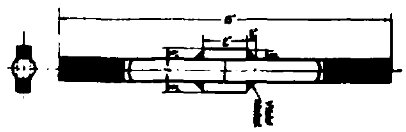
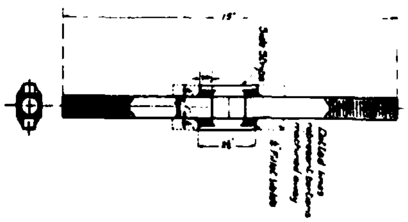
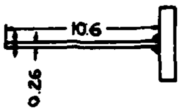
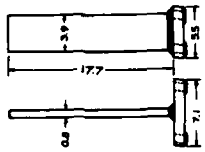
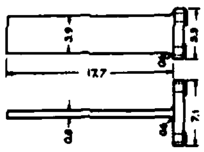
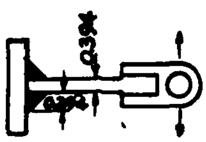
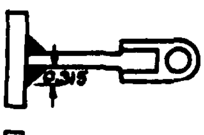
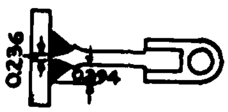
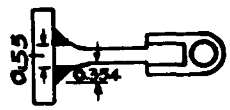
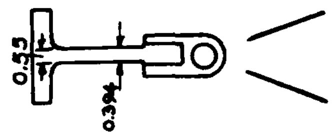
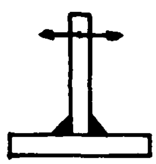
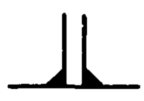
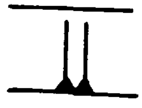
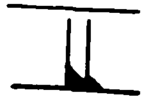
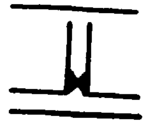
Type of Joint		Lower Stress psi Su	Upper Stress psi So	Cycles to Fracture x 10^6	Fatigue Strength psi S_f
	M. Roš & A. Eichinger[57] (1936)				
	**Normal Shear tension**	**0**		**>1**	**12,800-15,700**
	Normal Shear tension	**0**		**"**	**10,000-11,200**
	**Normal Shear compression**		**0**	**"**	**10,000**
	**Parallel Shear tension**	**0**		**"**	**12,800-15,700**
	Parallel Shear tension	**0**		**"**	**12,800**
	**Parallel Shear compression**		**0**	**"**	**12,800**
	G Bierett,[263] 1933				
	A	11,000	22,000	0.46	
	B	11,100	21,900	0.51	
	C	11,500	23,000	2.10	
	A Hankins and P L Thorpe,[20] 1934				
	2 Normal Shear				15,700
	4 Parallel Shear				13,400

Table 6 Results of Fatigue Test on T Joints

	Type of Joint	Lower Stress Su	Upper Stress So	Cycles to Fracture x 10^6	Fatigue Strength S_f
	A. Thum and T. Lipp,[15] 1934 (Reversed Bend)				
	Small welded				12,800
	Large welded			B_k=1.95-2.31	15,700
	Cast steel large			B_k=1.20-1.61	27,000
	Cast iron large			B_k=1.16-1.21	12,800
	Forged large			B_k= 1.58	17,100
	R. Sulzer,[65] 1933 (Reversed Bend)				
	1				23,500
	2				21,400
	3				22,800

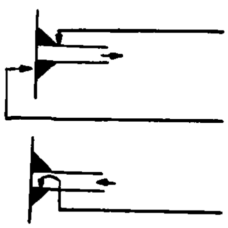
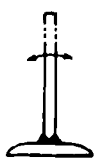
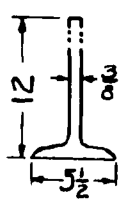
$$B_k = \frac{\text{Fatigue Limit of Polished Parent Metal in Rotating Bend}}{\text{Fatigue Limit of Shape}}$$

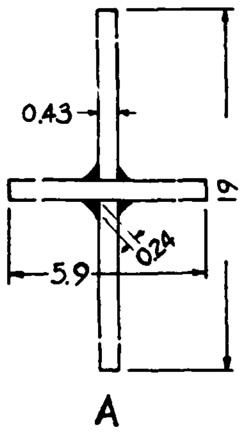
Table 6 (continued) Results of Fatigue Test on T Joints

