BEHAVIOR OF RIVETED CONNECTIONS
IN TRUSS-TYPE MEMBERS

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SYNOPSIS

The tests reported herein were performed to provide information on the general behavior of large truss-type riveted steel connections. The variables of the test program included specimen configuration, method of hole preparation, and size of rivets. A study is made of the comparative behavior of the specimens, the distribution of load to the gusset plates, the strains in the lacing bars, the effect of hole preparation, and the predicted and computed efficiencies of the connections.

INTRODUCTION

Research on riveted joints has been conducted for more than a century; nevertheless, many of the problems investigated have never been completely answered. The emphasis of past research has been

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on flat plate joints, probably reflecting largely the past, but declining, importance of riveted joints in vessels, tanks, and boilers. In the last half century, the use of long-span bridges and the construction of many tall buildings have brought large built-up members to new importance. Yet, a review of the literature (1)*** yields little data on tension tests of full-size truss-type members, although a small number of studies have been made on gusset plates, columns, and some few related structural components such as angles, lug angles, tie plates, etc. Generally, these latter tests were limited in scope and involved but few specimens. Since 1945, occasional tests of large tension members have been made; however, they included only several specimens of similar size and shape, and often were limited to or principally concerned with the behavior of the recently developed high-strength bolts.

Flat plate joints are sometimes referred to as "single plane members." That is, members in which the loads on the fasteners are applied in one plane; or, in the case of double lap joints, the loads on the fasteners are applied in two planes separated only by one or more thicknesses of material, a distance which is usually small relative to the width of the joint. In contrast to single plane members, we may describe many large truss members in general use today as "double plane members." That is, members in which the loads

*** Numbers in parentheses indicate reference number in the Bibliography.
are applied through gussets in two planes separated by a distance which is often equal to or exceeds the width of the joint.

The purpose of this investigation was to examine the behavior, up to failure, of full-size truss-type members subjected to static tensile loads. Since the specimens were tested in duplicate, the sixteen specimens tested represent eight variations. These variations include five distinct specimen patterns for which the rivet holes were drilled plus three of these same specimen patterns for which the rivet holes were punched.

**DESCRIPTION OF SPECIMENS**

Fabrication and Description of Specimens

The material for these specimens was ordered in accordance with ASTM Designation A7. The gusset plates were cut from hot rolled sheared plates 40" x 1/2", the web plates and battens (or tie plates) were cut from universal mill plates 16" x 1/2" and the lacing bars were sheared from 1/4" plate. The angle stock consisted of 3 1/2" x 3 1/2" x 7/16" material in 22'-6" lengths, 5" x 3" x 3/8" angles 34'-0" long, and 5" x 5" x 3/8" angles, 30'-0" long.

All material was carefully identified and cut in the shops of the University of Illinois. The batten plates and/or web plates for a given specimen came from the same piece of plate; similarly, without exception, all four angles for any given specimen were cut from a single length. Coupons were taken from approximately the
mid-length of each piece of stock which would comprise part of the
critical section of the specimens. In general, the plate material
was flame-cut to final dimensions; the angles were generally saw-
cut to length.

One of the principal variables of these tests was the
method of hole preparation. In order to reduce to a minimum the
variations resulting from fabrication, all pieces for the drilled
specimens were match drilled and fitted up completely in the
University shop.

Punched specimens were fabricated in the following manner.
The plates were laid out in the University shop and center punched.
These were then punched full size at the shops of the fabricator,
using a conventional punching machine. The angles for the punched
specimens, having been cut at the University, were set up and
carefully punched on a standard spacing table at the fabricator's
shop. Since these angles had been laid out earlier by the
University shop, the stops or settings of the spacing table were
checked in a "dry-run" before actual punching began. The use of
these procedures resulted in uniform spacing and constant gage
distances. In driving the more than 1500 rivets, only nine holes
required reaming. However, reaming did not appear to reduce the
strength of the specimens involved since the failures did not occur
at those joints in which the rivet holes had been reamed.

All rivets were from the stock of the fabricator, and of
ASIM Al4l (3) designation with cold formed heads. The length required for the rivets was determined by the rivet gang foreman in the usual shop fashion. New kegs of rivets were opened and used for these specimens and four sample rivets of each diameter and length were set aside for laboratory testing. The 3/4-in. rivets in Specimens AD1 and AD2 were all hand driven. The 7/8 in. rivets for the tie plates and lacing bars of Specimens DD1 and DD2 were also hand driven. Without exception, all other rivets were machine driven in both the punched and the drilled specimens.

There were five basic specimen types, designated alphabetically A through E. These basic types were designed to give as great a range of predicted efficiencies as was possible with the usual specification requirements of gage distances, (except for the type D specimens), edge distances, spacing, etc. A marking system was used which identified each piece of material by specimen type, method of hole preparation, specimen number, and final location in a specimen. This permitted each piece to be followed from the original length of stock to the assembled specimen. All drilled specimens are identified by a "D" following the specimen type designation; the punched specimens are designated by a "P". Since each specimen type was tested in duplicate, the first and second specimens are designated 1 and 2, respectively. Thus, BD1 signifies the number one specimen of type B, prepared by drilling, and DP2, the second punched specimen of type D. Details of the
specimens may be obtained from Figs. 1 through 5 and Table 1.

**Mechanical Properties of Materials**

Coupons from the materials were machined to a 1 1/2 in. width and to a standard 8 in. gage length. Both surfaces of coupons from the junction of the legs of an angle were machined to provide parallel surfaces. All other coupons were tested with the flat surfaces in the "as-rolled" condition. Every coupon was carefully marked to identify its original position and its related specimen. Since all angles of a given size were from the same heat, coupons were taken from the toe, center, and fillet positions of each leg of one length of stock. Only two coupons, one from the center of each leg were taken from other lengths of angles. Two to five coupons were tested from each of the various pieces of plate stock.

The average mechanical properties of the coupons are listed in Table 2; the chemical composition and mechanical properties from mill reports for the angles and the plate material are listed in Table 3.

It will be noted from Table 2 that most of the material for these specimens met the requirements of ASTM A7, although some of the plate material ran as low as 58,000 psi ultimate, or about 3 per cent lower than the 60,000 psi required. All coupons met the minimum yield requirement of 33,000 psi, and also the requirements for elongation.
The information obtained from the coupons provided a means of checking the actual dimensions of the truss-type specimens also. By taking the thicknesses of the unmachined coupons and averaging them for a given member, it was possible to compute the areas of those members for comparison with the areas obtained from the AISC handbook. On this basis, it was found that the measured areas of the specimens tended to be about 99 per cent of the handbook areas. Individual specimens were as low as 97.5 per cent, and as high as 100.3 per cent, as shown in Table 1. Such a range is within the ± 2.5 per cent allowed by ASTM and AISC specifications. The measured area was used only in the calculation of test efficiencies; all other references to area will be to the handbook or nominal areas.

The four samples of each rivet length-diameter combination were tested in shear and tension. The results of the tests are shown in Table 4. The average shear strength of the 3/4-in. rivets exceeded that of the 7/8-in. rivets by almost 10 per cent. The ultimate tensile strengths of these rivets were generally higher than those specified by ASTM A41-39; however, that specification governs the properties of the "as-rolled" bars and not the manufactured rivets which were tested.

Instrumentation and Equipment

All sixteen specimens had similar instrumentation, involving mechanical dials, electric strain gages, and a qualitative visual indicator of the extent of yielding.
The mechanical dials had 0.001 in. divisions and a range of 1 in., and were used in the following ways: (1) to indicate the overall deformation of the specimen and joint; (2) to measure the relative movement of the gusset plates and angles at the critical sections or first rows of rivets in the joints; and (3) to indicate the relative movement of the angles and gussets at the last row of rivets in the joints. Mechanical dial locations can be seen in Figs. 1 through 5.

The electric strain gages were SR-4 (type A-1, 13/16 in. long) wire resistance gages. These were placed on the various specimens as shown in Figs. 1 through 5, and were used to give comparative strains in the angles, lacing bars, and web plates of the members. To determine the load distribution to the members, three pairs of gages were placed on each of the four pull plates (plates to which the gussets were welded). The gages, one on either side of the pull plate, gave average strains at the gage locations and were used to evaluate the magnitude of the load in the gusset plates. Strain gage locations were chosen with the intent of obtaining the most representative measurements with the least number of gages.

The procedure of whitewashing the specimens provided a simple means of indicating where yield patterns (shear lines or Lüder's bands) formed in the specimens. The whitewash spalled off the surfaces of the specimens with the mill scale as yielding took place.
DESCRIPTION OF TESTS

The specimens were tested in the 3,000,000-lb. Southwark-Emery Tatnall testing machine (Fig. 6) in Talbot Laboratory at the University of Illinois. The gusset plates of all specimens were welded to the pull plates of the test fixtures with fillet welds. A consistent welding procedure was followed for all specimens to minimize the secondary effects of the welding and to keep them the same from one specimen to the next. When it was observed that most of the first eight specimens tested broke on the east side, it was decided to rotate the next group of eight duplicate specimens 180 degrees. However, the welding sequence was not changed. In spite of this change in orientation, most of the second group of specimens also failed on the east side possibly because of a peculiarity of the testing machine. It appeared that the sequence of welding did not affect the behavior of the members. After a test, the pull plates were flame cut just beyond the welds, thus removing approximately 1 1/2 in. from the pull plates for each specimen tested.

The specimens were tested to ultimate load in about fifteen load increments. The strain gages and deformation dials were read immediately after each load increment had been applied but while the load was maintained constant.
Each test will now be described briefly. In this paper, a "row of rivets" signifies those rivets in a direction perpendicular to the axis of loading, and a "line of rivets" refers to rivets parallel to the axis of loading. Unless otherwise specified, the order of the rivet rows refers to the member itself; i.e., the first row of rivets in a joint is the one at the net section of the member, or it is the first row of rivets nearest the mid-length of the specimen. Similarly, the last row of rivets is that farthest from the mid-length of the specimen.

Type A Specimens

Because of the unexpected failures of these two specimens, their tests are described rather fully. The specimen details are shown in Fig. 1; the cross section may be described as a double channel or box section.

Specimen ADL. During the test of ADL, the flaking of whitewash of the lower east gusset around and below the last row rivets, at 400,000 lb., indicated the initiation of yielding in the member. At 700,000 lb. (25,500 psi on gross area), the first Lüder's lines appeared inside the east web at the first row rivets, and at 1,155,000 lb. the entire lower east joint failed suddenly in shear. This maximum load is equivalent to 57,100 psi on the net section, 42,000 psi on the gross section, and a nominal average rivet shear of 37,300 psi.
The sheared rivets were then removed from the lower joint and the connection was bolted with high strength (ASTM A325(7)) bolts at a torque of about 370 ft-lb. When the bolts were installed, it was noticed that the east gusset had necked down considerably at the center of the last row of holes.

With the lower joints bolted and the upper joints still fastened with the original rivets, a second test was conducted. Failure began by the tearing of the lower east gusset followed by a similar rupture of the lower west gusset. Both tears then propagated simultaneously until the east gusset had torn to an edge. Prior to failure, five bolts on the west side and two on the east side sheared as shown in Figs. 7 and 8. The exact order in which these bolts failed is not known; however, the approximate order is marked in the figures. The maximum load was 1,235,000 lb. (61,000 psi on net section and 44,900 psi on gross area), thus, the rivets in the upper joint withstood a nominal average shearing stress of 39,900 psi without failing.