	Type of Joint	Su	So	Cycles to Fracture x 10^6	Fatigue Strength S_f
	R. Sulzer,[63] 1933 (Reversed Bend)				
	4				28,600
	5 Steel Casting				42,800
	8 Special Cast Iron				24,200
	A. Jünger,[43] 1930 (Reversed Bend)				
	Steel 1				21,800
	Steel 2				26,800
	P Schoenmaker,[132] 1936; E J F Thierens[131] (Gerritsen), 1935				
	1	12,400	23,100	P_B = 76,000	
	2	16,400	30,500	P_B = 77,000	
	3	15,100	28,000	P_B = 66,000	
	4	17,900	33,300	P_B = 71,500	
	5	18,900	25,100	P_B = 76,500	

P_B = static rupture load - lb

Table 6 (continued) Results of Fatigue Test on T Joints

	Type of Joint	Su	So	Cycles to Fracture x 10^6	Fatigue Strength psi S_f
	M. Roš & A. Eichinger[5] (1936)				
	**Tension**	**0**		**> 1**	**12,800-15,700**
	Tension	**0**		"	**8,600-10,000**
	Compression		**0**	"	**12,800**
	A M Roberts[16] 1935 (Reversed Bend)				
	**1**				**±17,900**
	**2**				**±20,200**
	**3 unwelded**				**±24,600**



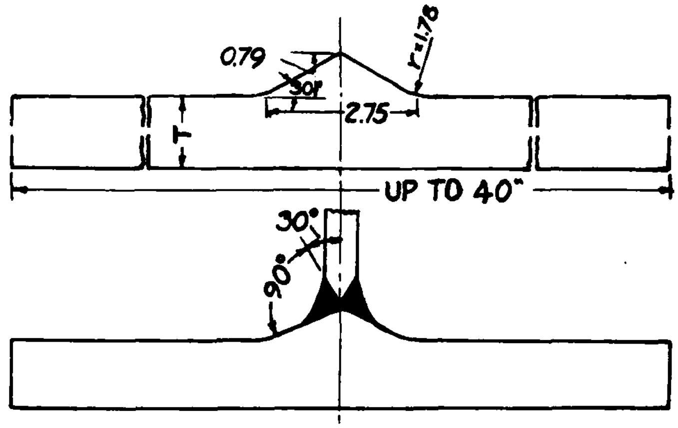


Fig. 1 Arc Welded Double T Joints

Strength-psi	A	B	C
Tensile Strength	68,500	80,500	83,000
Pulsating Tension Fatigue Limit. 2×10^6 Cycles	13,500	15,700	21,400

Low-Alloy Structural Steel
Special Electrode (no details)
Graf.
See page 7.

Fig. 2 Ribbed Flange for Welded Parallel Flange Beams; a type commonly produced in Germany. The tip of the rib is rounded off. Weight of rib: 3.7 lb/ft. T up to 3.55".
Bürger.
See page 8.

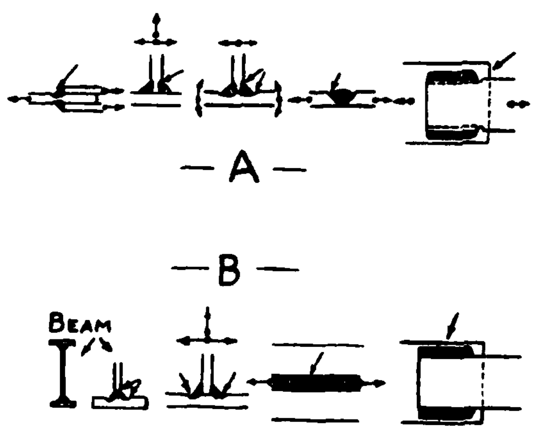


Fig. 3 Lowering of Fatigue Value by Undercutting. Undercut notches are dangerous if they cut the lines of stress at a large angle
A - Dangerous Undercut.
B - Harmless or Less Dangerous Undercut.
Bierett.
See page 12.