**Specimen AD2.** Both lower gussets of Specimen AD2 showed signs of yielding at 400,000 lb. as the whitewash spalled off around the last rows of rivets. Under a load of 500,000 lb. (18,200 psi on gross section), Lüder's bands developed on the east web at the first row rivets, and 100,000 lb. later, both web plates had yielded at the net section.

At a maximum load of 1,190,000 lb. (58,800 psi on net area,
43,300 psi on gross section, and nominal average shear of 38,500 psi), the outer lines of rivets in the lower east gusset sheared suddenly and the load dropped to 400,000 lb. Since the gusset was still attached to the web by the three inner lines of rivets, the member continued to carry load until, at 940,000 lb., the east web tore at the first row rivets as shown in Fig. 9. An indication of the extent of yielding of the lower west gusset at failure can be seen in Fig. 10. Note that the relative movement of web and gusset amounted to about 1/2 in. This figure also shows the areas of strain concentration in the gusset and the high load transfer which took place in the first row rivets and the outer lines of rivets. This was typical of both A specimens.

Type B Specimens

The B type specimens, being of a "laced-I" configuration (of which there were also two other groups of more-or-less the same pattern), may be thought of as representative of all the laced specimens. The first specimen to be tested in the entire program was BD2, and in this test, extensive photographs were made of the progress of yielding as depicted by the Lüder's bands in the white-wash. Because such yielding was typical of specimen types B, D, and E and because of the more thorough pictoral coverage, the test of Specimen BD2 will be discussed somewhat more fully than the tests of the similar members. Details of the type B specimens are shown in Fig. 2.
Specimen BD2. The formation of Lüder's lines on Specimen BD2 is recorded pictorially in Figs. 11 through 14.

As the load reached 350,000 lb. (30,600 psi on gross section), Lüder's lines appeared in the angles at the first row rivets. When the load was raised to 390,000 lb., the whitewash began to spall off the outstanding (5 in.) legs of the angles at a lacing rivet as shown in Fig. 11. (Position of Lüder's lines are noted by arrows). At 410,000 lb., the yield bands were pronounced on the outstanding legs of the angles opposite all the lacing rivets as illustrated in Fig. 12. At 475,000 lb., it was noted that the heel of the angles (see arrow in Fig. 13) had pulled away from the gusset plates 1/8 in. to 1/4 in. This behavior was typical of that noted in the tests of all the laced-I and solid-I specimens. At 500,000 lb. (58,000 psi on net section, 43,700 psi on gross section and nominal average shear stress of 34,700 psi), the specimen failed on the east side at the top lacing rivet as shown in Fig. 14. The toes of the inside legs of the west angles also ruptured at the center lacing rivet, producing, so-to-speak, a secondary failure. This secondary failure was at a point of high localized stress produced by the lacing bars, which bent the angles or "pinched them in" in the manner similar to that shown in Fig. 15 for Specimen BPl. In addition, it was noted that a "necking down" had occurred at the net section of the connection as well as at the other lacing rivets. This, too, was characteristic of all the laced specimens.
Specimen BDI. Yield patterns appeared at the first row rivets of the east angles of BDI when a load of 250,000 lb. (21,900 psi on gross area) was reached. At a maximum load of 498,000 lb. (57,800 psi on net section, 43,500 psi on gross section, and a nominal average shear stress of 34,500 psi), the toes of the east angle started to tear at the center lacing rivet. As the load slowly dropped, fractures appeared through the toes of the 3-in. legs of the west angles also. In the meantime, the fractures in the east angles had spread through the 3-in. legs and across the 5-in. legs. The primary fracture can be seen in Fig. 16; the secondary fracture was similar to that shown in Fig. 15.

Specimen BPI. At a load of 300,000 lb. (26,200 psi on gross section), the first Lüder's bands on Specimen BPI were apparent on the inside of the east angles at the first row rivets. The specimen reached a maximum load of 462,000 lb. (53,600 psi on net section, 40,400 psi on gross area, and a nominal average shear stress of 32,000 psi), at which time failure occurred at the toe of an east angle at the center lacing rivet, followed by a tearing of the toe of a west angle at the top lacing rivet. Although tears were apparent in all the angles, final fracture occurred in the west angles at the top lacing rivet in a manner similar to that shown in Fig. 14. The secondary failure in the east angles may be seen in Fig. 15. Even though the east side of this specimen was more highly strained initially and failure was initiated on the east side, the principal failure occurred on the west side of the specimen.
**Specimen BP2.** The west angles of BP2 began to yield at the first row rivets at about 325,000 lb. (28,400 psi on gross section). By 410,000 lb., the east angles exhibited Lüder's lines originating at the lacing rivets, and at 425,000 lb. the west angles showed similar yielding. When a maximum load of 458,000 lb. (53,100 psi on net section, 40,000 psi on gross section and a nominal average shear stress of 31,700 psi) was reached, the east angles failed as shown in Fig. 17. Although of the same general character and at approximately the same ultimate load as the other B specimens, the failure was unusual in that the southeast angle ruptured at the lower lacing rivet and the northeast angle ruptured at the upper lacing rivet. At the center lacing rivet in the west angles, the toes ruptured in a secondary failure similar to that shown in Fig. 15.

**Type C Specimens**

The C specimens were of the "solid-I" type and are illustrated in Fig. 5. Both these specimens failed at the net sections and in a like manner.

**Specimen CDI.** The lower east gusset of Specimen CDI began to yield at the last row rivets at a load of 300,000 lb. However, it was not until the load had reached 550,000 lb. (28,300 psi on gross section) that the east angles gave an indication of yielding at the net section. About 50,000 lb. later, the east edge of the
web developed Lüder's lines at the first row rivets, and at 650,000 lb. the web and inner legs of the east angles showed yield bands in the whitewash at the stitch rivets. The specimen failed at a maximum load of 872,000 lb. (55,800 psi on net section, 44,900 psi on gross area and a nominal average shear stress of 36,300 psi). Final failure occurred at the net section at the lower east side as shown in Fig. 18. It is interesting to note the necking down of the angles at each stitch rivet (see arrow in Fig. 18). In addition, a similar yielding was noted in the web at each of the stitch rivets.

**Specimen CD2.** At a load of 300,000 lb., Lüder's lines developed on the lower gusset plates of Specimen CD2 at the last row of rivets, and by 500,000 lb. (25,700 psi on gross section), the outstanding legs of the angles had yielded at the first three rows of rivets. At 650,000 lb., first signs were noticed of the spalling of whitewash on the web. The maximum load of 902,000 lb. (57,700 psi on net section, 46,400 psi on gross section and a nominal average shear stress of 37,500 psi) produced the primary failure at the top west net section in a manner similar to that shown in Fig. 18. A secondary failure occurred at the toes of the east angles at the lower net section.

**Type D Specimens**

The type D specimens were of the "laced-I" design shown
in Fig. 4. These members had the same size angles as the type B specimens; however, the section at the lacing rivets tended to be less important as points of stress concentration, because the D specimens were prepared with a smaller net area.

Specimen DB1. At a load of 250,000 lb. on Specimen DB1 (21,900 psi on gross section), Lüder's lines appeared at the first row rivets of the southeast angle. When the load reached 410,000 lb., yield bands became evident on the angles at the lacing rivets. The maximum load for failure was 450,000 lb. (62,500 psi on net section, 39,300 psi on gross section and a nominal average shear stress of 37,400 psi) with the primary failure occurring at the top west and a secondary (or partial) failure occurring in the net section at the lower east side of the member.

At the point of primary failure, an unusual break occurred: the rupture of the southwest angle passed through the two rivet holes in the outstanding leg and then to the second rivet at the batten plate but did not tear completely through the angle; instead, the toe of the inner leg of the angle tore at the first rivet. This may be seen in Fig. 19. The northwest angle, however, tore through the two rivets in the outstanding leg and through the first batten rivet as shown in Fig. 20.

Specimen DD2. The angles of Specimen DD2 showed first Lüder's lines or yield bands at the toe near the first row rivets when the load reached about 300,000 lb. (26,200 psi on gross area).
When the load was raised to 400,000 lb., the east angles developed Lüder's bands at the lacing rivets. A maximum load of 444,000 lb. (61,700 psi on net section, 38,800 psi on gross area, and a nominal average shear stress of 36,900 psi) was reached. At this load, the east angles failed through the lower net section in a manner similar to that shown in Fig. 20.

**Specimen DP1.** The lower east gusset of Specimen DP1 began showing Lüder's bands or yield lines in the whitewash at 275,000 lb. When the load was raised to 300,000 lb. (26,200 psi on gross section), the outstanding legs of the east angles developed yield patterns at the net section. The maximum load was 439,000 lb. (61,000 psi on net section, 38,400 psi on gross section and a nominal average shear stress of 36,500 psi) and rupture occurred at the lower east joint in a manner similar to that shown in Fig. 19.

**Specimen DP2.** By 300,000 lb. (26,200 psi on gross section), plastic flow of the angles beneath the first row rivets was apparent in Specimen DP2. Lüder's lines developed in the angles at the lacing rivets at 400,000 lb. The maximum load reached was 449,000 lb. (62,400 psi on net section, 39,200 psi on gross section and a nominal average shear stress of 37,300 psi), when the top west angles ruptured at the toe. As the load dropped, the lower east joint began to tear and final failure was through the net section at the lower east side in a manner similar to that shown in Fig. 20. It is of particular interest to note that the secondary (or partial)
failure actually occurred first.

Type E Specimens

The type E specimens were also of the "laced-I" configuration but of 5" x 5" x 3/8" angles. Details of this specimen type are shown in Fig. 5. Because of the marked differences in behavior between the drilled and punched specimens, and because of the repeated tests which had to be made on the drilled specimens before final failure, the tests of this group of specimens will be described in somewhat greater detail.

Specimen ED1. Three tests were conducted on Specimen ED1. In the first of these tests, when the load had reached 250,000 lb., Luder's bands appeared on the lower east gusset at the last row of rivets. By 500,000 lb. (34,600 psi on gross section), yielding was evident at the first row rivets of the east angles. The maximum load was 722,000 lb. (60,500 psi on net section, 50,000 psi on gross section), at which point the load began to drop. At about 690,000 lb., the specimen suddenly failed by shearing all rivets of the lower east gusset. The nominal average maximum shear on the rivets had been 40,860 psi.

The lower west rivet heads were cut off and the rivets were driven out so that both lower gussets could be reconnected using high strength (ASTM A325(7)) bolts installed by torque-wrench at 370 ft-lb. or more. With the lower joint so bolted, the second test
was run on Specimen ED1. At about 625,000 lb. both first row bolts on the east side sheared off. When the load reached 762,000 lb. (about 63,800 psi on net section, 52,800 psi on gross section), the upper west gusset suddenly sheared all rivets. This occurred at a nominal average shearing stress of 43,100 psi on the rivets.

Again, the heads of the remaining rivets were cut off and the rivets were replaced with high strength bolts. The two bolts which sheared in the lower east gusset during the second test were not replaced since, to do so, considerable reaming would have been necessary. With both joints bolted, a third test was run. The maximum load was 811,000 lb. (67,900 psi on net section, 56,200 psi on gross section and a nominal average shear stress of 45,900 psi), and the tension failure occurred on the lower east side at the second row rivet holes (first bolts) in the outstanding legs and through the first batten rivet as shown in Fig. 21.