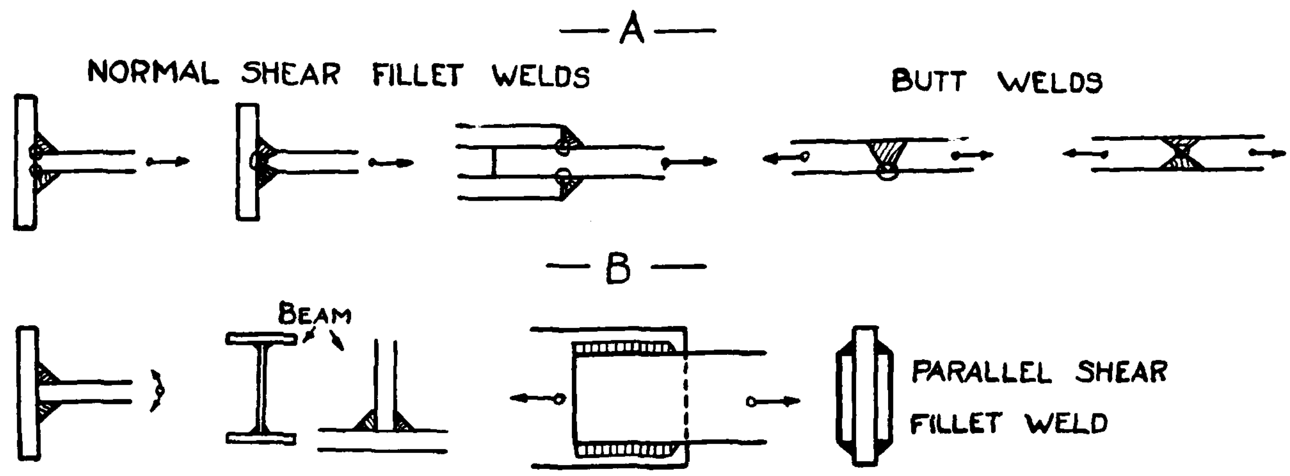


Fig. 4 Lowering of Fatigue Value by Poor Penetration
A - Joints Highly Sensitive to Poor Penetration
B - Joints Insensitive or Less Sensitive to Poor Penetration
Bierett.
See page 16.

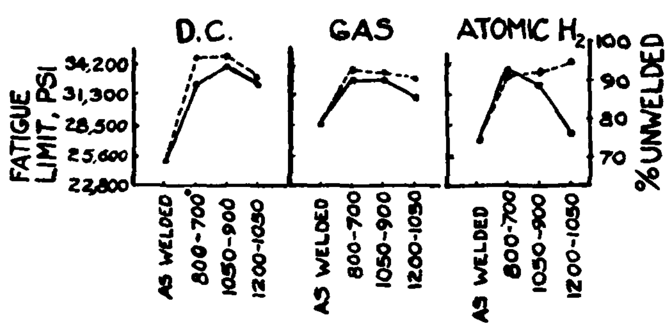


Fig. 5 Effect of Temperature of Forging and Per Cent Reduction on Rotating Bend Fatigue Limit. Becker
Full Line - 20% Reduction in Forging
Dotted Line - 40% " " "

See page 18.

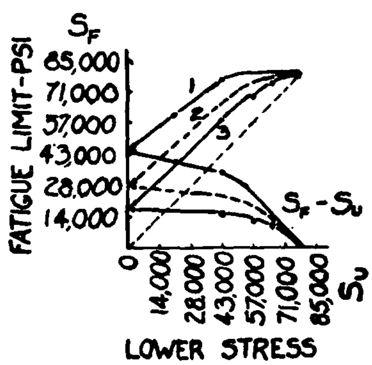


Fig. 6 Pulsating Tension Fatigue Limits (S_f) for Low-Alloy Structural Steel.
Curves 1 - Unwelded
" 2 - Transverse Butt Welds (Arc) Machined Flush
" 3 - Unmachined
Plates 0.4"-0.6" thick
Bierett.(12)
See page 20.

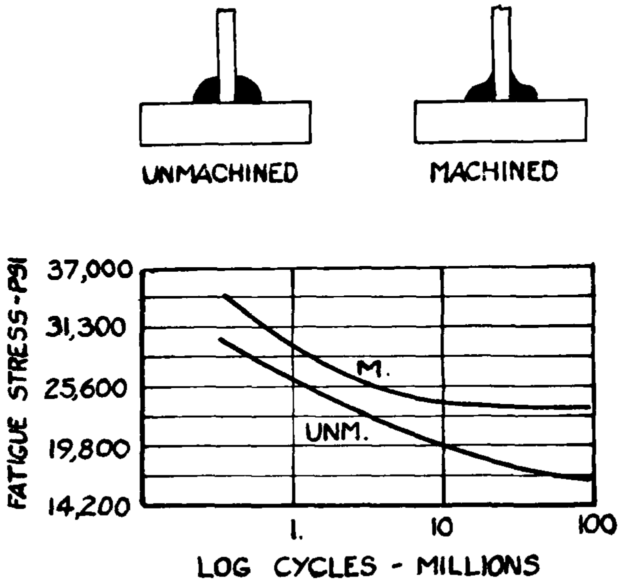


Fig 7 Wöhler Curves for Unmachined (UNM.) and Machined (M.) T's in Reversed Bending. Bare Electrodes. Thum & Lipp.
See page 21.

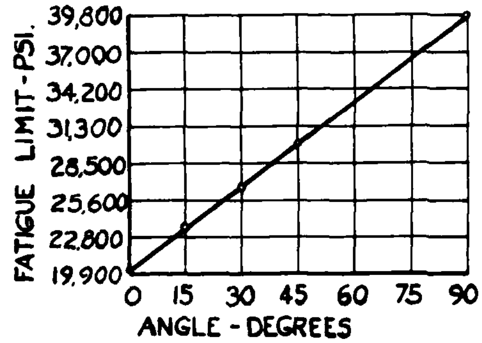


Fig 8 Relation Between Pulsating Tension Fatigue Limit and Angle Between Machined Butt Weld and the Normal to the Tensile Force.
Wazau Hydraulic Pulsator;
Coated Electrode;
Low-Alloy Structural Steel Cu-Mo.
Diepschlag, Matting, & Oldenburg.
See page 26.

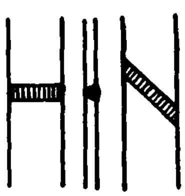


Fig 9 Pulsating Tension Fatigue Limit, Gas Welds in Mild Steel.
Transverse Butt
Root Welded-25,600 psi
not " " -17,100 "
45° Butt Root Welded-31,200 psi
not " " -24,200 "
$2\frac{3}{4}$" wide; $\frac{1}{2}$" thick
Graf
See page 26

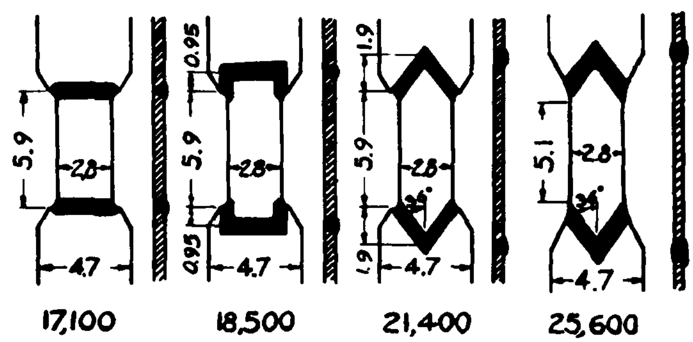


Fig 10 Pulsating Tension Fatigue Limit (psi) Gas Welds in $\frac{1}{2}$" Mild Steel.
Graf.
See page 26.

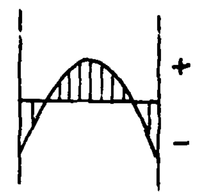


Fig 11 Shrinkage Stresses in Butt Weld.
Bierett
39.
See page 39.

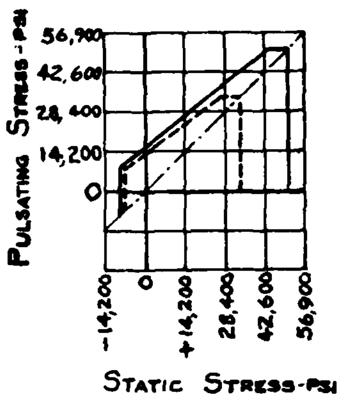


Fig. 12. Direct Tension Fatigue Strength of Arc and Gas Butt Welds, Unmachined
Full Line - Low-Alloy Structural Steel (Tensile Strength=74,000 psi)
Dashed Line - Mile Steel (Tensile Strength= 53,000 psi)
(K. Schaechterle)
See page 43.