**Specimen ED2.** Specimen ED2 required four tests. At 500,000 lb. (34,600 psi on gross section) during the first test, faint Lüder's lines were noticed at the first row rivets on the angles. By about 600,000 lb. yield bands had appeared at the lacing rivets. The load was increased in steps to 700,000 lb. (58,600 psi on net section, 48,500 psi on gross section) at which point the usual readings and a few photographs were taken. After dropping the load to 600,000 lb. to permit safe removal of the gages, the specimen failed in shear, as it was reloaded, at 698,000 lb. (58,500 psi on
net section and 48,300 psi on gross section). At 700,000 lb., the average nominal shear stress on the rivets had been 39,600 psi.

For the second test, the rivets in the lower gussets were replaced with common bolts which were torqued to relatively high tensions. These bolts had an average ultimate strength of 66,230 psi on the mean root area. The two first row bolts in the east angles sheared at 525,000 lb. and at 618,000 lb. When the load reached 625,000 lb., the second row bolts in the east angles sheared. This was promptly followed by a shearing of all the bolts in the bottom east joint.

For the third test, the lower joint was welded with full length fillet welds along the toes and across the ends of the angles; no weld was placed across the edge of the gusset near the first row holes. The fitting up bolts were left in place. As the load reached 774,000 lb. (64,800 psi on net section, 53,600 psi on gross section), the rivets sheared at the top east gusset at a nominal average shear stress of 43,800 psi. An indication of the relative shear deformation in the rivets and bolts along the length of this member may be seen in Fig. 22. It is readily apparent that the fasteners in the first rows withstood extremely large distortions without failing and that the deformations along the line of rivets were far from uniform. In addition, a slight necking of the angles at the first row was apparent.

As had been the case for the lower joint, the upper joint
was welded. The specimen was tested a fourth time, and failure occurred by tearing of the angles at the ends of the weld at the lower east joint at a load of 796,000 lb. (66,700 psi on net section, 55,100 psi on gross section).

Specimen EPl. As the total load on Specimen EPl approached 450,000 lb. (31,200 psi on gross section), Lüder's lines indicated yielding at the first row rivets of the east angles. Yield bands appeared around the lacing rivets on the inside legs of the angles at 500,000 lb. The maximum load obtained was 738,000 lb. (61,800 psi on net section, 51,100 psi on gross section and a nominal average shear stress of 41,800 psi), at which time the toes of the east angles ruptured at the first row as shown in Fig. 23. A secondary failure was found to have started at a rivet in a west-angle at the top joint.

Specimen EP2. As a load of 550,000 lb. (38,100 psi on gross section) was reached on Specimen EP2, Lüder's bands were noted at the last row rivets of the gussets and at the first row rivets of the angles indicating balanced yielding in the member. At 600,000 lb., signs of yielding were evident around the lacing rivets. The maximum load reached was 733,000 lb. (61,400 psi on net section, 50,800 psi on gross section and a nominal average shear stress of 41,500 psi) and failure occurred through the net section at the lower east gusset in a manner similar to that shown in Fig. 23.
RESULTS AND ANALYSIS OF TESTS

Table 1 lists the areas and properties of the various specimens and Table 5 shows the ultimate loads, type and location of the failures, and specimen efficiencies.

Because of the more general usage of stresses rather than strains, the strain data obtained in these tests is presented in terms of stress. Such an analysis must, of course, be limited to the range of loads for which stress is proportional to strain. It is hoped that this method of presenting strains in terms of a stress level will give the reader a clearer picture of the behavior of the members. However, it must be kept in mind that a stress obtained in this way represents only the stress in the member at the gage location, just as the recorded strain can only represent the strain at that gage location.

Distribution of Load to Pull Plates

The load in each of the pull plates was computed by assuming a parabolic strain distribution to determine an average strain and by using a Modulus of Elasticity, $E$, of 30,000,000 psi.

The loads for both pull plates at the top and bottom could thus be computed, and a check made of the actual load applied to the specimen. By a comparison at one end, of the load on one plate to the total load carried by both plates, the per cent of load in each gusset plate was obtained. The distribution of load to the pull plates, as obtained in this manner, is shown in Figs. 24 through 31.
In these figures, the average stress on the gross section and the total load is plotted against per cent of load in the pull plates. The ultimate load is also shown, as is the manner of failure. From these plots, it can be seen that near the ultimate loads the load distribution to all four gussets had approached 50 per cent. None had a distribution more than 5 per cent different, despite highly unequal distributions at earlier loads. For this reason, it is felt that the ultimate loads obtained in these tests were independent of the variations in load distribution during the early stages of the tests. However, it is reasonable to surmise, from a study of these plots and other data, that the earlier inequality of load distribution may have increased the deformation of one side of the specimen over the other side and thus may have had an effect on the point of failure.

With but few exceptions, the point of failure (east or west, top or bottom) can be predicted from the plots of load distribution. Specimen BPl, Fig. 26, failed on the west side despite the heavy initial loading of the east gussets. At the loads near ultimate, it is seen that the west gussets, particularly the top west, carried an increasing share of the load, and the top west gusset finally took more than half the load. Although the primary failure occurred on the west side, we find from the test description that the east side actually did rupture first. Thus, this exception certainly is not in disagreement with the
correlation noted between the location of failure and the initial
load distribution in the gussets.

Specimen CD2, Fig. 27, also failed at the top west joint
despite higher loads in the east gussets. Though it is not known
which rupture occurred first, this specimen exhibited a primary
failure at the top west and a secondary failure at the bottom east.

Specimen DM1, Fig. 28, also appears to be an exception.
However, again we find that this specimen failed at two points:
the primary failure was at the top west, following the indication
of Fig. 28 where at about 350,000 lb. the west was carrying a
higher percentage of the load near ultimate; the secondary failure
occurred at the bottom east, reflecting the effect of the general
tendency for the bottom east to carry a large portion of the load
during most of the test. It is believed that the initial appearance
of compression in the top east gusset of this specimen was partially
a result of the manner of specimen installation and partially a
result of the insensitivity of the measurements at the lower loads.

Load-Strain Relationships
An appreciation of the fact that the use of "stress" in
this section means the stress at the gage point and not an average
stress is important to an understanding of the following discussion
which has been divided into three groups. First, the type A
specimens will be considered; second, the type C specimens; and
finally, the laced members, types B, D, and E, will be analyzed.
Type A Specimens. The stresses in the angles of ADL, at the four strain gage locations, are summarized in the following tabulation:

**ANGLE STRESSES, ADL**

<table>
<thead>
<tr>
<th>Load (lb.)</th>
<th>Av. Stress on Gross Area (1000 psi)</th>
<th>Stress, 1000 psi, in the Angles, Based on the Measured Strains</th>
<th>Ratio, Av. Stress based on the Measured Strains</th>
</tr>
</thead>
<tbody>
<tr>
<td>50</td>
<td>1.82 SW 1.5</td>
<td>1.5 Gage S1 2.1 Gage S2 2.7 Gage S3 1.60 Gage S4 1.95</td>
<td></td>
</tr>
<tr>
<td>100</td>
<td>3.64 SW 2.5</td>
<td>3.0 Gage S1 4.5 Gage S2 5.1 Gage S3 1.75 Gage S4 3.77</td>
<td></td>
</tr>
<tr>
<td>200</td>
<td>7.28 SW 5.7</td>
<td>6.0 Gage S1 8.1 Gage S2 9.6 Gage S3 1.51 Gage S4 7.10</td>
<td></td>
</tr>
<tr>
<td>300</td>
<td>10.92 SW 8.7</td>
<td>9.3 Gage S1 12.6 Gage S2 14.1 Gage S3 1.48 Gage S4 11.17</td>
<td></td>
</tr>
<tr>
<td>400</td>
<td>14.56 SW 12.0</td>
<td>12.6 Gage S1 16.8 Gage S2 18.6 Gage S3 1.44 Gage S4 15.00</td>
<td></td>
</tr>
<tr>
<td>500</td>
<td>18.18 SW 15.3</td>
<td>16.5 Gage S1 21.3 Gage S2 23.4 Gage S3 1.41 Gage S4 19.12</td>
<td></td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>Av. 1.53</td>
</tr>
</tbody>
</table>

From the above data, it may be seen that both of the east angles have higher stresses than do the west angles. This is as should be expected from the load distribution shown in Fig. 24 wherein, up to about 600,000 lb., the east pull plates carried about 60 per cent of the load, or about one and a half times the load in the west plates. This same relative distribution is evident in the angle stresses.

Since the four angles were not equally strained, the batten plates and webs connecting them must have developed shears and thus introduced additional secondary stresses in the specimen. If the difference in total load between the east (60 per cent) and the west (40 per cent) sides of the specimen were assumed to
be taken by the batten plates only, at 500,000 lb. the 100,000 lb. total shear would be distributed to the four batten plates giving a computed unit shear of less than 4500 psi. However, the distribution of load to the two sides of the specimen became more nearly equal at loads approaching ultimate, so that the shear on the battens did not increase proportionately with the load. From a similar inspection of the differences in strain and the relative areas involved, the unit shearing stresses in the webs would probably be 1/5 to 1/10 of that in the batten plates, and thus of little significance.

The strain data for the angles of Specimen AD2, shown in the following tabulation, reveal that the northeast angle was the most highly stressed, whereas the other three angles appear to be about equally stressed (or strained).

**ANGLE STRESSES, AD2**

<table>
<thead>
<tr>
<th>Load (lb.)</th>
<th>Ave. Stress on Gross Area (psi)</th>
<th>Stress, 1000 psi, in the Angles Ratio, Ave. Meas. Stress</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>1000</td>
<td>1000 Gage S1 Gage S2 Gage S3 Gage S4 East Fire</td>
</tr>
<tr>
<td>50</td>
<td>1.82</td>
<td>2.1 NE 2.1 1.8 1.8 1.17 1.95</td>
</tr>
<tr>
<td>100</td>
<td>3.64</td>
<td>4.0 SE 3.6 3.6 1.8 1.18 3.92</td>
</tr>
<tr>
<td>200</td>
<td>7.28</td>
<td>7.5 SW 7.5 7.5 1.08 7.80</td>
</tr>
<tr>
<td>300</td>
<td>10.92</td>
<td>11.7 NW 11.7 11.4 1.05 11.85</td>
</tr>
<tr>
<td>400</td>
<td>14.56</td>
<td>15.3 NW 15.3 1.08 15.82</td>
</tr>
<tr>
<td>500</td>
<td>18.18</td>
<td>19.8 West 19.8 1.05 20.10</td>
</tr>
</tbody>
</table>

This is further substantiated by Fig. 24 which shows that the loads in all pull plates of AD2 were about equal. As a result, the shears
in the battens and web plates caused by the unequal distribution of strains in the angles of this specimen will be of even smaller consequence than those in Specimen AD1.

An inspection of the strains in the web plates of both A specimens showed that very little bending occurred in these plates at mid-length. Since the strains on both sides of the web plates were similar at a given load, strains at the center of each web plate have been averaged and converted to a stress at that point. These web stresses are as follows:

<table>
<thead>
<tr>
<th>Load in lb.</th>
<th>Average Gross Area</th>
<th>Average Net Section at Center</th>
<th>Specimen AD1 Stress, 1000 psi</th>
<th>Specimen AD2 Stress, 1000 psi</th>
</tr>
</thead>
<tbody>
<tr>
<td>1000</td>
<td>1000 psi</td>
<td>1000 psi</td>
<td>S6+S8 2 2</td>
<td>S5+S7 2 2</td>
</tr>
<tr>
<td>50</td>
<td>1.82</td>
<td>2.04</td>
<td>3.6 1.6</td>
<td>3.3 2.3</td>
</tr>
<tr>
<td>100</td>
<td>3.64</td>
<td>4.09</td>
<td>6.5 3.5</td>
<td>5.7 4.0</td>
</tr>
<tr>
<td>200</td>
<td>7.28</td>
<td>8.18</td>
<td>12.2 7.2</td>
<td>10.8 8.4</td>
</tr>
<tr>
<td>300</td>
<td>10.92</td>
<td>12.27</td>
<td>18.2 11.3</td>
<td>15.6 13.5</td>
</tr>
<tr>
<td>400</td>
<td>14.56</td>
<td>16.36</td>
<td>24.0 15.9</td>
<td>20.4 18.0</td>
</tr>
<tr>
<td>500</td>
<td>18.18</td>
<td>20.45</td>
<td>29.7 20.5</td>
<td>25.4 22.8</td>
</tr>
</tbody>
</table>

At first glance, this tabulation seems to show that the webs of these box members tended to be over 100 per cent efficient, i.e., carry a higher stress (or share of the total load) than would be expected from the average stress on the net section of the specimen at that gage location. This appearance is due to the uneven distribution of stress to the webs and angles. However, it is reasonable to expect that the web efficiency of these box members
would be greater than that of I-beam webs,\(^8\) because of the direct transfer of load from the gussets to the webs in these box sections.

A comparison of the stresses in the angles and webs of the type A specimens indicates that the webs of AD2 were probably less effective, that is, had less strain at any given total load than did the AD1 webs. Consequently, for AD2 there was probably less shear on the rivets connecting the gusset to the web (not including those rivets which also connected the angles) and more shear on the rivets connecting the angles to the gussets than for AD1. This may account, at least in part, for the different modes of first failure for AD1 and AD2, (AD1 had a complete shear failure of the joint; and AD2 sheared only the outer gusset-web-angle rivets). Another factor which may have contributed to the difference in behavior was the variation in ductility of the web plates, which may be found in Table 2.

**Type C Specimens.** Analysis of the strain data for both type C specimens reveals that the stress in the east angles was considerably higher than that in the west angles. However, because of the small number of gages and the stress concentrations caused by the stitch rivets, a thorough analysis of the strains cannot be made. See the following tabulation.
ANGULAR STRESSES AT MID-LENGTH FOR C SPECIMENS

<table>
<thead>
<tr>
<th>Total Load Stress on Gross Spec. Area</th>
<th>Specimen CD1</th>
<th>Specimen CD2</th>
</tr>
</thead>
<tbody>
<tr>
<td>Specimen</td>
<td>Stress, 1000 psi</td>
<td>Stress, 1000 psi</td>
</tr>
<tr>
<td>Av.</td>
<td>S1+S2</td>
<td>S3+S4</td>
</tr>
<tr>
<td>---</td>
<td>---</td>
<td>---</td>
</tr>
<tr>
<td>1000 lb.</td>
<td>50</td>
<td>2.57</td>
</tr>
<tr>
<td>100</td>
<td>5.14</td>
<td>3.90</td>
</tr>
<tr>
<td>150</td>
<td>7.72</td>
<td>5.65</td>
</tr>
<tr>
<td>200</td>
<td>10.29</td>
<td>7.55</td>
</tr>
<tr>
<td>250</td>
<td>12.86</td>
<td>9.55</td>
</tr>
<tr>
<td>300</td>
<td>15.43</td>
<td>11.70</td>
</tr>
<tr>
<td>350</td>
<td>18.00</td>
<td>13.80</td>
</tr>
<tr>
<td>400</td>
<td>20.58</td>
<td>15.95</td>
</tr>
<tr>
<td>Av.</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

It is of interest to note that the average loads in the west sides of both members, as shown in Fig. 27, were about 79 percent of the loads in the east sides at total loads from 50,000 lb. to about 400,000 lb. This ratio differs considerably from that obtained on the basis of the strains in the angles. Although measurements are not available to explain this difference, it is believed that it may have resulted from an initial curvature in the specimens.

Laced Specimens: Types B, D, and E. The discussions of the load-strain relations for the three laced specimen types, B, D, and E, have been combined since their behaviors were similar. Although the lacing configuration is not the same (east to west) at the mid-height of the specimens for the No. 1 and No. 2 specimens, no change was made in the numbering of the strain gages. Thus, for example, S3 and S4 will always designate those gages.
opposite the point on the angles where there was a lacing rivet, and readings of these gages may always be compared with other S3 and S4 gages on similar specimens.

By converting the ratios of strains in the two sides of the laced members to percentage of load, we find that between 25,000 lb. and 300,000 lb., the east sides tended to carry about 60 per cent and the west sides 40 per cent of the total load. However, the load distributions based on the strains, and given in the following tabulation, all appear to be slightly higher than those obtained from the pull plate measurements shown in Figs. 25, 26 and 28 through 31. This relationship is similar to the variation noted for the type C specimens also.

**PER CENT OF LOAD ON EAST SIDE OF MEMBERS COMPUTED FROM ANGLE STRAINS**

**LOAD RANGE FROM 25,000 TO 300,000 LB.**

<table>
<thead>
<tr>
<th></th>
<th>BD1</th>
<th>BPI</th>
<th>DDI</th>
<th>DPL</th>
<th>EDL</th>
<th>EPI</th>
<th>No. 1 Av.</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>0.68</td>
<td>0.66</td>
<td>0.57</td>
<td>0.59</td>
<td>0.54</td>
<td>0.62</td>
<td>0.61</td>
</tr>
<tr>
<td>BD2</td>
<td>BPI</td>
<td>DDI</td>
<td>DPL</td>
<td>EDL</td>
<td>EPI</td>
<td>No. 2 Av.</td>
<td></td>
</tr>
<tr>
<td>0.63</td>
<td>0.61</td>
<td>0.63</td>
<td>0.60</td>
<td>0.57</td>
<td>0.61</td>
<td>0.61</td>
<td></td>
</tr>
</tbody>
</table>

By means of a comparison of the measured strains in all laced members, it was seen that there was very little difference in the average strains at the mid-length gage locations for loads from 25,000 lb. to 300,000 lb. whether the specimens were drilled or punched. A comparison was also made of ultimate loads for the four pairs of laced specimens which had failures other than
shear failures. This latter comparison gave an average of 96 per cent for the ratios of loads of punched to drilled members versus 99 per cent for the ratio of drilled to punched average strains in the twelve laced specimens.

The four E specimens have not been included in the ultimate load comparison mentioned in the preceding paragraph because the two drilled specimens sheared their fasteners at loads about 3 per cent smaller than those at which the punched specimens failed at the net section. If we include these in the average ratio, we get 0.985 which is about the same as the 0.99 obtained by comparing the strains. Were we to neglect the strain hardening effects of the multiple loadings on specimens ED1 and ED2 and to use their final fracture loads in computing the ratios, the ultimate loads of the punched specimens with lacing would be approximately 95 per cent as great as that of the drilled specimens.

Of considerable interest is the fact that the ultimate strength of the punched specimen DP2 exceeded the ultimate strength of its drilled counterpart, DD2, and that both failed in tension in similar fashions.

Lacing Bar Strains

Each of the two upper lacing bars of all the laced specimens had two strain gages mounted at mid-length and along the centerline, one gage on either side. Thus, a determination of the magnitude of the stresses in the lacing bars could be made. It
was noted that there was very little bending of the lacing throughout the range of loading. Accordingly, the readings of the two strain gages on each bar have been averaged and converted to stress on the gross section of the bar. The results of these computations are presented in Figs. 32, 33 and 34.

In addition, curves are shown for the theoretical stresses (for the elastic range only), which are equal to the nominal stresses in the angles multiplied by the squares of the cosines of the angles that the lacing bars made with the axes of the member. The stresses in the top lacing bars differed from the theoretical stresses because of the complex end effects and the unequal distribution of load to the members. These same factors, no doubt, also affected the stresses in the second lacing bars, but to a lesser degree. It is felt that for a long member the central lacing bars would generally be stressed at a level approximately equal to that suggested by theory.

It will be noted that the lacing bars of both No. 1 specimens behaved differently from those of the No. 2 specimens. Because of the consistency of this variation, it is believed that orientation of the specimens in the testing machine produced this dissimilarity.

From Figs. 32, 33 and 34, it is seen that, at working loads, the stresses on the lacing bars were less than 10,000 psi on the gross section of the bars; and, at loads about twice the design load, the stresses on the gross section of the lacing bars,
reached 15,000 to 20,000 psi, in some cases.

A variety of failures was obtained with the laced specimens. Four of the members, the type B specimens, failed at lacing rivets in the middle of the members; the drilled type E specimens failed initially in shear and subsequently in tension at the net section; and the punched type E specimens and all of the type D specimens failed at the net section. Of those specimens which failed at the net section, three fractured at the side of the connection at which the lacing bar terminated; the remaining five fractured at the side of the connection opposite the lacing bar. Thus, the position of the lacing bars did not seem to affect the location of the rupture.

Load-Deformation Relationships

Measurements were made of the overall deformation, the slip at the first and last rows, and the lateral movement of the upper pull plates. For the analysis of the overall deformation, the four readings from the duplicate specimens were averaged, thus permitting a comparison of the punched and drilled specimens as well as a comparison of the average deformations of the various types of members tested.

In general, the measurements of overall deformation reflected the same general pull-plate load distribution shown in Figs. 24 through 31. Where the east pull plates were more heavily loaded, the east deformation was greater. And, just as the loads in the pull plates became more nearly equal as the loads approached
ultimate, the deformations of the two sides of the specimens became more nearly equal. Although the differences in loading on the two sides introduced a smaller deformation on one side than on the other, the average deformations in Fig. 35 give a general indication of the relative behavior of the various members.

It is of interest to note in Fig. 35 that at a given load the punched specimens of any type (shown by dashed lines) deformed less than did the similar drilled specimens. This was particularly true of the loads above the normal design range.

For the sixteen specimens tested, the overall deformation at a stress of 15,000 psi on the gross section (about 20,000 psi on net section) varied from a minimum of 0.024 in. for the BP specimens to a maximum of 0.059 in. for the AD specimens. Since these members were only 32 in. between the first row fasteners, the usual elastic analysis (based on the gross section) gives a computed deformation of 0.016 in. for the member itself; however, the actual deformation would be slightly larger. This suggests that slip at the first rows and deformation along the length of the two joints should account for the balance of the movement or about 0.01 in. for the BP specimens and 0.04 in. for the AD specimens. Referring to Fig. 36, we find that at the two first rows, the slip in the joints of the BP specimens actually totalled almost 0.01 in. and in the two joints of the AD specimens almost 0.03 in. The average slip and deformation in the two connections of a specimen,
therefore, were equal approximately to the computed deformation of an additional 4 ft. length of the member. This seems to partially justify the use of the distance between panel points to determine the deformation of the members of a truss. Such an assumption, although approximate, is probably no more in error than many of the other assumptions made in determining the deformations of a structure.