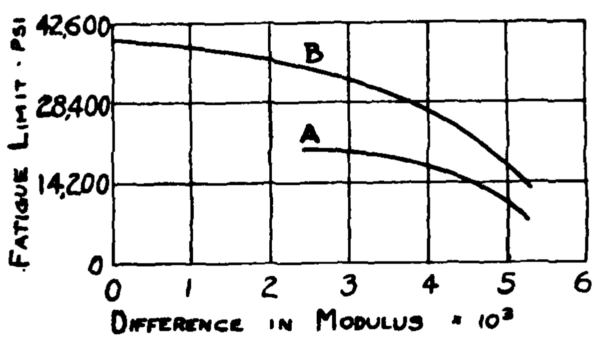


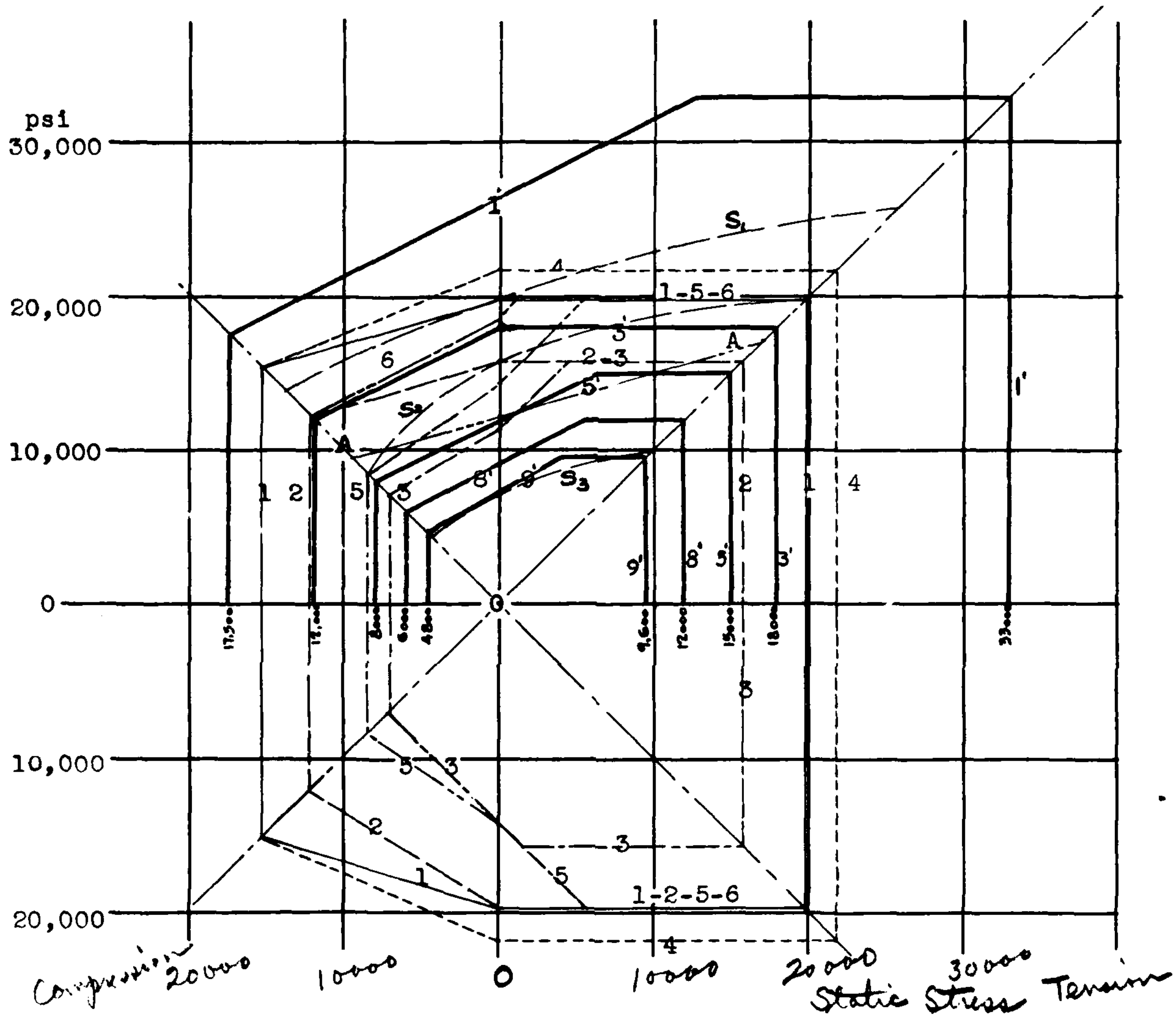
Fig. 13 Relation Between Pulsating Tension Fatigue Limit (2×10^6 Pulsations) and the Difference in Inverse Modulus of Elasticity (Expressed as $(1/E) \times 10^9$ $KG^{-1}MM^2$) Between Weld Metal and Parent Steel
Curve A - Cruciform Specimens
" B - Butt Welds
(Diepschlag, Matting, & Oldenburg)

See page 46.

Fig. 14

SUMMARY OF PERMISSIBLE DESIGN STRESSES IN FATIGUE FOR WELDS IN MILD STEEL

ACCORDING TO NATIONAL SPECIFICATIONS



American Welding Society
1'-Approx. Experimental Fatigue Strength of Structural Steel
3'-Formula 3, Art. 203 not Fillet
5'- " 5 " " Fillet
8'- " 8 " 204 Butt
9'- " 9 " " Fillet

Austrian Standard - A

Swiss Federal (B_2 Lines)

S_1= Butt Welds, Compression
S_2= " " Tension
S_3= Fillet "

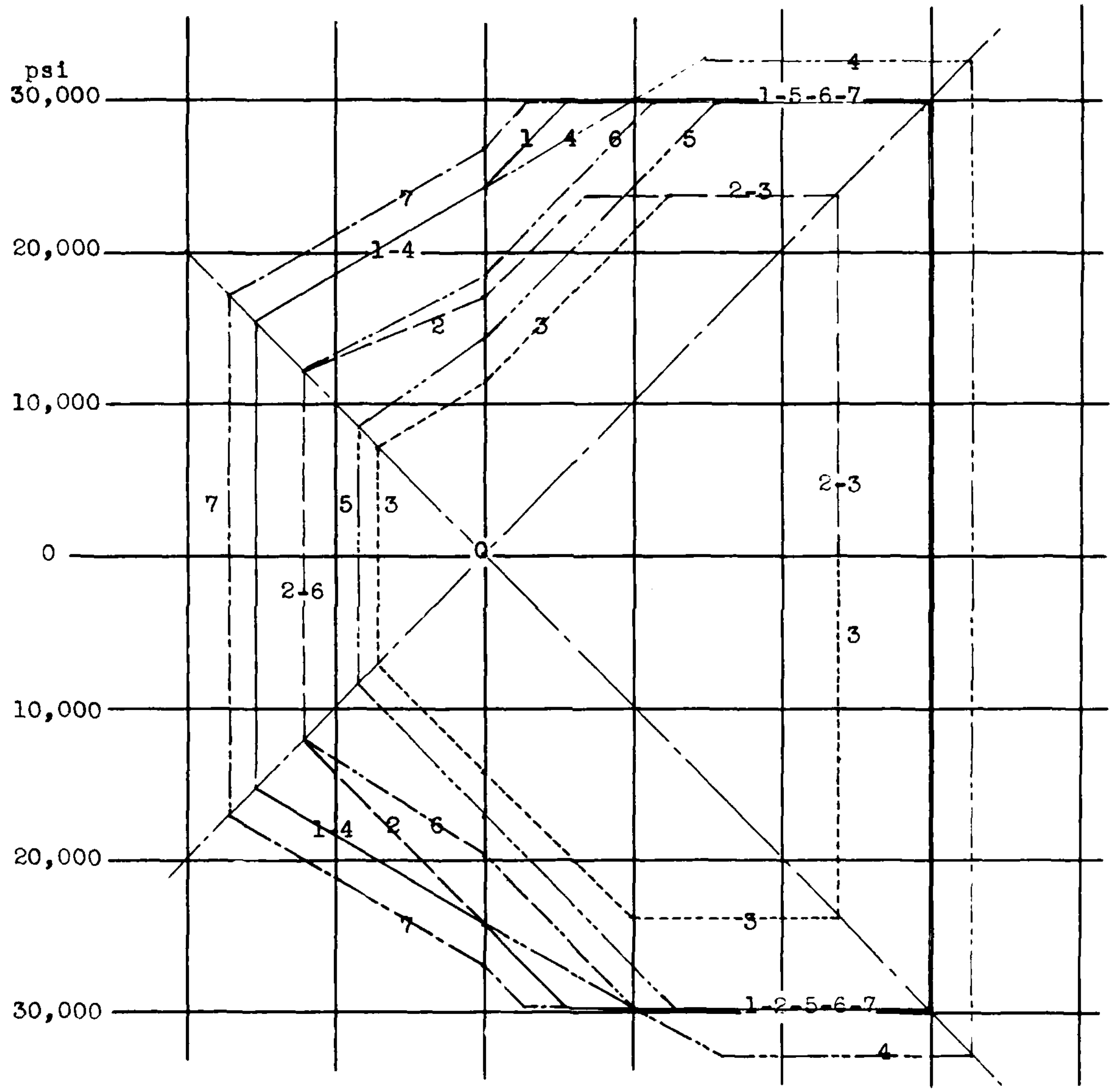
German Railways
1- Base Material Unwelded
2- Butt welds, reverse welded and machined
3- Same as 2, not reverse welded
4- Permissible principal stress given by $S = \frac{S_1}{2} + \frac{1}{2}\left(S_1^2 + 4T_1^2\right)^{\frac{1}{2}}$
5- Normal-and parallel-shear fillet welds unmachined
6- Same as 5, ends of fillets machined