For a given specimen, slip at the first row was obtained at six separate points. The measurements at all six points reflected variations in the distribution of load and specimen configuration. By averaging all six first row slip readings of one specimen with the six from the identical specimen, Fig. 36 was obtained. Here we see, once again, that at any given load the punched specimens generally underwent less average deformation than did the comparable drilled specimens. By taking into consideration the scale of this figure and that of Fig. 35, it can be seen that the slip in the two joints was equal to about one half of the overall deformation obtained from these comparatively short members.

The measurement of slip at the last row of rivets yielded only relative information on the deformation. These measurements at the end of the joints included slip as well as local deformations in the angles and gussets and consequently are not directly comparable with the other slip data. Nevertheless, the deformation of the punched specimens at the last row of rivets was again less than that
of the drilled specimens and was similar to that shown in Fig. 36.

Discussion of Failures

Of the sixteen specimens tested, only eight failed initially at the net section. The other eight specimens, fell into two groups: those which failed in shear, and those which failed in tension at points other than the net section.

The behavior of riveted joints has long defied exact analysis; the many attempts made in the past century have met with only partial success because of the many variables which affect this behavior. As early as 1867 Schwedler\(^1\) showed that the behavior of a riveted joint at working loads was dependent on the friction, thereby extending the observations in 1850 by Clark.\(^1\).

Since then, this finding has been repeatedly reported, and at one time, according to Francis,\(^9\) German specifications used joint friction as a basis for design. However, the amount of friction which will exist in a given joint is difficult to predict. This fact led American engineers to continue the use of design methods which do not utilize friction, but which instead, are based on the areas required for tension, shear, and bearing.

The most common American specifications \(^{10, 11, 12}\) based on many years of experience, assume that designs will be made for equal partition of the load to the fasteners in a joint, and specify the use of nominal unit stresses. Some investigators have presented statements supporting this practice (Ref. 13 and
however, others have questioned the validity of the assumption when a connection is very long (Discussion of Ref. 14).

Francis, (9) Batho, (15) and others, (1) present observations and calculations which indicate that, dependent on the number of fasteners in a line, the end rivets of a connection may take 2 to 15 times the load carried by the innermost rivets and that such may represent actual rivet loads of about 1.2 to 2.8 times that assumed in design. Batho (15) further suggests that increasing above five the number of rivets in a line does not materially reduce the load on the end rivets. Despite the fact that such elastic analyses are not completely suitable for the prediction of the load partition to rivets because of joint friction, the change in joint behavior and slip throughout the range of loading, and the impossibility of obtaining an ideal joint commercially, they do point up the error in assuming equal load distribution. Experiments on conventional joints have indicated that load is only partially redistributed after yielding and slip occur.

Because of the unequal distribution of loads along the length of long connections, a shear failure of the rivets may be a progressive failure. However, it should be noted that progressive failure may in reality be quite sudden - so sudden, in fact, that the eye cannot detect the sequence of rivet failure. Most likely the first rivet fails after gradually reaching a large deformation. When it fails, the load is very suddenly shifted to the next rivet which already has been deformed nearly enough to cause its failure.
The shock or impact attending the load transfer, shears this second rivet immediately and seemingly the rivets all fail at the same time.

An examination was made of the deformations of the specimens tested in this program. An unfractured joint from each type of specimen with each method of hole preparation was cut from that specimen and sectioned at the rivet center lines. These sections gave a complete picture of the relative deformation of the rivets in the connections and aided greatly in explaining the behavior of the members.

The unequal rivet deformation and the resulting inequality in the partition of load which caused the premature shear failures in the type A specimens were evident in the sections of the type A member. All the rivets in the center line of rivets of the joint suffered very small deformations; the two end rivets exhibited shear deformations of only about 0.08 in. The end rivets of the second line deformed about 0.10 in. and the end rivets of the outer line deformed about 0.32 in. This large deformation in the rivets of the outer line may be seen in Fig. 37. An enlarged view of the first rivet is shown in Fig. 38. Notice how the sharp corner of the hole in the web was deforming the rivet at the web-gusset plane and how the severe bending had started to pry off the rivet head.

An inspection of the sectioned connections revealed that the end rivets were the most highly deformed, the inner rivets
frequently had comparatively low deformations, the rivets of the punched specimens were less deformed than were those of like drilled specimens, the holes of the drilled specimens enlarged more than the holes of the punched specimens, and the first row rivets of specimens CD2 and EP1 (both with 10 rows) were probably deformed such that the ultimate strength of the end rivets had been reached. Further loading of those two joints would probably have caused failure of the rivets.

The type A and E joints which were refastened with ASTM A325(7) high strength bolts failed in tension and sheared several of the bolts. However, since the bolts were installed in a specimen which had already been distorted, direct comparisons should not be made between the riveted and bolted connections.

Shear failures occurred in the AD and ED specimens, all four of which were drilled and fastened with 3/4 in. rivets. The rivets of the AD specimens were hand driven but those of the ED specimens were machine driven.

From Table 4 it may be seen that the undriven 3/4 in. rivets exhibited an average nominal ultimate shear strength of 48,200 psi and the undriven 7/8 in. rivets an average nominal ultimate of 44,000 psi. A number of previous investigators have shown that driving increases the shear strength based on nominal diameter by as much as 33 per cent.(16) After driving, increases of similar magnitude have been noted also in the tensile strength
of rivets. Since the properties listed in Table 4 are for undriven rivets, we would expect that the ultimate rivet strengths in the connections might be perhaps as much as 15 to 20 per cent higher.

A summary of the maximum nominal shearing stresses which may have existed at ultimate will be found in Table 6. These values are based on the distribution of load to the east and west pull plates just before failure.

The only specimens which failed in shear were AD1, AD2, ED1 and ED2. The nominal unit shears (from Table 6) at failure of the A specimens and E specimens are quite different, yet the ultimate strength in shear of the rivet stock for both was about 48,200 psi. Therefore, the nominal shear stresses on the A specimen rivets were only about eight-tenths of the ultimate. Further, from Table 1, we see that the Tension:Shear ratio (T:S ratio) of 1.0:0.65 is well below the 1.0:0.75 allowed by current specifications.

Since the nominal shear stresses were not excessively high, could the cause of failure be combined tension and shear caused by the bowing of gussets and webs? This question may be answered negatively. Munse and Cox (17) show that a rivet subjected to 0.8 of its ultimate shear stress is able to resist, in addition, a tensile stress equal to about half its ultimate tensile stress. It appears unreasonable then to assume that tension plus shear could have been the principal cause. From an examination of the joint deformation, it would appear that the shear failures of the A and
E specimens were due to two causes, singly or in combination: excessive deformation of the end rivets in a long joint, and failure to distribute the rivets on gage lines in proportion to the corresponding cross sectional areas. This latter point has been raised previously by other authors. (e.g., Ref. 13)

Since the A joint was only 7 rows long, the effect of the length of joint was probably minor compared with the second effect. A comparison of the distribution of area to the five lines of rivets indicates that the outer lines of rivets each connected 3.42 sq. in. of net area (using AREA areas) or 34.1 per cent of the total. The three inner rows each carried 1.06 sq. in., or 10.6 per cent of the total. Immediately we see by this crude analysis that the material in the two outer lines of rivets contained 68 per cent of the net area of the joints but only 40 per cent of the rivets. From the maximum gusset load for the A specimen as given in Table 6, the unit shear on the outer lines of rivets would be

\[
\frac{600,000 \times 0.68}{14 \times 0.442} = 65,900 \text{ psi}
\]

This high unit shear undoubtedly did not exist at failure because of a partial redistribution of load after yielding. However, it is of considerable interest to note that this computed ultimate shear stress is only about 37 per cent greater than the ultimate shear strength of the undriven rivets; as noted earlier, the driving could have produced an increase of as much as 33 per cent.
It may be recalled also that only the rivets in the angles of Specimen AD2 sheared and then the web tore, indicating that the shear on the outer lines of rivets was excessive. Consequently, it would seem desirable to proportion the rivets in such a connection on the basis of the contributing cross sectional areas to more nearly equalize the loads on the rivets.

A redesign of the A specimens on the basis of area distribution, maintaining the same net section and approximately the same number of rivets, would give: twelve rivets in each outer line, and four rivets in each inner line. Such a joint would not be acceptable to many engineers because of the excessive length of the joint and the large pitch along the inner lines of rivets which would make these rivets less effective. One means of reducing the length of such a connection proportioned on the basis of area would be to increase the rivet diameter, another would be the use of lug or clip angles. However, the use of lug or clip angles to transfer some of the load from the member to the gusset may not be a fully satisfactory solution in view of the observations of Wyly, Shedd and others. The use of clip angles has been examined by few investigators and might well receive additional attention.

As noted earlier, drilled specimens ED1 and ED2 failed in shear at loads below those at which the like punched members failed in tension, although many investigators have held that drilled joints are to be preferred. Table 6 shows that the average nominal unit shears on the rivets of the punched specimens actually
were higher than the average nominal shears on the drilled specimens.

In recent literature on the strengths of joints prepared by drilling and punching, this apparent higher shear strength of punched specimens has not been noted because most of the investigators have designed their joints to assure tension failures. Similarly, by using a T:S ratio of 1.0:0.68, it was assumed that tension failures would occur also for all of the E specimens. However, the design of the Type E specimens resulted in joints which were closely balanced with respect to shear and tension. This balance was affected by the method of hole preparation; the punched holes actually providing stronger joints, by a few per cent, than did the drilled holes. Interestingly enough, this same paradox had been noted some ninety years ago by Maynard.\(^{(1)}\)

It will be recalled from Figs. 35 and 36 that the performance of all the punched specimens at any given load was better than that of the corresponding drilled specimens in one other respect--the deformation or slip was smaller. This may be a clue as to why the punched specimens did not fail in shear even at nominal unit shearing stresses equal to or greater than those which caused rivet failure for the drilled specimens. One reason for this difference in shear strength may be the sharpness of the edge of the drilled holes. Another reason may be the keying action produced by the slight depressions and burrs left on the surfaces of the connected parts by the punching. These burrs, under the
forces produced by tension in the rivets, may act as shear keys and impede the shearing deformation of the joints.

This still leaves the question: why did the drilled E specimens fail in shear when they were thought to be overdesigned to assure tension failures? The highest nominal unit shear on the drilled E specimens was 46,000 psi while the undriven rivets had a coupon strength of 48,200 psi. If no allowance is made for increase in strength due to driving, we find that this shear was only 95 per cent of the ultimate; but, with a moderate increase in strength of only 15 per cent from driving, the nominal shear in the connection would have been only 83 per cent of the expected ultimate. One factor which might result in such an unanticipated failure is the length of the joint. A number of years ago Jones (14) warned that perhaps the unit shear allowed for a long joint should not be as great as that permitted for a short joint. The apparent lower joint strength of the drilled E specimens may be a result of the highly unequal distribution of rivet loads which occur along the length of such a joint. On the basis of these few tests and the other test data in the literature, it appears that only about eight rivets can be placed in a line if they are to develop their full collective shear strength. At working stresses where the load is carried principally by friction, longer joints behave satisfactorily but may give a false sense of ultimate strength because of their reduced ultimate shearing capacity.