See page 57.

Fig. 15

Permissible Design Stresses in Fatigue for Welds in Low-Alloy Structural Steel
(74,000 psi minimum tensile strength)

According to Specifications of German Railways for Plate Girder Railway Bridges



1-Base Material, Unwelded, Heavy Traffic
2-Butt welds, reverse welded and machined
3-Same as 2, not reverse welded
4-Permissible principal stress given by:

$$S = \frac{S_1}{2} + \frac{1}{2}\left(S_1^2 + 4T_1^2\right)^{\frac{1}{2}}$$

5-Normal-and parallel-shear fillet welds, unmachined
6-Same as 5, ends of fillets machined
7-Base Material, Unwelded, Light Traffic

See page 57.

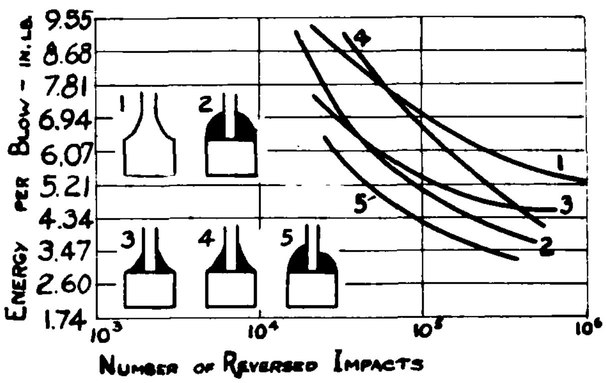


Fig. 16 Wöhler Curves in Reversed Bend Impact of Welded and Unwelded Mild Steel T's
Specimen 1 Machined Mild Steel
" 2 Bare Electrode Weld
" 3 " " " Concave Contour Produced By Filing.
" 4 Covered Electrode Weld
" 5 Bare Electrode Weld, Unequal Height of Weld.
(Thum & Lipp)

See page 64.

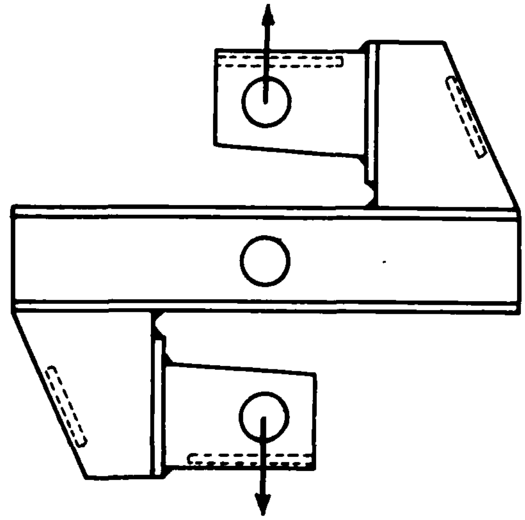


Fig. 17 Welded Connection Tested in Fatigue by Kater

See page 78.