Two types of the tension failures which warrant attention
are the gusset plate failures of specimen AD1 and the failures at
the lacing bar connections for all four of the type B specimens.
The final gusset failures of AD1 were largely the result of the
relatively high stress concentrations. A number of other
investigators (1, 20, 21) have shown the importance of these
stress concentrations. The gusset net section area, 35.63 sq. in.
(AREA), was 176 per cent of the net section of the specimens and
undoubtedly would have been more than sufficient had the gussets
been narrower but thicker. As shown by Lüder's lines in the
whitewash, the stress concentrations caused yielding of the gusset
at the last row rivets at a load of 400,000 lb. This crude
evaluation suggests a stress concentration of slightly more than
three. The gusset plate tore at a nominal average stress of about
35,000 psi, a stress higher than that reached by any other specimen.
In the test of AD1 it was noticed that after the first shear failure
at 1,155,000 lb., the east gusset had necked down considerably at
the last row of rivets. Upon reloading, the gussets failed before
the rivets of the upper joint sheared. However, it should be pointed
out that the upper joint of AD1 was near the point of shear failure.
This is evident from an inspection of the joint section in Fig. 37
and of the rivet shown in Fig. 38. The loss of this rivet head at
the first row would probably have started a progressive shear failure
throughout the remainder of the joint.

The other type of tension failure which was unexpected
was that shown by the B specimens. The AREA net section of two
angles at the first row of rivets was 4.31 sq. in. and yet failure occurred in the two angles at a point having an AREA net area of 4.97 sq. in., 15 per cent greater. The reason for this unusual failure was the effect of the lacing bars, which actually contributed to the failures. There were three laced specimen types but only one exhibited this unusual failure pattern.

The type D specimens had a very small net area to gross area ratio--63 per cent; the type B specimens with the same size angles, rivets, and lacing bars had a net area to gross area ratio of 75 per cent; thus, failures at the net section would be expected to occur in the D specimens at lower total loads since both specimen types had the same cross section at the lacing rivets. This, then, is one explanation why the D specimens were more likely to fail at the net section than were the B specimens. A second reason lies in the contribution of the lacing bars to the loading in the angles. Some of the lacing bars in the B specimens exhibited stresses which were somewhat higher than those stresses in the D specimen lacing bars. These tensile forces acted as concentrated loads at the lacing rivets thereby further stressing the member angles which were already subjected to axial tension. This bending stress applied by the lacing increased the tensile stresses on the toes of the angles at the lacing rivets and the combined stresses caused failure sooner than would have otherwise occurred.

By means of an approximate analysis the conditions can be
determined which would have had to exist to induce failures at the lacing connections. At failure, the center lacing bars of the B specimens would have had to have a gross stress of about 6000 psi by such an analysis. This is in reasonably good agreement with the maximum stress measured at the lacing bars and shown in the lower portion of Fig. 32. The D specimens, by similar analysis, would require a lacing bar stress of about 8000 psi to produce the same type of failure. However, this stress was not reached by any of the D specimens lacing bars. Because the E specimens had angles of greater stiffness than those used in the B and D specimens, the secondary effects from the forces in the lacing bars were comparatively small. Accordingly, none of the E specimens failed at the lacing connections. Therefore, although the lacing bars may have affected the ultimate strength of the B specimens by perhaps 10 per cent or less, they did not appear to affect materially the ultimate loads of the D and E specimens.

Lacing in a tension member is used to assist in handling in the field and the shop, thus avoiding local buckling, and to adjust the shears in the member which result from unequal loading. Scott and Cox(22) indicate that in actual service, each of the lacing bars of a floor beam hanger composed of two 12 in. channels carried less than 1000 lb. total load. They concluded that, for working loads, continuous lacing, properly spaced, appeared to be adequate to tie the main components of the hanger together. Earlier, Wyly et al(18) had observed that hangers composed of two channels which
are connected with occasional tie plates did not act as a unit, but as two individual members which were subjected to severe racking stress. Thus, on the basis of the data reported herein and the observations of others, it seems that where a solid web is not warranted, lacing bars will provide a suitable tie; however, the solid web would be preferable.

Although it did not produce failures of the members, a great deal of warping and bending occurred in the angles and gussets at the outer ends of the type E connections. This deformation in the long connections would have been reduced greatly if the batten plates had extended the entire length of the joint. In the B and D specimens which had comparatively long tie plates, this deformation was not apparent.

Analysis of Joint Efficiencies

In the United States the three most commonly used specifications present the same rule for the computation of the effective net section of a tension member. The specifications of the American Railway Engineering Association (AREA), (10) The American Association of State Highway Officials (AASSO), (11) and the American Institute of Steel Construction (AISC), (12) although in slightly different words, all provide what is subsequently referred to as the AREA Rule for tension net areas.

The several specifications present somewhat different requirements for angles in tension which are connected through one
leg or which may be subject to bending. However, all three specifications require the following whether the method of hole preparation is drilling, sub-punching and reaming, or punching:

"The diameter of the hole shall be taken as 1/8 inch greater than the nominal diameter of the rivet."

The history of the acceptance of the AREA $\frac{s^2}{4g}$ Rule has been conveniently summarized in an article by C. H. Chapin,(23) who also mentions some of the other rules for net section determination used in the 1920's. Some of these latter rules specified that the net section along a diagonal line of holes should be from 10 per cent to as much as 40 per cent in excess of that along a transverse line.($19, 23, 24$) It has recently been shown by W. G. Brady and D. C. Drucker,(25) based on their limit analysis and tests of flat plate specimens with open or plugged holes, that the $s^2/4g$ rule corresponds to an approximate upper bound at yielding for a riveted joint. The so-called Modified AREA Rule differs from the AREA Rule only in that the actual hole diameter is used in computing the net width.

Two other suggested design rules, which are based on empirical studies, are those of W. M. Wilson (presented in a discussion of a paper by Davis, Woodruff, and Davis(14)) and F. W. Schutz, Jr. (26) The first of these has not been used in the analysis and review of these tests and, since it is readily available in the literature, will not be repeated here. The second, known as the
Relative Gage Rule, has been used to examine the results of these tests and may be expressed as follows:

**Effective Net Section**

"In the case of a chain of holes extending across a part in a zigzag, diagonal or straight line, the effective net section of the part shall be the summation of the effective net sections of all the gage strips along the chain of holes. No chain of holes shall be considered which has a gage strip with a pitch of 2/3 or more of the gage of that strip.

The critical net section of the part is obtained from that chain which gives the least effective net section.

A gage strip is the portion of the part bounded by the longitudinal center lines of two successive holes in the chain of holes being investigated. A transverse edge distance is considered as one half of a gage strip which has a gage twice the edge distance. The effective net section of a gage strip is the product of the effective net width and thickness of the strip.

The effective net width (E. N. W.) of a gage strip shall be determined by the following equation:

\[ E. \text{ N. W.} = 1.05 (g - 0.9d) KH \]
\[ \text{but not more than } 0.87 gKH \]

where

- \( d \) = Actual hole diameter
- \( g \) = Transverse spacing (gage) of any two successive holes
- \( K = 0.82 + 0.0032R \) but not more than 1.00
- \( R \) = Reduction in area of standard control coupons in per cent
- \( H = 1.00 \) for drilled holes; 0.862 for punched holes."

With respect to the Relative Gage Rule, the following
points should be emphasized: (1) It is necessary to deal with each gage strip separately and then to determine a weighted effective area. (2) Actual hole diameter is used in contrast to nominal connector diameter plus 1/8 inch as is now customary. (3) A marked distinction is made between punched and drilled holes. (4) No effect is reflected in the formula for varying the stagger between the conditions of no stagger and $s/g$ of 2/3. (5) The rule sets an upper bound of effective net width, indicating that the use of a gage of more than about 5.25 times the actual hole diameter does not increase the efficiency of a joint. (6) Some estimate of the ductility of steel must be made.

Test Efficiency as used in this report may be defined as the ratio in per cent, of the ultimate test load to the expected strength of the gross section based on the average coupon strength of the specimen at the critical section. The computed test efficiencies, the predicted efficiencies, and the ratios of these efficiencies are shown in Table 5 along with data on the ultimate strength of the specimens, AREA design loads and the resulting factors of safety. To convert the design loads to those allowed by AISC, the tabulated values must be multiplied by 20/18.

From the ratios of predicted efficiencies we see that the AREA Rule predicted, on the average, values which were 6.3 per cent high based on initial failure loads only. The Modified AREA Rule gave results which averaged 8.9 per cent too high. The
Relative Gage Rule gave results 11.8 per cent too high on the average, for all the drilled specimens, and 2.8 per cent low for all the punched specimens. If we consider only those specimens having initially net section failures, we find that: the AREA Rule gave averages 3.0 per cent high; the Modified AREA Rule gave values 5.8 per cent high; the Relative Gage Rule drilled predictions were 8.5 per cent high and the punched predictions averaged 6.5 per cent low. On the basis of these few tests, it appears, then, that of the design rules compared, the AREA Rule gave the best agreement for truss-type members.

A comparison of the predicted AREA efficiency and test efficiency is presented graphically in Fig. 39. If we approximate the net section failures with a straight line, we may obtain the following empirical relationship for test efficiency:

\[
\text{Test Efficiency} = 24.5 + 0.63 \times \text{(AREA Efficiency)} \quad \text{Eq. 1}
\]

but since

\[
\text{AREA Efficiency} = \left[ \frac{W_G - \sum D + \sum \frac{s^2}{4g}}{W_G} \right] \times 100 \quad \text{Eq. 2}
\]

and

\[
\text{Effective Net Width (ENW)} = \frac{\text{Efficiency}}{100} \times W_G \quad \text{Eq. 3}
\]

then we may obtain,

\[
\text{ENW} = 0.245 W_G + 0.63 \left[ W_G - \sum D + \sum \frac{s^2}{4g} \right] \quad \text{Eq. 4}
\]

This may be closely approximated by

\[
\text{ENW} = 0.875 W_G - \sum \frac{5}{6} D + \sum \frac{s^2}{6g} \quad \text{Eq. 5}
\]
where

\[ W_G = \] gross width of section
\[ D = \] nominal fastener diameter plus \( 1/8 \) in.
\[ s = \] pitch (stagger) of any two successive holes in the chain
\[ g = \] gage of same holes

Equation 5 was derived on the basis of the eight net section failures. From a comparison of the values of efficiency predicted by this equation and the test efficiencies for all sixteen specimens, we find that the predicted values are 2.4 per cent too high as compared to 6.3 per cent for the AREA Rule. The eight net section failures are predicted 0.5 per cent too high rather than 3.0 per cent by the AREA Rule. By using Equation (5), all initial failures are predicted to within + 10 per cent and - 1 per cent, whereas the predicted AREA values varied from + 15 per cent to - 2 per cent.

Equation (5) of this report has not been applied to any other specimens except those reported in Reference 8. Nor is it presented as a recommendation for effective net width determination, but only as a curve of "best fit" for the present tests. The scarcity of full scale tests on double plane members will not allow as extensive a statistical comparison as has been made with other rules for predicting efficiencies of flat plates. Further, the above equation is limited in application to double plane members, and should not be applied to single plane members, although if used...
for flat plates, the results obtained would probably be more conservative than those obtained from the AREA Rule.

In order to understand more fully the significance of studies on the efficiency of riveted joints, it may be well to consider some of the factors which are not satisfactorily reflected in the derivations or analyses of joint efficiencies.

(1) The possible variation due to rolling tolerances (5, 6) is ± 2.5 per cent. No method for the prediction of efficiency can remove this source of variation.

(2) Wilson, et al. (27) have pointed out that identically fabricated laboratory specimens may vary in strength by as much as 10 per cent. Obviously, then, the variables of fabrication introduce an unpredictable effect in any joint, the magnitude of which can only be determined by a destructive test.

(3) Current specifications (this does not include the Relative Gage Rule (26)) do not limit the maximum efficiency of a joint. And, contrary to the design formulae, few joints have been reported in the literature with test efficiencies above 88 per cent. An upper limit of 75 per cent has been suggested by Davis, Woodruff and Davis (14) and Wilson (14) has suggested 85 per cent. Other recommended maximum efficiencies may be found in the literature. (1) This absence of an upper limit is perhaps one of the most questionable points of current specifications.

(4) No current specifications penalize punched holes.
Various tests during the past century have shown both that punching may or may not reduce the strength. The results of the tests reported herein suggest that punching reduces the strength only slightly, if at all, and that in long joints a punched hole may actually increase the strength, if shear is critical. The Relative Gage Rule suggests that punched holes are only 86.2 per cent as strong as drilled holes, thereby permitting a maximum of 75 per cent efficiency for punched joints.

(5) No specification now distinguishes between single plane and double plane joints, although a difference in behavior is taken into account in some specifications for angles connected by one leg. It is felt that a shear lag, such as was noted in the tests by Fuller, et al, reduces the effectiveness (to about 82 per cent) of the webs of truss-type members and accounts for a large part of the difference between test efficiencies and those predicted by present design rules.

CONCLUSIONS

The following conclusions are based on the results of the tests and studies reported in this paper.

1. Adherence to current design stresses does not necessarily insure a balanced design (i.e., a design in which, at ultimate, the member is likely to fail in either shear or tension); shear failures may be expected in long truss-type joints of "balanced
2. Large connections should be proportioned such that the distribution of rivets in a joint is similar to the distribution of areas connected by the rivets.

3. Members with drilled holes in the connections are more susceptible to shear failures than are similar punched specimens. In addition, the shear strength of the drilled member can be expected to be slightly smaller than that of the punched member.

4. Punched and drilled truss-type members of the same joint pattern and of 3/8 to 1/2 in. thick material may be expected to have approximately the same efficiency. This may be different for thicker materials.

5. The use of lacing bars in tension members provides a secondary loading which may reduce the strength of the members. To reduce the likelihood of tensile failures at the lacing rivets, the edge distances at these rivets should be made as large as possible, and the lacing bars as small as feasible.

6. Of the several design rules considered, the \textit{AREA} net section rule appears to give the best agreement with the test efficiencies of these truss-type members.

7. In view of the lack of complete agreement between theoretical and test efficiencies, and the unpredictable variations in the materials, it is doubted that complicated formulae for the design of tension members are justified. Because of the simplicity
of application and our familiarity with the currently specified rule, it would seem desirable to retain the present net-section rule as a basis for design but to institute a suitable upper limit on efficiency or effective net section. Such a procedure, would correct the most serious deficiency of the current specifications for tension members and would provide, for riveted connections, a predicted or theoretical efficiency which does not differ greatly from the test efficiency.

ACKNOWLEDGMENTS

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## Table 1

**Areas and Properties of Specimens**

<table>
<thead>
<tr>
<th>Spec. Type</th>
<th>Gross Area, Sq. In.</th>
<th>Ratio, Based on Mod. Scale</th>
<th>Net Area, Sq. In.</th>
<th>Tension:Shear:Bearing Ratio</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>(1)</td>
<td>(2)</td>
<td>(3)</td>
<td>(4)</td>
</tr>
<tr>
<td>AD1 Box Section</td>
<td>Drilled 3/4</td>
<td>27.48</td>
<td>27.12</td>
<td>98.68</td>
</tr>
<tr>
<td>AD2 Box Section</td>
<td>Drilled 3/4</td>
<td>27.48</td>
<td>27.24</td>
<td>99.13</td>
</tr>
<tr>
<td>BD1 Laced Angles</td>
<td>Drilled 7/8</td>
<td>11.44</td>
<td>11.44</td>
<td>100.00</td>
</tr>
<tr>
<td>BD2 Laced Angles</td>
<td>Drilled 7/8</td>
<td>11.44</td>
<td>11.20</td>
<td>97.90</td>
</tr>
<tr>
<td>BF1 Laced Angles</td>
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<td>11.24</td>
<td>98.25</td>
</tr>
<tr>
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<td>Punched 7/8</td>
<td>11.44</td>
<td>11.24</td>
<td>98.25</td>
</tr>
<tr>
<td>CD1 I-Section</td>
<td>Drilled 7/8</td>
<td>19.44</td>
<td>19.37</td>
<td>99.64</td>
</tr>
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<td>CD2 I-Section</td>
<td>Drilled 7/8</td>
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<td>19.17</td>
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<td>Punched 7/8</td>
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<td>11.20</td>
<td>97.90</td>
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<td>ED1 Laced Angles</td>
<td>Drilled 3/4</td>
<td>14.44</td>
<td>14.48</td>
<td>100.35</td>
</tr>
<tr>
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<td>Punched 3/4</td>
<td>14.44</td>
<td>14.44</td>
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</table>

Average 98.99

* Based on the nominal diameter of rivets.
TABLE 2

AVERAGE MECHANICAL PROPERTIES OF
SPECIMEN MATERIALS

All coupons were standard 8 in. gage length and tested at a loading rate of 0.2 in/min. Upper yield was determined by drop of beam; lower yield was verified by automatic stress strain plot, where taken. Angle coupons averaged below were from critical sections for specimens.

<table>
<thead>
<tr>
<th>Coupons for Spec.</th>
<th>Location</th>
<th>Av. Upper Yield psi</th>
<th>Av. Lower Yield psi</th>
<th>Av. Ultimate Strength psi</th>
<th>Av. Elong., %</th>
<th>Av. Reduc. in Area %</th>
</tr>
</thead>
<tbody>
<tr>
<td>AD1</td>
<td>angles</td>
<td>45,900</td>
<td>45,000</td>
<td>69,400</td>
<td>26.2</td>
<td>49.6</td>
</tr>
<tr>
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<td>webs</td>
<td>37,400</td>
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<td>62,900</td>
<td>26.7</td>
<td>51.6</td>
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<tr>
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<td>angles</td>
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<td>49.7</td>
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<tr>
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<td>webs</td>
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<td>33,500</td>
<td>58,000</td>
<td>30.6</td>
<td>53.7</td>
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<td>lacing bars</td>
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<td>59,900</td>
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<td>47.4</td>
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### TABLE 3

**CHEMICAL ANALYSIS AND MECHANICAL PROPERTIES OF MATERIALS**  
*(From Mill Reports)*

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<tr>
<th>Material</th>
<th>Carbon</th>
<th>Mang.</th>
<th>Phos.</th>
<th>Sulphur</th>
<th>Silicon</th>
<th>Tensile Strength, psi</th>
<th>Yield, psi</th>
<th>Elong., %</th>
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<tr>
<td>Angles 3 1/2 x 3 1/2 x 7/16 x 22'-6&quot;</td>
<td>.23</td>
<td>.52</td>
<td>.014</td>
<td>.039</td>
<td></td>
<td>66,320</td>
<td>39,650</td>
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<tr>
<td>Angles 5 x 3 x 3/8 x 34'-0&quot;</td>
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<td>.52</td>
<td>.023</td>
<td>.042</td>
<td>.07</td>
<td>63,283</td>
<td>38,877</td>
<td>27.0</td>
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<td>Angles 5 x 5 x 3/8 x 30'-0&quot;</td>
<td>.24</td>
<td>.52</td>
<td>.017</td>
<td>.036</td>
<td>.04</td>
<td>68,963</td>
<td>40,948</td>
<td>25.0</td>
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<td>Plate, Hot Rolled, Sheared 40&quot; x 1/2 x 10&quot;</td>
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<td>.52</td>
<td>.010</td>
<td>.032</td>
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<td>60,000</td>
<td>34,200</td>
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<tr>
<td>Plate, Universal Mill, 16 x 1/2 x 25'-8&quot;</td>
<td>.23</td>
<td>.52</td>
<td>.010</td>
<td>.033</td>
<td></td>
<td>62,280</td>
<td>37,500</td>
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<tr>
<td>Plate Universal Mill 16 x 1/2 x 25'-8&quot;</td>
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<td>.52</td>
<td>.016</td>
<td>.026</td>
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<td>64,740</td>
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<td>26.25</td>
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TABLE 4
COUPON TESTS ON RIVET STOCK

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<th>Rivet Size, in.</th>
<th>Av. Meas. Diam., in.</th>
<th>Av. Ult. Shear Strength* psi</th>
<th>Tensile Test **</th>
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<td></td>
<td>Ult. psi</td>
<td>Reduction in Area, %</td>
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<td>0.741</td>
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<td>3/4 x 2 3/8</td>
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<td>46,700</td>
<td>68,900</td>
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<td>3/4 x 2 3/4</td>
<td>0.740</td>
<td>49,100</td>
<td>70,100</td>
<td>62.1</td>
</tr>
<tr>
<td>3/4 x 2 7/8</td>
<td>0.743</td>
<td>50,300</td>
<td>69,600</td>
<td>65.9</td>
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<tr>
<td>Av., 3/4</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>7/8 x 2 5/8</td>
<td>0.864</td>
<td>43,700</td>
<td>62,500</td>
<td>65.5</td>
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<tr>
<td>7/8 x 3</td>
<td>0.861</td>
<td>44,200</td>
<td>62,800</td>
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<tr>
<td>Av., 7/8</td>
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<td></td>
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<td></td>
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</table>

* Average of four loadings on two rivets; two loadings on each rivet were made on surfaces 3/4" to 1" apart. Shear stress tabulated is based on nominal diameter; loading rate was 0.04 in/minute.