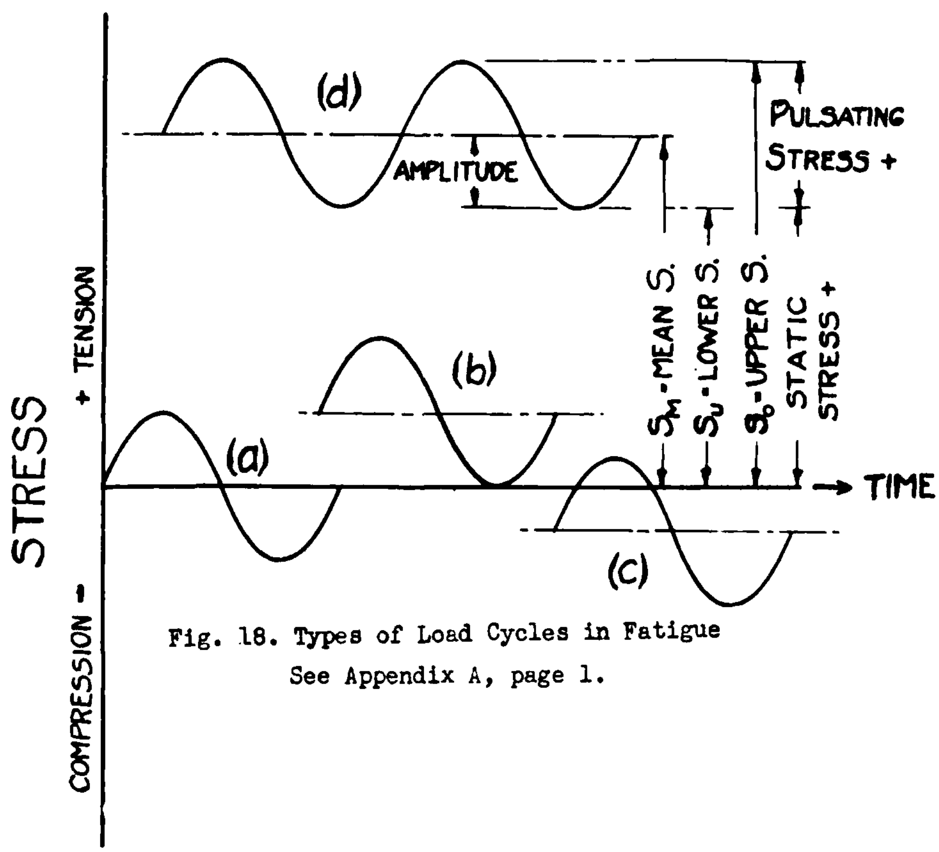


Fig. 18. Types of Load Cycles in Fatigue

See Appendix A, page 1.

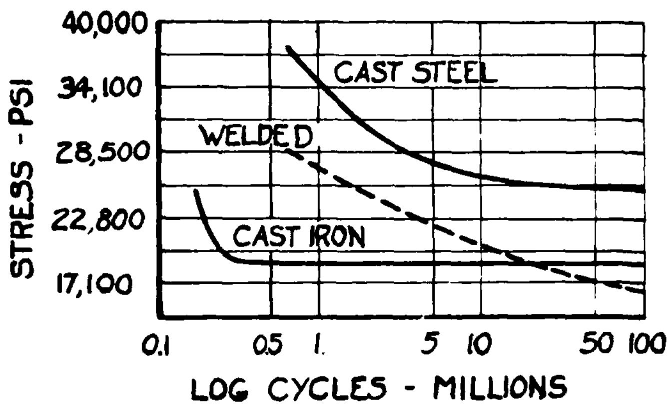


Fig. 19, Wöhler Curves in Alternating Bend for T's (Preliminary Tests Welded T was made with Bare Electrode, Unmachined) Thum & Lipp

See Appendix B, page 3.

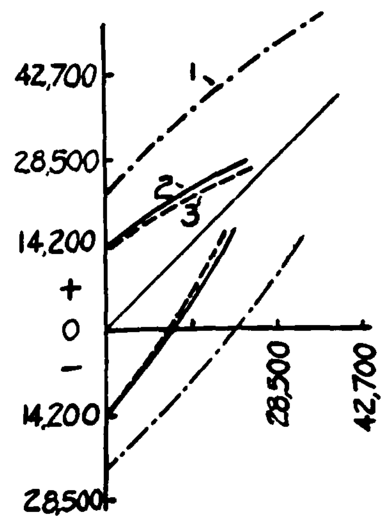


Fig. 20 Fatigue Limits in Bending of T's
1- Cast Steel
2- Welded Mild Steel Unmachined Convex Fillet, Bare Electrode
3- Cast Iron, no defects.
Thum & Lipp.

See Appendix B, page 3.

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The complete bibliography with abstracts upon which the review is based is available in the files of the Fundamental Research Committee.

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