** Average from two tests on coupons machined from undriven rivets with no annealing. Coupons were 0.25 in. in diameter, had 1.00 in. gage and were tested at a loading rate of 0.02 in/minute.
# TABLE 5

**ULTIMATE LOADS AND EFFICIENCIES OF SPECIMENS**

<table>
<thead>
<tr>
<th>Spec. Specimen Type</th>
<th>Hole Prep.</th>
<th>Rivet Size, Load, in. 1000 lb.</th>
<th>Mode of Failure</th>
<th>AREA Design Load,***</th>
<th>AREA Shear,****</th>
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<tr>
<td>AD1 Box Section</td>
<td>Drill 3/4</td>
<td>1155</td>
<td>Sheared rivets, E gusset</td>
<td>364.1</td>
<td>3.17* 2.77</td>
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<tr>
<td></td>
<td>(Bolted)</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>AD2 Box Section</td>
<td>Drill 3/4</td>
<td>1235</td>
<td>Tore lower gussets</td>
<td></td>
<td></td>
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<tr>
<td></td>
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<td></td>
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<td></td>
<td></td>
</tr>
<tr>
<td>BD1 Laced Angles</td>
<td>Drill 7/8</td>
<td>498</td>
<td>at E center lacing rivet</td>
<td>155.2</td>
<td>3.21</td>
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<tr>
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<td>BD2 Laced Angles</td>
<td>Drill 7/8</td>
<td>500</td>
<td>at E top lacing rivet</td>
<td>155.2</td>
<td>3.22</td>
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<tr>
<td>BP1 Laced Angles</td>
<td>Punch 7/8</td>
<td>462</td>
<td>at W top lacing rivet</td>
<td>155.2</td>
<td>2.98</td>
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<tr>
<td>BP2 Laced Angles</td>
<td>Punch 7/8</td>
<td>458</td>
<td>at E top and bot. lacing rivet</td>
<td>155.2</td>
<td>2.95</td>
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<tr>
<td>CD2 I-Section</td>
<td>Drill 7/8</td>
<td>902</td>
<td>W net section</td>
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<tr>
<td>DD1 Laced Angles</td>
<td>Drill 7/8</td>
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<td>W net section</td>
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<td>DD2 Laced Angles</td>
<td>Drill 7/8</td>
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<td>E net section</td>
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<td>E net section</td>
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<td></td>
<td></td>
</tr>
<tr>
<td>ED1 Laced Angles</td>
<td>Drill 3/4</td>
<td>722</td>
<td>Bot. E rivets sheared</td>
<td>214.9</td>
<td>3.36* 3.02</td>
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<td></td>
<td>(Bolted)</td>
<td></td>
<td></td>
<td></td>
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</tr>
<tr>
<td></td>
<td>(Bolted)</td>
<td>3/4 762</td>
<td>Top W rivets sheared</td>
<td>214.9</td>
<td>3.55* 3.19</td>
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<td>(Bolted)</td>
<td>3/4 811</td>
<td>E net section</td>
<td>214.9</td>
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<tr>
<td></td>
<td>(Welded)</td>
<td>3/4 625**</td>
<td>Bot. E bolts sheared</td>
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<td>--- 796</td>
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<td>700</td>
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<td>E net section</td>
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<td>Top W rivets sheared</td>
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* Rivets sheared. ** Common bolts used. *** Based on 18,000 psi in tension.

**** Factor of Safety in Shear = \( \frac{\text{Ult. Load}}{\text{Rivet Area} \times 13,500 \text{ psi}} \)
TABLE 5 (Cont't.)

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<td>Mod. Rel. Gage AREA Based on Avg. (5)</td>
<td>Eq.</td>
<td>AREA</td>
<td>Mod. Rel. Gage (5)</td>
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* Fasteners sheared.
## TABLE 6
APPROXIMATE MAXIMUM NOMINAL SHEAR ON RIVETS

<table>
<thead>
<tr>
<th>Specimen</th>
<th>% of Load in Gusset</th>
<th>Est. Max. Load on Gusset, 1000 lb.</th>
<th>Shear Area, Gusset, sq. in.</th>
<th>Maximum Nominal Unit Shear, psi</th>
<th>Rivet Size, in.</th>
</tr>
</thead>
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<tr>
<td>AD1</td>
<td>52</td>
<td>601</td>
<td>15.46</td>
<td>38,900*</td>
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<td>595</td>
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<td>259</td>
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<td>38,800</td>
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<td>37,400</td>
<td>7/8</td>
</tr>
<tr>
<td>DD2</td>
<td>50</td>
<td>222</td>
<td>6.01</td>
<td>36,900</td>
<td>7/8</td>
</tr>
<tr>
<td>DP1</td>
<td>52</td>
<td>228</td>
<td>6.01</td>
<td>37,900</td>
<td>7/8</td>
</tr>
<tr>
<td>DP2</td>
<td>52</td>
<td>233</td>
<td>6.01</td>
<td>38,800</td>
<td>7/8</td>
</tr>
<tr>
<td>ED1</td>
<td>53</td>
<td>383</td>
<td>8.33</td>
<td>46,000*</td>
<td>3/4</td>
</tr>
<tr>
<td>ED2</td>
<td>51</td>
<td>357</td>
<td>8.33</td>
<td>42,900*</td>
<td>3/4</td>
</tr>
<tr>
<td>EP1</td>
<td>54</td>
<td>399</td>
<td>8.33</td>
<td>47,900</td>
<td>3/4</td>
</tr>
<tr>
<td>EP2</td>
<td>52</td>
<td>374</td>
<td>8.33</td>
<td>44,900</td>
<td>3/4</td>
</tr>
</tbody>
</table>

* Rivets failed.
NOTE: ALL RIVETS 3/8"; ALL HOLES DRILLED 1/10"

ADI SOUTH
AD2 NORTH

WEST EAST

NORTH SOUTH

EAST WEST

1" GUSSET PLATE

BATTEN PLATE
16 x 1/2 x 11 1/2

S7 (INSIDE)

ANGLES 3 1/2 x 3 1/2 x 7/16

16 x 1/2 WEB PLATES

WEB PLATE

FIG. 1 FABRICATION AND INSTRUMENTATION FOR TYPE A SPECIMENS

TYPE A-1 SR-4 STRAIN GAGES

SLIP AND DEFORMATION DIALS
NOTE: ALL RIVETS \( \frac{\ell}{2} \), HOLES IN BD1 & BD2 DRILLED \( \frac{\ell}{16} \), HOLES IN BPI & BP2 PUNCHED \( \frac{\ell}{16} \).

BD1 & BPI SOUTH  
BD2 & BP2 NORTH

WEST  
EAST

NORTH  
SOUTH

EAST  
WEST

TYPE A-1 SR-4 STRAIN GAGES

SLIP AND DEFORMATION DIALS

FIG. 2 FABRICATION AND INSTRUMENTATION FOR TYPE B SPECIMENS
FIG. 3 FABRICATION AND INSTRUMENTATION FOR TYPE C SPECIMENS

- TYPE A-1 SR-4 STRAIN GAGES
- SLIP AND DEFORMATION DIALS

NOTE: ALL RIVETS 1/8" IN.; ALL HOLES DRILLED 15/16"

CD1 SOUTH WEST EAST
CD2 NORTH SOUTH EAST

1/2" GUSSET PLATE

ANGLES 5 x 3 x 3/8"

WEB PLATE 16 x 1/2"
NOTE: ALL RIVETS 7/8”; HOLES IN DDI & DD2 DRILLED 5/16"; HOLES IN DPI & DP2 PUNCHED 5/16”

DD1 & DPI SOUTH  
DD2 & DP2 NORTH

WEST  
EAST  
NORTH  
SOUTH  
EAST

TYPE A-I SR-4 STRAIN GAGES  
NO SLIP AND DEFORMATION DIALS

FIG. 4  FABRICATION AND INSTRUMENTATION FOR TYPE D SPECIMENS
NOTE: ALL RIVETS $\frac{3}{8}$"; HOLES IN ED1 & ED2 DRILLED $\frac{13}{16}$"; HOLES IN EP1 & EP2 PUNCHED $\frac{13}{16}$".

ED1 & EPI SOUTH
ED2 & EP2 NORTH

WEST
EAST

NORTH
SOUTH

EAST
WEST

FIG. 5 FABRICATION AND INSTRUMENTATION FOR TYPE E SPECIMENS
FIG. 6  SPECIMEN (ED2) IN 3,000,000 LB.
TESTING MACHINE
Fig. 7 East Gusset After Rupture, Specimen AD1

Fig. 8 West Gusset After Rupture, Specimen AD1

Fig. 9 Bottom East Joint After Failure of AD2

Fig. 10 Typical Lüder's Patterns on Gussets of Type A Specimens
II FIRST LüDER'S BANDS, SHOWING ORIGINATING RIVETS, BD 2

FIG. 12 EXTENT OF YIELDING AT 410,000 LB., EAST SIDE, BD 2

FIG. 13 EXTENT OF YIELDING AT 475,000 LB., EAST SIDE, BD 2

FIG. 14 EXTENT OF YIELDING AND FRACTURE, SPECIMEN BD 2
G.15 SECONDARY FAILURE AT CENTER LACING RIVET, BP 1

FIG. 16 LOCATION OF RUPTURE, SPECIMEN BD 1

FIG. 17 LOCATIONS OF RUPTURES, SPECIMEN BP 2

FIG. 18 NET SECTION FAILURE, SOUTHEAST ANGLE, SPECIMEN CD 1
FIG. 19 PRIMARY FAILURE, SOUTHWEST ANGLE, SPECIMEN DD 1

FIG. 20 PRIMARY FAILURE, NORTHWEST ANGLE, SPECIMEN DD 1

FIG. 21 RUPTURE, NORTHEAST ANGLE, SPECIMEN ED 1
FIG. 22 FASTENERS REMOVED FROM THE UNFRACTURED JOINTS OF ED 2

FIG. 23 PRIMARY FAILURE OF SOUTHEAST ANGLE, EPI
FIG. 24  LOAD DISTRIBUTION TO PULL PLATES FOR TYPE A SPECIMENS
FIG. 25  LOAD DISTRIBUTION TO PULL PLATES
FOR TYPE B DRILLED SPECIMENS

SPECIMEN BDI

SPECIMEN BD2
FIG. 26  LOAD DISTRIBUTION TO PULL PLATES FOR TYPE B PUNCHED SPECIMENS
FIG. 27 LOAD DISTRIBUTION TO PULL PLATES FOR TYPE C SPECIMENS
FIG. 28 LOAD DISTRIBUTION TO PULL PLATES FOR TYPE D DRILLED SPECIMENS
FIG. 29 LOAD DISTRIBUTION TO PULL PLATES FOR TYPE D PUNCHED SPECIMENS
Fig. 30 Load distribution to pull plates for Type E drilled specimens.
FIG. 31 LOAD DISTRIBUTION TO PULL PLATES FOR TYPE E PUNCHED SPECIMENS
ULTIMATE LOADS ARE LISTED BELOW

AVERAGE STRESS IN TOP LACING BAR, 1000 LB./SQ. IN.

AVERAGE STRESS IN SECOND LACING BAR, 1000 LB./SQ. IN.

FIG. 32 AVERAGE STRESS IN LACING BARS FOR TYPE B SPECIMENS
ULTIMATE LOADS ARE LISTED BELOW

AVERAGE STRESS IN TOP LACING BAR, 1000 LB./SQ. IN.

AVERAGE STRESS IN SECOND LACING BAR, 1000 LB./SQ. IN.

FIG. 33 AVERAGE STRESS IN LACING BARS
FOR TYPE D SPECIMENS
NOTE: STRAIN READINGS ON SPECIMENS ED1 & ED2 ARE SHOWN ONLY FOR THE INITIAL LOAD TEST.

ULTIMATE LOADS OF FIRST TESTS ARE LISTED BELOW

AVERAGE STRESS IN TOP LACING BAR, 1000 LB./SQ. IN.

FIRST ULTIMATE LOADS
ED1 722,000 LB.
ED2 700,000
EPI 738,000
EP2 733,000

AVERAGE STRESS IN SECOND LACING BAR, 1000 LB./SQ. IN.

FIG. 34  AVERAGE STRESS IN LACING BARS FOR TYPE E SPECIMENS
FIG. 35 LOAD-OVERALL DEFORMATION CHARACTERISTICS

FIG. 36 LOAD-FIRST ROW SLIP CHARACTERISTICS
FIG. 37 SECTION ALONG OUTER LINE
OF RIVETS, SPECIMEN AD1

FIG. 38 CLOSE-UP OF FIRST ROW RIVET
IN OUTER LINE OF FASTENERS, AD1
FIG. 39  COMPARISON OF EFFICIENCY
BY A.R.E.A. RULE TO
ACTUAL TEST EFFICIENCY
FOR ALL SPECIMENS